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Preface

This 62d volume of the TRANSACTIONS of the American Institute of Electrical Engineers contains all approved technical-program papers and related discussions presented at national and District technical meetings during the calendar year, in accordance with the improved publication procedure adopted in August 1940. Pages 1–310 and 467–790 comprise the papers published in the Transactions Sections of the monthly issues of ELECTRICAL ENGINEERING, and pages 311–466 and 791–1006 comprise papers and discussions that appeared in advance in the June and December 1943 "Supplements to Electrical Engineering—Transactions Section," respectively.

The technical papers and discussions in this volume were presented at these AIEE national and District technical meetings:

1. 1943 winter technical meeting, New York, N. Y., January 25–29, 1943.
2. North Eastern District technical meeting, Pittsfield, Mass., April 8–9, 1943.
3. South West District technical meeting, Kansas City, Mo., April 28–30, 1943.
4. 1943 summer technical meeting, Cleveland, Ohio, June 21–25, 1943.
5. 1943 Pacific Coast technical meeting, Salt Lake City, Utah, September 2–4, 1943.

In addition, this volume contains:

1. 1943 annual report of the AIEE board of directors.
2. A complete listing of AIEE officers and committees for 1943–44.

Full correlation of all material in this volume has been accomplished by means of the multientry index beginning on page 1029. Reference to any of the several subject entries for a technical paper will lead directly to the paper and to any published discussion on that paper.

Statements and opinions given in papers and discussions published in TRANSACTIONS are the expressions of contributors for which the American Institute of Electrical Engineers assumes no responsibility.

Errata

1. Page 67, column 2, equation 23. The equation $M=Z_{14}+Z_{26}-Z_1-Z_{34}$ should read $M=Z_{14}+Z_{26}-Z_{16}-Z_{24}$.

2. Page 70, column 1. The equation at the bottom of the column beginning I_7, should read $I_7=I_8=I_9=I_{10}\frac{Z_{10}}{Z_8}I_1$.

3. Page 194, second column, 28th line from top. Instead of "less sparsely" the line should read "more sparsely."

4. Page 623, column 3. The first line of the last equation should end with a multiplication sign to join the line with the following expression which begins with an integral sign.

Performance Calculations on Tapped-Winding Capacitor Motors

A Method of Calculating Low-Speed Performance of Two-Speed Capacitor Motors With Open-End Type of Control Winding

P. H. TRICKEY
MEMBER AIEE

THE tapped-winding capacitor motor is used quite commonly to obtain a two-speed fan motor with a comparatively simple switching arrangement. With the open-end type of connection as shown in Figure 1, the high-speed connection gives a normal capacitor motor, and its performance under any load may be determined by calculation with the formulas of either the revolving-field theory or the cross-field theory. When a control winding is added to the already complicated capacitor motor circuits, the analysis by the usual methods becomes even more difficult, and the calculation more involved and tedious than ever. In the following discussion a method is presented for calculating the low-speed performance. This method while not absolutely rigorous, should give results well within the limits of variation of individual motors of the same design.

Analyzing the Problem

The problem may be divided into two parts:

1. The performance of the motor is obtained when operating with the high-speed connection at the lower than normal voltage which will give the desired second speed.
2. If one knows the performance at the proper speed with a certain line voltage applied, the voltage is calculated which will be necessary to apply to the total main and control winding to give the same speed. If this voltage is not equal to the line voltage, a different control winding must be designed.

Low-Speed Performance on High-Speed Connection

One procedure to obtain the low-voltage performance would be to calculate the full-voltage performance at the desired second speed such as point *B* in Figure 4. Then if one knows that the fan torque of point *A* varies as the square of the speed and that the motor torque of point *B* varies as the square of the voltage, the proper voltage to use in calculating point could be obtained by simple ratios.

In actual practice, however, this procedure does not always work out so perfectly, partly because of the change in saturation factor with lower voltage, but mainly because the friction does not drop off with voltage, the torque which should equal point *C* very often is a little more or less, and a third calculation must be made. It has been found advantageous to estimate the voltage for point *C* with its corresponding saturation factor. It is usually found that the torque is so close to point *C* that in one more trial the performance at point *C* is determined, and thus the result is obtained in two trials instead of three.

Another and sometimes more convenient method to obtain the low-voltage low-speed performance is by actual test. The common fan-load test with variable voltage known as the fan saturation test gives the performance of the motor and blade at various voltages and speeds. It makes no difference to the second part of this problem whether the low-voltage low-speed values are obtained from test or calculation. In fact, the calculation sheet shown in Figure 3 shows an approximate method for use when the test does not have phase readings.

Low-Speed Performance on Low-Speed Connection

With the low-voltage performance as a foundation, it is now possible to calculate the performance on the low-speed connection at the same rpm. With the speed set, the voltage necessary to drive the fan at this speed is calculated, and the current and input are obtained. If this voltage

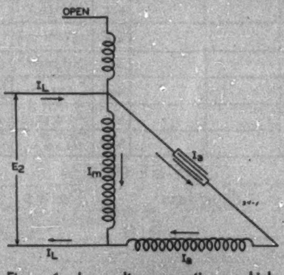

Figure 1. Low-voltage operation on high-speed connection

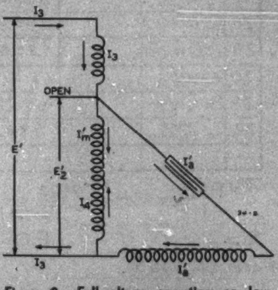

Figure 2. Full-voltage operation on low-speed connection

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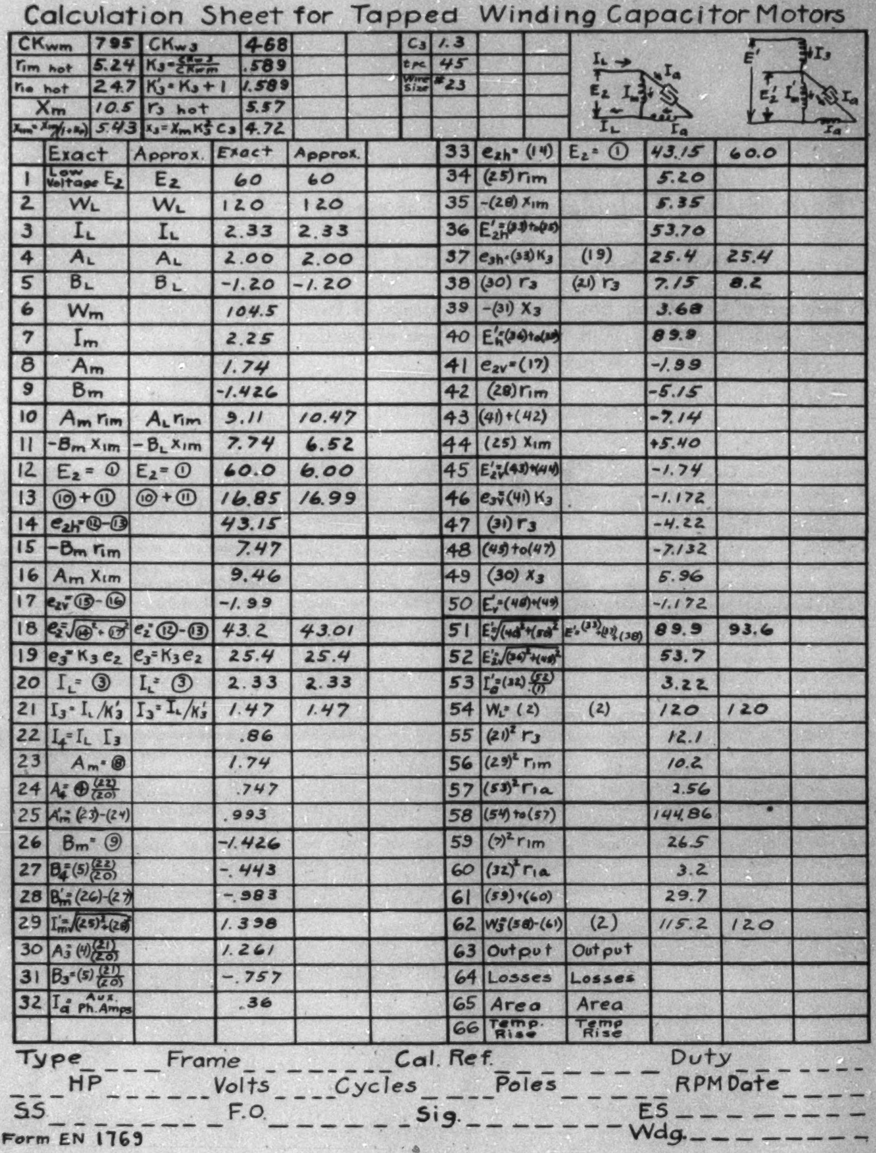

Calculation Sheet for Tapped Winding Capacitor Motors

CK_{wm}	795	CK_{w3}	468			C_3	1.3		
r_{im} hot	5.24	$K_3 = \frac{CK_{w3}}{CK_{wm}}$	.589			tpc	45		
r_{ia} hot	24.7	$K_3' = K_3 + 1$	1.589			Wire Size	#23		
X_m	10.5	r_3 hot	5.57						
$X_{m'} = X_m/(1+K_3)$	5.43	$x_3 = X_m K_3^2 C_3$	4.72						

I_L
I_a
E_2
I_m
I_L
I_a
E'
I_3
E_2'
I_m'
I_a
I_a

	Exact	Approx.	Exact	Approx.		33	$e_{2h} = (14)$	$E_2 =$ ①	43.15	60.0	
1	Low Voltage E_2	E_2	60	60		34	$(25) r_{im}$		5.20		
2	W_L	W_L	120	120		35	$-(28) x_{im}$		5.35		
3	I_L	I_L	2.33	2.33		36	$E_{2h}' = (33)$ to (35)		53.70		
4	A_L	A_L	2.00	2.00		37	$e_{3h} = (33) K_3$	(19)	25.4	25.4	
5	B_L	B_L	-1.20	-1.20		38	$(30) r_3$	$(21) r_3$	7.15	8.2	
6	W_m		104.5			39	$-(31) X_3$		3.68		
7	I_m		2.25			40	$E_h' = (36)$ to (39)		89.9		
8	A_m		1.74			41	$e_{2v} = (17)$		-1.99		
9	B_m		-1.426			42	$(28) r_{im}$		-5.15		
10	$A_m r_{im}$	$A_L r_{im}$	9.11	10.47		43	$(41) + (42)$		-7.14		
11	$-B_m x_{im}$	$-B_L x_{im}$	7.74	6.52		44	$(25) x_{im}$		+5.40		
12	$E_2 =$ ①	$E_2 =$ ①	60.0	6.00		45	$E_{2v}' = (43) + (44)$		-1.74		
13	⑩ + ⑪	⑩ + ⑪	16.85	16.99		46	$e_{3v} = (41) K_3$		-1.172		
14	$e_{2h} =$ ⑫ − ⑬		43.15			47	$(31) r_3$		-4.22		
15	$-B_m r_{im}$		7.47			48	(45) to (47)		-7.132		
16	$A_m x_{im}$		9.46			49	$(30) x_3$		5.96		
17	$e_{2v} =$ ⑮ − ⑯		-1.99			50	$E_v' = (48) + (49)$		-1.172		
18	$e_2 = \sqrt{⑭^2 + ⑰^2}$	$e_2 =$ ⑫ − ⑬	43.2	43.01		51	$E' = \sqrt{(40)^2 + (50)^2}$	$E' = (33) + (37) + (38)$	89.9	93.6	
19	$e_3 = K_3 e_2$	$e_3 = K_3 e_2$	25.4	25.4		52	$E_2' = \sqrt{(36)^2 + (45)^2}$		53.7		
20	$I_L =$ ③	$I_L =$ ③	2.33	2.33		53	$I_a' = (32) \frac{(52)}{(1)}$		3.22		
21	$I_3 = I_L / K_3'$	$I_3 = I_L / K_3'$	1.47	1.47		54	$W_L = (2)$	(2)	120	120	
22	$I_4 = I_L \; I_3$		.86			55	$(21)^2 r_3$		12.1		
23	$A_m =$ ⑧		1.74			56	$(29)^2 r_{im}$		10.2		
24	$A_4 =$ ④ $\frac{(22)}{(20)}$		.747			57	$(53)^2 r_{1a}$		2.56		
25	$A_m' = (23) - (24)$		.993			58	(54) to (57)		144.86		
26	$B_m =$ ⑨		-1.426			59	$(7)^2 r_{im}$		26.5		
27	$B_4 = (5) \frac{(22)}{(20)}$		-.443			60	$(32)^2 r_{1a}$		3.2		
28	$B_m' = (26) - (27)$		-.983			61	$(59) + (60)$		29.7		
29	$I_m' = \sqrt{(25)^2 + (28)^2}$		1.398			62	$W_3 = (58) - (61)$	(2)	115.2	120	
30	$A_3 = (4) \frac{(21)}{(20)}$		1.261			63	Output	Output			
31	$B_3 = (5) \frac{(21)}{(20)}$		-.757			64	Losses	Losses			
32	$I_a =$ Aux. Ph. Amps		.36			65	Area	Area			
						66	Temp. Rise	Temp Rise			

Type____ Frame________ Cal. Ref.________ Duty______

___ HP______ Volts____ Cycles____ Poles______ RPM Date_____

SS_________ F.O._______ Sig.________ ES__________

Form EN 1769 Wdg.__________

Figure 3. Example calculation

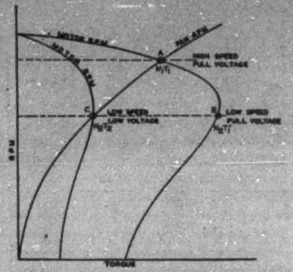

Figure 4. Motor and fan speed-torque curves

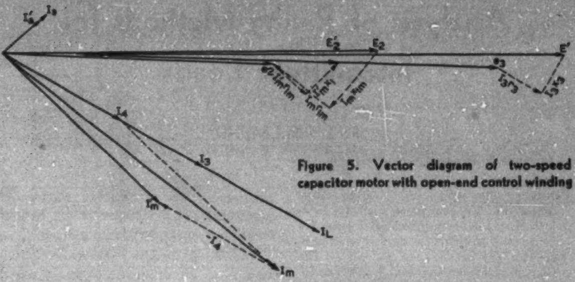

Figure 5. Vector diagram of two-speed capacitor motor with open-end control winding

E' is not equal to the line voltage, the motor will not drive the fan at the proper second speed, and a new control winding must be designed.

Figure 5 shows the combined vector diagrams of the two connections when operating at the same speed. I_m, I_a, and I_L are the currents with the high-speed connection and low-voltage E_2. The induced voltage e_2 in the main winding is obtained by subtracting the reactance and resistance drops in the main winding due to the current I_m. It is assumed that this induced voltage will remain the same under the low-speed connection if the speed is maintained at the same value.

Since the control winding is placed in the main-winding slots, it acts with the main winding to form an autotransformer as well as to provide a series impedance. It is assumed that the autotransformer and motor effects are superimposed on each and that the motor provides the magnetizing current. Because of the transformer action, the new line amperes I_3 will be reduced from I_L in the transformer ratio.

$$I_3=I_L/K_3'$$

where

$K_3'=K_3+1$
$K_3=CK_{w3}/CK_{wm}$
CK_{w3} = effective series conductors in control winding
CK_{wm} = effective series conductors in main winding

Because of the transformer action, the main winding carries a transformer current

$$I_4=I_L-I_3$$

The actual current in the main winding I_m' will be a combination of this current I_4 with the main-winding motor current I_m.

$$I_m'=I_m-I_4$$

When all the new current values in each branch of the circuit are obtained, the new low-speed connection line voltage E' may be obtained by adding to e_2 the proper impedance drops plus the induced voltage e_3 in the control winding.

$$E'=e_2+I_m'r_{1m}+jI_m'x_{1m}+e_3+I_3r_3+jI_3x_3$$
$$e_3=K_3e_2$$

The input will differ somewhat from W_L, the input on the high-speed connection, by the addition of the control winding copper loss $I_3^2r_3$, and by the reduction in the copper loss in the other two windings. The main-winding copper loss is reduced because of the lower value of the current I_m'. Also due to this reduced main-winding current, the capacitor-phase terminal voltage E_2' is less than E_2, and with it the capacitor-phase current I_a'. The capacitor-phase copper loss is reduced accordingly.

Figure 3 shows a convenient form of calculation sheet with an illustrative example.

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Analysis of Rating Methods for Intermittent Loads

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Synopsis: The economics of rating structures for electrical equipment from the viewpoints of manufacturing and application are considered. Methods for dealing with periodic load applications are reviewed. The present methods have certain limitations for some practical applications. An improved method for applying motors to load cycles where the motor stops and ventilation changes in the no-load part of the cycle is developed. This method is based on certain motor-loss ratios, that is, the relation between the losses dissipated with standard temperature rise running and at standstill and the losses at the nominal or short-time rating. It permits the application of service-factor or short-time ratings to periodic loads with more exactness. Attention is called to the wide variations in permissible rms current on periodic loads for different loss ratios and changes in ventilation on a motor.

Rating Structures

RATINGS for intermittent or varying loads have recently received considerable attention[4–10] and AIEE Standard 1*A*[1] is an attempt to clarify and rationalize suitable methods for this purpose. No radically new methods of rating are proposed, but some new nomenclature and new viewpoints are introduced; the report also advocates increased use of some methods of rating which in the past have been applied to only a limited extent.

Naturally there is some hesitation in making changes in the rating practice, which is justified, because departures from well-established methods are likely to lead to confusion and expense. On the other hand, it must be realized that any change in practice which results in economies appreciably outweighing the disadvantages involved will probably be made sooner or later. All action of this nature should be based primarily on economic considerations. Broadly speaking, the number of rating methods should be kept as small as possible but they should not be so limited that they prevent satisfactory and reasonably economical applications of devices under the various conditions encountered in practice.

In making changes in the rating structure from an economic point of view, serious thought should be given to what such a change really entails. If a new rating method requires entirely new lines of machines and apparatus different from those already in existence and thus interferes with quantity production, and if it furthermore complicates the stock situation both at the factory and in the field, it is justified only if it leads to decided improvements. On the other hand, if a change merely means giving some additional data for apparatus already standardized, it should not be objectionable. Even here it must be realized that the additional information means expense and complications in listing, merchandising, and applying the apparatus. Hence the inclination to adhere to the present practice of handling most apparatus by means of a single continuous rating as far as possible is quite natural. In the past, a departure from this simple practice has in general been made only under compelling circumstances. An example of an extreme case is the rating and application of single-phase railway motors[2] where the information given with the rating structure entails weeks of expensive tests as well as time-consuming methods of application. These expenses, however, are unquestionably well justified in view of the large number of motors of the same rating in use and the enormous investment involved. Between the extreme conditions there is, of course, room for a variety of intermediate practices, some of which have been used to a limited extent, as, for example, the rating structure for d-c railway, mill-type, and other motors. In deciding whether the use of a more complicated rating structure is warranted, the expense for securing and giving additional rating data as well as the advantages derived should be taken into account. Frequently the inclusion of additional information will result in easy and economical application and fully compensate for the expense involved. The choice of method should also be influenced by the existence or lack of well-established practices. If a rating practice has been in use for many years and has given reasonable satisfaction, a change would seem advisable only if the advantages obtained are appreciable. On the other hand, where it is necessary to establish new rating structures in entirely new fields, it seems logical to apply newer methods although they have only moderate advantage over others. In the following it is proposed to discuss some of the methods recently advocated and to point out their limitations; subsequently methods are indicated for overcoming these limitations and for handling cases which do not seem to be covered satisfactorily by AIEE Standard 1*A* and related literature.

Ratings for Intermittent Duty

AIEE Standard 1*A* strongly emphasizes the many applications of electrical machines and apparatus where a given load is carried for sustained periods or repeatedly for shorter periods, but where there are also light- or no-load conditions. The device must be satisfactory for the required load in many respects, such as regulation, torque characteristics, mechanical strength, and so forth, and it is therefore proposed to assign a nominal rating corresponding to the load. It is also proposed to take advantage in the design of the noncontinuous nature of the load.

Two methods are given for such cases, namely, the dual or service-factor method and the load-cycle method of rating. In the first method the nominal rating is supplemented by a service factor, which is the ratio of the continuous rating to the nominal rating and thus indicates the continuous rms rating of the machine or apparatus. This method, when applicable, has the advantage of being much more flexible in application than the load-cycle method. It permits the analysis of loads of varying size and time of application, merely requiring that the rms load does not exceed the continuous load indicated by the service factor. In contrast, the load-cycle method is rather inflexible in its present form as its application is limited to a single load cycle, with both the amplitude and duration of the load for each cycle specified. Therefore, this method naturally will be considered only where for some reason the service-factor method is not suitable.

Either of the periodic rating methods permits designs smaller than those with continuous ratings equal to the proposed nominal ratings of the periodic rating methods. Thus appreciable economy

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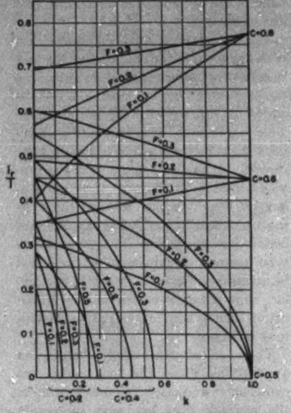

Figure 1. Curves for equation 3 of Appendix I, with loss ratios C and F variable and $p = 0.5$, showing permissible rms currents for different ratios of running time per cycle

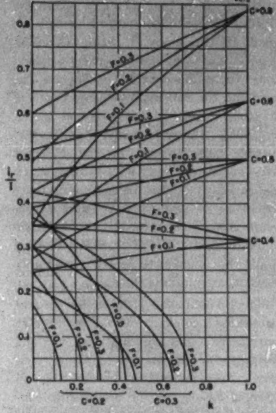

Figure 2. Curves for equation 3 of Appendix I, with loss ratios C and F variable and $p = 0.333$, showing permissible rms currents for different ratios of running time per cycle

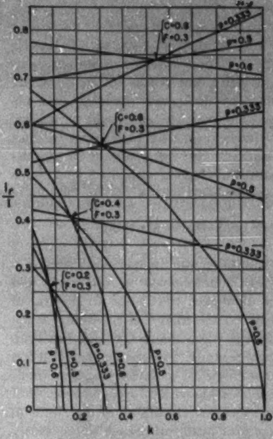

Figure 3. Curves for equation 3 of Appendix I, with loss ratios p and C variable and $F = 0.3$, showing permissible rms currents for different ratios of running time per cycle

may be accomplished in view of the many applications where continuous loads are not involved. However, it is highly desirable to obtain this economy without creating entirely new lines of ratings. This can be done by using the standard short-time ratings[5,8] wherever they exist. These ratings cannot carry the nominal rated load continuously but usually are suitable for service-factor or load-cycle ratings. To get a service-factor rating, or to indicate the load duration and the percentage of the total time during which the nominal load can be carried to obtain a load-cycle rating, it is then merely necessary to supplement the short-time or nominal rating with the service factor. Even where no short-time ratings are available and new ratings have to be established, it seems desirable to make designs which are suitable for two or more of the rating methods, such as short-time, service-factor, and load-cycle ratings.

Unfortunately the service-factor method, which is flexible in its application to a great variety of load conditions, is not always applicable. It is based on a single rms value applying for a constant condition of cooling, which results in limitations under certain conditions.

For example, when a device cannot carry any load continuously without overheating, it is obviously impossible to give a service factor. This is frequently the case with enclosed machines or other enclosed apparatus where the core and similar losses, if prevailing continuously, cause temperature rises in excess of those permissible for continuous operation. Such devices can be and frequently are used to good advantage on intermittent loads where no core or similar losses occur during idle periods. (In a few cases, such as series d-c motors where the speed varies and the operating voltage ranges between zero and rated voltage, an rms rating can at times be given at a reduced voltage corresponding to the average operating voltage and a modified application of the service-factor method is possible.)

Again, when the ventilation during the on and off periods of a load cycle changes materially, making the resultant effective ventilation appreciably less than the ventilation prevailing during the tests with the continuous load current, the service-factor method cannot be readily applied. Motors which stand still during the no-load periods, with an appreciable reduction in ventilation, are an illustration of this.

When the heating or other operating limitations of devices are not entirely a function of the rms current, the service-factor method is not very suitable. For example, in electronic tubes the safe output is influenced by many different factors but only to a negligible extent by the rms load, and therefore the rms load cannot be used as a correct indicator of permissible load.

In these cases and possibly others, the load-cycle method of rating may have to be considered, and it would be of advantage to find ways of making this method more flexible. One possible way is to assign to a given device, in place of a single load-cycle rating, a number of different load-cycle ratings[9,10] in such a way that intermediate applications can be handled by interpolation. This method has been used with electronic devices for applications, such as spot welding, and is giving satisfactory results in practice. Although the assignment of a number of ratings naturally introduces complications, in this case the necessary interpolation can be readily and quickly made and, in fact, takes less time than the calculation of the rms or average currents. In other words, the extra expense for establishing several ratings is compensated for by time saved in applying the device.

Periodic Loads on Motors

Unfortunately, a similar practice for motors is almost out of the question. An acceptance test for a load cycle during which the motor must be started and stopped repeatedly is difficult because of temperature measurement and the necessity of properly adjusting the load to the desired value during each cycle. Fair results can be obtained where the operating period is reasonably long, but serious difficulties are encountered in short load cycles, which are more common in practice. Therefore an entirely different method of attack seems desirable. In a previous paper[7], a method permitting certain load-cycle tests with relatively long operating periods on standard short-time rating motors was outlined. Methods were indicated whereby data obtained from such tests could be utilized to derive results for load cycles of short duration. In the following a simpler method applicable with short load cycles is suggested.

The Loss-Ratio Method

Basically, the temperature rise of a motor is determined by the losses and the ability of the motor to dispose of them. This ability is obviously much different when the motor is at rest than when running. Consequently it seems almost hopeless to devise proper methods of application without some knowledge of the losses and the ability of the motor to dispose of heat during each of the widely different conditions of operation. Furthermore, with this knowledge, applications can be readily made for any motor having a short-time or any other nominal rating. The essential quantities are:

1. The constant losses (core and so forth) at the nominal rating $=L_c$
2. The variable losses (copper and so forth) at the nominal rating $=L_v$
3. The continuous losses permissible for standard temperature rise[1] at rated speed $=L_r$
4. The continuous losses permissible for standard temperature rise at standstill $=L_s$

Values for the first two items are always available from the efficiency determination of the machine. With a service-factor rating, the value for the third item can be readily calculated from items 1 and 2 by use of the service factor (equation 4, Appendix I). If no service-factor rating is available, the information for item 3 can be obtained from a continuous run at reduced voltage and load giving standard temperature rise. Preferably the ratio between constant and varying losses during this test should be the same as at the nominal rating. The information for the fourth item can be obtained by a continuous test run with the motor at rest, at a voltage low enough to again result in the standard temperature rise. Then by means of the simple algebra of Appendix I, any desired load-cycle condition can be calculated. Formula 2 gives the load current which can be carried for any value of running time per cycle, and formula 3 indicates the rms current for the entire load cycle corresponding to the load current of formula 2. Figure 1 shows curves indicating the value of the permissible rms current for cases where the constant losses and varying losses are equal at the nominal rating ($p=0.5$). Figure 2 shows similar curves for applications where the constant losses are one third and the varying losses two thirds of the total losses at the nominal rating ($p=0.33$). It will be noted that the permissible rms current for the different ratios varies over very wide ranges, which confirms the fact that inexact methods are not likely to give very satisfactory results. Also, it is evident that the basic factors considered in the present method should be known to make reasonably accurate calculations possible.

Figure 3 again shows values for the rms current for a constant ratio of permissible continuous losses standing and running ($F=0.3$). Different ratios (p) for constant to total losses at nominal rating and different ratios (C) for permissible continuous losses running to total losses at the nominal rating are used. A number of curves of this character, each for a different value of F, seem to be most suitable for practical use. The ratio F is a machine characteristic, whereas p and C depend upon the nominal rating considered. Reasonably correct results can be obtained from such curves by interpolation for different values of the various ratios. Since the variables used in this method are all ratios of losses, this method might be referred to as the "loss-ratio method." It is applicable directly to existing short-time ratings as well as to service-factor ratings, and it therefore obviates the necessity for separate and distinct lines of additional ratings.

In a previous paper[8] dealing with load-cycle ratings, a factor (S_3) was introduced to cover motors standing still during no-load periods. The formulas given were based on a factor (f), intended to make allowance for reduced ventilation and the absence of core losses at standstill. These two effects influence results in opposite directions, and thus factor f depends upon the difference of two quantities. Since in practice each of these effects varies over wide ranges, the resultant difference is erratic, making it almost impossible to deal with uniformly consistent values for f (see discussion of paper, reference 8). Formula 5 of Appendix I gives the same factor (S_3) in terms of the loss ratio F. With the method now proposed, factor F depends only upon the change of ventilating conditions and hence is likely to be fairly uniform, or at least quite consistent for a given type and line of machines. Therefore the present method will yield reasonably correct results even if test data for the value F are not available for all sizes of a line of motors.

The calculations outlined above deal with the total losses and neglect the fact that hot-spot temperatures are influenced to some extent by the distribution of the losses in various parts of the machine. This can be taken into account by allowing a slight factor of safety in applying the motors. It is also evident that the previous considerations apply more particularly to a-c induction motors, the type most universally used. For the different conditions prevailing in commutator-type motors, certain modifications will be necessary; for series motors in particular, more complicated methods of application such as used for d-c railway motors may be in order.

These calculations apply directly to applications where the load cycles are rather short and where the temperature of the machine does not vary materially during the cycle, the applications most common in practice. For relatively long load cycles or for load cycles very unevenly distributed, the methods previously outlined[8] for the service-factor method can be used, or the factor S_2 given there can be applied to the method outlined here. In other words, the factor S_2 can be used to give the relations between short- and long-cycle applications regardless of whether the service-factor rating is given. When no service factor is given, the permissible rms current is obtained from formula 3, Appendix I, for short-load cycles. By multiplying this value by the factor S_2 (Figure 7 of reference 8), permissible rms values for longer load cycles involving appreciable temperature changes can be obtained. Thus the loss-ratio method is applicable to both types of periodic duty cycles involving standstill during the no-load period, a condition for which other methods have limitations.

Appendix I. Determination of Permissible Periodic Load Cycles for Motors by the Loss-Ratio Method

Let

$L=$ total losses at the nominal rating current I

$L_c=$ constant (core and so forth) losses $=pL$

$L_v=$ variable (copper and so forth) losses $=(1-p)L$

$L_r=$ continuous losses at rated speed for standard temperature rise

$L_s=$ continuous losses at standstill for standard temperature rise

$L_r = CL$ where $C = L_r/L$
$L_s = FL_r = CFL$ where $F = L_s/L_r$
T = total time per cycle
$T_r = kT$ = running time per cycle

For any value of k, the losses which can be dissipated during the total cycle must equal the losses produced by a certain load current (i_k) during the running part of the cycle.

Therefore

$$L_r k + L_s(1-k) = L_c k + \left(\frac{i_k}{I}\right)^2 L_v k \qquad (1)$$

Introducing the loss ratios p, C, and F and solving

$$i_k = I\sqrt{\frac{1}{1-p}\left(C-p-CF+\frac{CF}{k}\right)} \qquad (2)$$

The rms current for the entire cycle corresponding to the periodic current (i_k) is

$$i_r = \sqrt{i_k^2 k} = I\sqrt{\frac{k}{1-p}\left(C-p-CF+\frac{CF}{k}\right)} \qquad (3)$$

For a motor with a service-factor rating, let

S_1 = service factor
i = continuous rating current
S_2 = ratio of rms current to continuous rating current

Then

$$L_r = pL + S_1^2(1-p)L \qquad (4)$$

Also

$$L_r = L_c + \left(\frac{i}{I}\right)^2 L_v$$

from which

$$i = I\sqrt{\frac{C-p}{1-p}}$$

Then

$$S_2 = i_r/i = \sqrt{\frac{k}{C-p}\left(C-p-CF+\frac{CF}{k}\right)} \qquad (5)$$

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A New Type of Adjustable-Speed Drive for A-C Systems

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Synopsis: This paper describes the theory and characteristics of a new type of polyphase adjustable-speed motor and its control circuit. The system provides an a-c drive that has characteristics comparable to those of the Ward Leonard system used on direct currents. The control circuit provides starting as well as speed adjustments from 0 to 100 per cent of rated value. The power factor of the motor is either leading or very close to unity over its operating range, and consequently the efficiency is high. Low costs of this type of motor and its control system, the large speed range, the facility for rapid reversal by regeneration, and its high starting torque make it suitable for some industrial applications.

ATTEMPTS to adapt d-c shunt motors for use on alternating currents have been unsuccessful because of the difficulty in producing a flux that is in time phase with the armature current. In order to obtain a high power factor and a large torque, such a motor would have its armature current under load inphase or approximately inphase with both the armature voltage and the air-gap flux. These conditions are unattainable in the a-c shunt motor. When the field and armature are supplied with the same voltage, the phase of the field current is approximately 90 degrees behind that of the voltage. The flux produced by the field is approximately in quadrature with the armature voltage. The armature current is more nearly inphase with the armature voltage, and consequently it cannot be inphase with the flux. In addition to this the generated voltage produced by rotation of the armature in the field is approximately 90 degrees out of phase with the applied voltage instead of being directly opposed to it as in a d-c motor. Consequently the armature current is limited largely by the impedance of the armature, and speed has little effect on this current. Thus the single-phase shunt motor used on alternating current will take very large currents at all loads, and, since changes in generated voltage have little effect on this armature current, increases in load will cause large reductions in speed. The speed regulation of such a motor is objectionably high.

The undesirable features referred to above can be overcome by adapting the motor for use on polyphase systems in a manner such that the voltage applied to the field is approximately at right angles in time phase with respect to the armature voltage. Under such conditions the inductive effect of the field will cause the air-gap flux to be approximately inphase with the armature voltage. The generated voltage in the armature due to rotation will oppose the applied voltage, causing the motor to behave very much like a d-c shunt machine. Thus in Figure 1, if the field and the armature of a motor are supplied from separate phases of a two-phase system, the motor would take a small armature current at no load and an increased current when load is applied. The armature current would be out of phase with the armature voltage, and, because of the large armature impedance, the speed regulation would be similar to that of a shunt motor with a large resistance in its armature circuit.

The Motor and Its Circuits

Objections to the two-phase motor shown in Figure 1 can be overcome by

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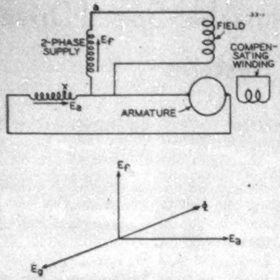

Figure 1. The motor supplied with two-phase voltages and its vector diagram

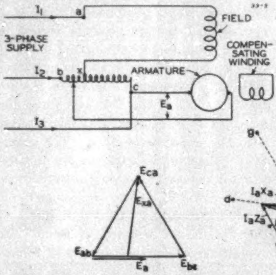

Figure 2. The motor supplied with three-phase voltage and the voltage vectors

proper design of the motor and by supplying the field with a voltage that is in proper phase relationship with respect to the armature voltage. The armature should be designed with the lowest value of impedance practicable. This requires a compensating winding on the stator which can be connected either in series with the armature or short-circuited—the latter connection produces inductive compensation. The voltage applied to the field should be shifted in time phase with respect to its quadrature position shown in Figure 1, so that the time phase of the air-gap flux is slightly behind the armature applied voltage. This shift is essential in order that the power factor of the motor be kept high and can be accomplished in the circuit shown in Figure 1 by connecting the field across the points *a* and *x*.

The methods of obtaining this slight shift of voltage on polyphase systems are numerous. The requisite voltages for field and armature referred to above can be obtained from a three-phase supply by use of the circuit shown in Figure 2. The motor has an armature similar to that of an ordinary repulsion motor. The compensating winding is placed on the stator in such a position as to neutralize the flux produced by the armature ampere turns. The main field winding is placed on the stator and is spaced 90 electrical degrees with respect to the compensating winding. Three-phase voltage is supplied at the points *a*, *b*, and *c*, and an adjustable transformer is connected between *b* and *c*. The motor armature can be supplied with all or any portion of the voltage between the points *b* and *c*. Selection of speed is obtained by adjustment of this armature voltage. The field winding is connected between line *a* and point *x* on the adjustable transformer. The field winding acts as a transformer in making essentially a three-phase to two-phase transformation of voltages. This differs from the ordinary Scott connection in that the point *x* is not exactly midway between *b* and *c*.

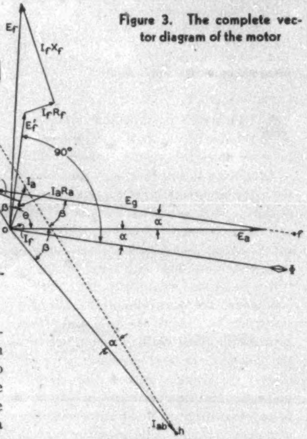

Figure 3. The complete vector diagram of the motor

Theory of the Motor

The theory and operation of the motor connected as in Figure 2 can be explained on the basis of its vector diagram shown in Figure 3. This is the diagram for a two-horsepower motor constructed in accordance with the required features described above. The field voltage is shown as the vector E_f, and for this particular case it is almost perpendicular to the armature voltage vector E_a. The phase angle between these voltages can be changed by moving the tap *x* on the control transformer. In the analysis presented here it is assumed that the armature and field voltages remain constant and fixed in direction for all values of load and that the impedance of the control transformer is negligible.

When voltage is supplied to the field of this motor, the back electromotive force in the field is produced by a resistance drop, a drop due to the mutual flux that cuts the armature and the field conductors, and a drop due to the leakage flux which cuts the field winding but not the armature. The vectors representing these voltages are shown in Figure 3. The voltage applied to the field is E_f, the leakage-reactance voltage drop is I_fX_f, and the voltage produced in the field winding by the mutual flux is E_f'. The mutual field flux that generates the voltage E_f' in the winding is represented by the vector ϕ.

If the armature voltage E_a is applied while the rotor is held stationary, the armature current will be limited by the equivalent resistance and reactance of the armature circuit. The equivalent resistance R_a is determined by the effective resistance of the armature circuit plus the resistance of the compensating winding reflected to the armature; whereas the equivalent reactance X_a is determined by the closeness of coupling of the compensating winding to the armature. Thus the blocked-rotor armature current I_{ab} is equal to the voltage E_a divided by the impedance Z_a of the armature circuit, where

$$Z_a=\sqrt{R_a^2+X_a^2}$$

The current I_{ab} shown in Figure 3, lags the armature voltage by the angle β where

$$\beta=\cos^{-1}\frac{R_a}{Z_a}$$

β is also the angle between the voltage I_aZ_a and the current I_a, where I_a is the armature current for any value of load

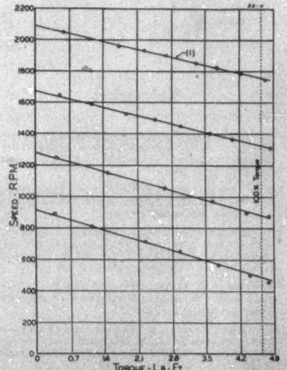

Figure 4. Speed-torque curves of the motor for different values of armature voltage

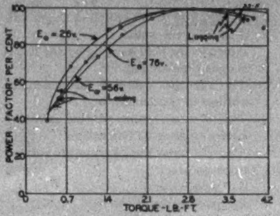

Figure 5. Power-factor curves for different speed settings

The current I_{ab} acting on the flux ϕ produces torque. If the motor is allowed to rotate, a counterelectromotive force similar to the back electromotive force in a direct-current motor will be generated. This generated voltage E_g will have the same time phase as the flux ϕ, since it is proportional to the product of the flux and speed. Therefore the generated voltage vector $\mathbf{E_g}$ is parallel to the flux vector ϕ, and the magnitude of E_g is proportional to the speed of the motor if the flux is constant. The sum of the voltages in the armature circuit must equal the applied voltage, or expressed as an equation

$$I_aZ_a+E_g=E_a$$

In this relationship the voltage E_g has a fixed direction but can change in magnitude as explained above; however the voltage E_a is constant in both direction and magnitude as assumed. Therefore the term $\mathbf{I_aZ_a}$ has a variable direction and magnitude such that the above equation is satisfied.

For an increase in speed, the magnitude of the vector $\mathbf{E_g}$ in Figure 3 increases along the dotted line *fd*—this line is parallel to the flux vector φ. The terminus of the vector $\mathbf{I_aZ_a}$ must always lie along the dotted line *fd* in order that the vector sum of $\mathbf{I_aZ_a}$ and $\mathbf{E_g}$ be equal to the vector $\mathbf{E_a}$. Thus the extremity of the vector $\mathbf{I_aZ_a}$ has the straight line locus *fd*. Since the changes in the brush resistances are small over the operating range of the motor, the armature impedance Z_a is essentially constant. The armature current I_a is proportional to $\mathbf{I_aZ_a}$; therefore, the terminus of the vector $\mathbf{I_a}$ also follows a straight-line locus in the same manner as the vector $\mathbf{I_aZ_a}$. The locus of $\mathbf{I_a}$, shown as the dotted line *gh* in Figure 3, is displaced from the line *df* by the impedance angle β. The vector $\mathbf{I_aZ_a}$ must lead the armature current vector by the impedance angle β.

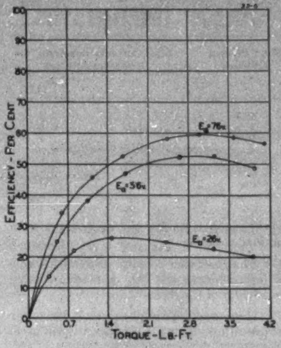

Figure 6. Efficiencies for different speed adjustments

The power developed is equal to the projection of E_g on I_a multiplied by I_a, and the copper loss in the armature circuit is equal to the projection of $\mathbf{I_aZ_a}$ on I_a multiplied by I_a. At no load the developed power is small, therefore the current vector $\mathbf{I_a}$ is approximately perpendicular to the vector $\mathbf{E_g}$. For the condition of no load the armature current I_a as shown by the vector diagram is leading the applied voltage E_a. When the machine is loaded, there is a slight reduction in speed and consequently a reduction in the magnitude of the vector $\mathbf{E_g}$. This change in $\mathbf{E_g}$ causes a change in the magnitude and direction of the vector $\mathbf{I_aZ_a}$—such that its extremity moves along its locus *df*. This clockwise shift of the vector $\mathbf{I_aZ_a}$ is associated with an equivalent shift in the current vector I_a. The locus of the terminus of the vector $\mathbf{I_a}$ is *gh*. Thus an increase in load is associated with a reduction of speed. The torque supplied to the shaft is represented by the projection of the current vector $\mathbf{I_a}$ on the flux vector ϕ (Figure 3). Expressed as an equation

$$T=K\phi I_a\cos(\alpha+\theta)$$

where θ is the power-factor angle of the armature circuit. This angle θ is considered plus for leading power factors and minus for lagging power factors. The factor K is a constant and α is the angle between the flux vector ϕ and the voltage vector $\mathbf{E_a}$.

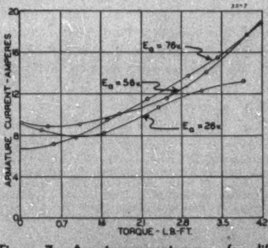

Figure 7. Armature-current curves for different speed adjustments

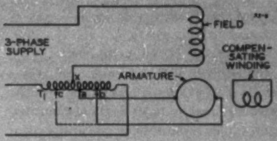

Figure 8. Circuit for reversing drive

Performance

Experiments conducted on a motor supplied as shown in Figure 2 reveal that the characteristics predicted from the vector diagram of Figure 3 check the performance characteristics of the motor. The speed-torque characteristics of this motor with different values of impressed armature voltage are shown in Figure 4. These speed-torque curves are straight lines and are parallel. For the high-speed setting the speed regulation of the motor is approximately nine per cent. These speed-torque curves indicate that the motor has an extremely high starting torque. Selection of speed can be accomplished by adjusting the armature voltage E_a. The power factor of the armature circuit for different speed settings is shown in Figure 5. The corresponding efficiency and armature current curves are shown in Figure 6 and Figure 7 respectively.

Since one of the three-phase lines to the motor carries only field current, while each of the other two lines carries the transformed armature current and a portion of the field current, the three line currents supplying this system must be unbalanced. The extent of unbalance for approximate equal intervals of load from no load to full load for the performance curve 1 of Figure 4 is indicated in Table I.

Additional Control Features

The motor described here can be easily adapted for reversing operation by the use of the circuit shown in Figure 8. In this circuit one side of the armature is connected to the mid-tap of the transformer. The other side of the armature can be connected to either side of the mid-tap for operation. When connected on the right (Figure 8) of the mid-tap, the direction of rotation of the motor will be reversed with respect to its direction of rotation when connected to the left. Thus reversing is easily obtained.

Other types of transformer connections can be used to supply this motor with the same terminal voltages as those discussed here. Such connections naturally provide the motor with the same operating characteristics. Field control can be added in these circuits, but it usually results in poorer motor characteristics. A slight amount of saturation in the field provides fair speed regulation in the motor. Weakening the field will cause an increase in speed, but it will also cause an increase in the speed regulation.

If the sliding contact is moved rapidly toward the other armature supply tap on the adjustable transformer, there will be a rapid reduction of the armature voltage E_a. This causes a reversal of the power component of the armature current. The machine will act as a generator, and high braking action will result. Thus the same control apparatus used for starting and controlling the speed can be used for braking.

Conclusions

1. The theory of a new type of adjustable-speed drive for use on alternating currents is presented here.

2. The theory is verified through experiments conducted on a two-horsepower motor and its controller. Performance of the system was found to be meritorious.

3. The system provides speed adjustment for either direction of rotation.

4. The speed range is from 0 to 100 per cent of the rated armature speed.

5. By proper adjustment the power factor of the motor is leading or close to unity from no load to full load.

Table I

Field Current Amperes	Line Currents Supplying the Armature Amperes	
I_1	I_3	I_2
9.5	12.2	6.1
9.5	13.0	6.0
9.5	15.1	7.1
9.5	16.4	8.0
9.6	17.4	8.2
9.6	19.3	9.7
9.6	22.9	13.5

6. The controller consists of an adjustable autotransformer and can be mounted independently of the motor itself.

7. High starting torques are available for accelerating heavy loads or loads with large inertias.

8. No energy is lost in starting resistors in this type of control. The system has smooth accelerating characteristics from standstill to maximum speed.

9. Since the armature currents on starting are supplied at low voltage, the input currents to the system are small, thereby eliminating line-voltage disturbances normally associated with motor starting.

10. Rapid acceleration and deceleration are provided in the control circuit.

11. Efficiency of the system is comparatively high.

12. Simplicity of the control circuit and its manifold uses make the unit relatively inexpensive.

13. The unbalance of line currents makes its application to large motors undesirable. The unbalance produced by small motors of this type is not objectionable.

List of Symbols

E_a—The voltage applied to the armature.

E_f—The effective value of the voltage applied to the field winding of the motor.

E_f'—The effective value of the voltage induced in the field winding by the flux ϕ crossing the air gap.

E_g—The effective value of the generated voltage in the armature.

I_a—The effective value of the armature current expressed in amperes.

I_{ab}—The effective value of armature current expressed in amperes when the motor is at standstill.

I_f—Effective value of the field current.

K—A constant for any particular motor involving the total number of turns on the armature, the number of parallel paths, and the number of poles.

R_a—The equivalent resistance expressed in ohms of the armature and compensating winding.

R_f—Resistance of the field winding.

T—The developed torque of the motor expressed in pound-feet.

x—The point on the adjustable transformer at which the field is connected.

X_a—The equivalent reactance of the armature and compensating winding expressed in ohms.

X_f—The effective reactance of the field winding expressed in ohms caused by the leakage flux that does not cross the air gap.

Z_a—The equivalent impedance of the armature and compensating winding expressed in ohms.

α—The phase angle expressed in electrical degrees between the generated voltage and the applied armature voltage.

β—The angle having a cosine that is equal to the ratio of the armature resistance R_a to the armature impedance Z_a.

θ—The phase angle or power-factor angle in electrical degrees between the impressed armature voltage E_a and the armature current I_a.

ϕ—The total flux per pole crossing the air gap expressed in maxwells.

A New Generator Differential Relay

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"RELAYS have been available for years capable of providing selective high-speed protection if—and it is a big IF—the intelligence transmitted to the relay closely approximates the truth." This statement, recently made by a protection engineer, is a true statement, the import of which is not always fully recognized.

Regarding current supply, there are many relay applications for which orthodox current transformers are quite adequate. On the other hand, applications such as differential relaying require careful consideration of the current-transformer characteristics. If a high-speed differential relay is desired, transient, as well as steady-state characteristics must be studied. The reason for this is the probability of false differential current flow occasioned by d-c saturation of a current transformer excited by an offset current.[1]

There has been a gradual improvement in the steady-state characteristics of current transformers. Also, the transient problem has been attacked by increasing the size of the core, by inserting air gaps in the magnetic circuit,[2] or by eliminating the iron entirely.[3] By and large, however, the major portion of differential-relay development has been concerned solely with the design of relays capable of overcoming the inherent deficiencies of standard current transformers. There is no better example, in support of this statement, than the protection of generators against internal faults.

As of today, what better protection could be imagined than the very first instantaneous overcurrent relay, connected differentially, if the currents supplied to the relay were true representations of the actual primary currents? Actually, however, there have been at least five generator protective-relay developments, all concerned with the inadequacy of the current supply. Briefly, the chronology has been somewhat as follows:

(*a*). Time-overcurrent relay connected differentially.[4,5] False current-transformer output requires high minimum operating current, or long operating time, or both, in order to prevent improper relay operation.

(*b*). Percentage- or ratio-differential relay (relatively slow speed).[6] Such a relay is provided with a restraining force dependent upon the sum of the currents entering and leaving the zone of protection. During a fault external to the protected zone, it operates only if one current is a certain percentage above the other (slope). Figure 1, curve *A*, shows the operating characteristic of one such relay.

(*c*). High-speed percentage- or ratio-differential relay. Such relays have had only limited success because of the high false differential current possible, as above noted, during transient conditions.

(*d*). Harmonic-current-restrained differential relay.[7] Although transformer and bus relays of this type have been successfully applied, the harmonic-current-restraint principle has never been extended to generator protection because of design limitations attending the requirements of high sensitivity and ability to withstand heavy fault currents.

(*e*). Relay utilizing both (1) variable ratio (variable slope) and (2) the out-of-phase relationship between the differential and restraining currents during heavy through faults[8] (Figure 1, curve *B*) shows a variable-slope characteristic with all currents in-phase.

(*f*). A new differential relay, described herein, the restraining force of which is obtained by means of a principle heretofore unused. It restrains in response to the product of the currents entering and leaving the protected zone, and, consequently the new principle is called "product restraint." In addition, the relay has a slope which is variable and which increases very rapidly above approximately twice normal current (Figure 1, curve *C*).

Limitations of Variable Slope With Summation Restraint

In order to appreciate fully the principle, product restraint, it is necessary first to consider the limitation of variable ratio or slope as applied to a relay using the orthodox summation restraint.

Variable ratio or slope, as the names imply, signifies that the slope of the through-fault operating characteristic continuously changes, as shown by curve *B* of Figure 1. In order to be really effective, however, the slope should increase rapidly above maximum full-load current of the machine, perhaps as indicated by curve *C* of Figure 1. To obtain such a relay characteristic, it is apparent that, as the current increases, the operating force must become deficient relative to the restraining force. However, assuming summation restraint, a relay with this characteristic could fail to operate under certain internal fault conditions. For example, if the internal fault is fed only by the protected generator (that is no external source), the restraining and the differential currents are identical. Figure 2 shows curves for the operating and restraining forces of a relay with a variable slope, summation restraint, and identical restraining and operating currents. For currents above the intersection of the curves, the relay will fail to operate, because the restraining force is in excess of the operating force.

For simplicity, it was considered that the curves of Figure 2 were based upon an internal generator fault without an external source of feed. Actually, the danger of failure to operate may be greater when there is an external source. With equal currents fed from the protected generator and an external source, the restraining force of a summation restraint relay is theoretically zero. However, the current from the external source may be much greater than the current supplied from the protected generator (for example: there may be several generators on the same bus). Consequently, the restraining force may be much greater when there is an external source than when there is not, and there may be a greater possibility of nonoperation. For example, assume a generator bus with four identical generators connected to it (and no other source of power). The total fault current during a bus fault is four times the current supplied by each generator. If the fault is on the leads of a generator (within its differentially protected zone), the differential current is four times and the net restraining current is twice the respective currents for a fault on the generator leads

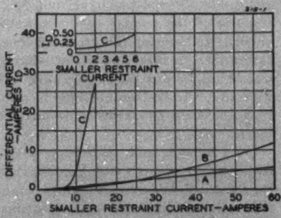

Figure 1. Through-fault operating characteristics of three kinds of differential relays

Restraining and differential currents in phase

Paper 43-15, recommended by the AIEE committee on protective devices for presentation at the AIEE national technical meeting, New York, N. Y., January 25-29, 1943. Manuscript submitted October 30, 1942; made available for printing November 27, 1942.

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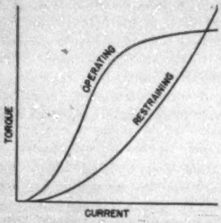

Figure 2. Torque curves of saturated operating element and unsaturated restraining element (summation restraint illustrating possible failure to operate on internal fault)

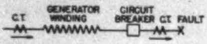

Figure 3. Directions of currents in current transformers during external fault

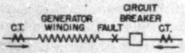

Figure 4. Directions of currents in current transformers during internal fault

with no external source. Relative to the current at the intersection of the curves of Figure 2, doubling the restraining current increases the restraining force to four times, whereas increasing the operating (differential) current to four times increases the operating force by much less than four times. Hence, the increase in restraining force relative to operating force increases the possibility of failure to operate when there is an external source.

Product Restraint

In order to retain the desired through-fault characteristic, curve *C* of Figure 1, and yet provide positive operating margin during an internal fault, it is necessary that the restraining force be greatly reduced, or, preferably, entirely eliminated during an internal fault. The latter result can be accomplished by restraining in response to the product of the currents entering and leaving the protected zone, rather than to their sum. Thus, during an internal fault fed only by the protected generator, the restraining force is zero, since one of the two terms of the product is zero.

For a through fault, the relative polarity of the restraining winding is such that the force is in the contact-opening direction. For an internal fault, assuming that the generator is connected to another source of power, the relative direction of the fault currents is reversed so that the so-called restraining windings will actually produce a contact-closing force. Thus, the windings which provide the restraining force during a through fault, produce, in so far as fault current is concerned, either an operating force or no force during an internal fault.

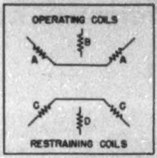

Figure 5. Coil locations in product-restraint differential-relay element

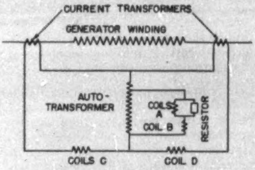

Figure 6. Schematic diagram showing relay and auxiliaries for one phase

A somewhat different conception of the operation of this relay results if the relay is considered to be primarily a directional relay. Figure 3 shows that, for an external fault, the current flow is in the same direction in the current transformers on opposite sides of the generator winding. Figure 4 shows that the short-circuit currents in the two current transformers are in opposite directions during an internal fault.

If Figure 4 were true under all conditions, a current polarized directional relay, one winding connected to each current transformer, would provide adequate protection against internal faults. However, there are two conditions during which Figure 4 is in error. First, there may be no external source, in which case the directional relay would fail to operate. Second, load current appears to the directional relay to be an external fault of relatively low current magnitude as in Figure 3. If the internal fault current is low, the load current may outweigh the fault current, thereby preventing the current reversal required for relay operation.

In order to provide sensitive operation with load current flowing and to permit operation when there is no external source of power, a differentially connected overcurrent relay can be added to the directional relay, both relays acting upon a single shaft. Although it is necessary that the overcurrent element be sensitive in order that low values of short-circuit current may overcome the effect of load current, it is not necessary that the overcurrent element torque ever be greatly in excess of the restraining torque of the directional element under maximum load current conditions. This fact permits sharp saturation of the differential current circuit, thereby causing the relay to be stable during external faults, even with the large false differential currents which may be present at the higher current levels.

Figure 7. Three-phase product-restraint differential relay

Description of Relay

The relay unit using the product-restraint principle is of the eight-pole, induction cylinder construction previously described before the Institute.[9] The available poles permit both the differential-operating and the product-restraining functions to be accomplished in a single unit. Although a single relay unit is described, it should be understood that three such units are required for a three-phase circuit.

Figure 5 shows the locations of the various coils on the eight-pole structure. The upper three coils, denoted by *A* and

B, are the differential-operating coils, while the lower three coils, *C* and *D* provide the product restraint.

Figure 6 shows schematically the connections of the various coils to the current transformers spanning the protected zone.

Figure 6 indicates that the differential current is not supplied directly to the upper coils of Figure 5, but, rather, indirectly through an autotransformer. The autotransformer, which saturates at a relatively low current, provides the greatly increasing slope characteristic as the through-fault current increases. It also permits the use of relatively large wire in the differential circuit, thereby facilitating testing at the higher currents without the overheating usually encountered in testing generator differential relays.

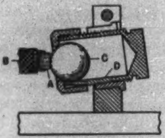

Figure 8. Cross-sectional view of relay backstop

It will be noticed that a resistor is used in the circuit of coils *A*. This resistor is for the purpose of shifting the phase of the current in the outer poles to obtain a torque-producing component. The necessary phase shift of the product-restraining coils is obtained easily in the relay unit itself by means of a flux-shifting copper tube.

Figure 7 shows a three-phase relay, all auxiliaries of which are mounted behind the relay units. Those familiar with this construction will recognize the usual bounce-suppressing contact on the left-hand side of the upper structure of each relay unit. Because of constructional similarity, there appears to be a contact on the right-hand side also. However, the tube or "barrel" on the right-hand side is a new type of backstop construction which prevents closing the tripping contacts upon the release of a through-fault restraining force. This backstop has another desirable quality in that it acts as a shock-suppressing device. The relay can be set as low as 0.1 ampere at no load, as indicated by the insert of Figure 1. However, for practical purposes 0.2 ampere is sufficiently low, and a relay calibrated at this value will be safe against false operation, even under unusual shock conditions.

Figure 8 shows a cross-sectional view of the backstop. Felt pad *A*, attached to the moving contact arm *B*, normally rests against stainless-steel ball *C*, which in turn is free to roll in race *D*. The race is inclined sufficiently to cause the ball to retain the contact arm in the position shown under normal conditions with full load current flowing. Upon the occurrence of an external fault, the restraining torque causes the moving contact arm to force the ball to the right, opening the contact gap and thereby effectively preventing the stored energy from causing contact closure upon the release of that energy when the external fault is cleared. Regarding the shock-resistant quality of the backstop, consider first the action of a solid backstop following a blow from the right. The energy of the impact would be transmitted, with practically no attenuation, to the moving contact arm, causing it to move to the left, or contact-closing direction. With the backstop of Figure 8, a blow from the right causes race *D* to move sharply to the left. However, since the ball is free to move relative to the race, the inertia of the ball causes it, together with the moving contact, to remain substantially motionless in space.

Relay Characteristics

The typical through-fault operating characteristic of this relay is that previously referred to as the "desired characteristic," curve *C* of Figure 1. The insert of Figure 1 shows the same curve at low current levels and can be used for internal faults, the abscissas then indicating the load currents. Any further attempt to depict the internal fault characteristic would be quite meaningless, since, as previously explained, a flow of short-circuit current from an external source causes an operating force to be produced by the so-called restraining windings and thereby, in effect, reduces the minimum operating current of the relay.

The relay is of the high-speed type as indicated by the time-current curve, Figure 9.

The burden of the restraining coils is very low, approximately 0.08 ohm total. The burden of the operating-coil circuit is a variable, reaching a maximum of 65 ohms. This seems high but is actually comparable with, if not less than, the burdens of other generator differential-relay operating circuits.

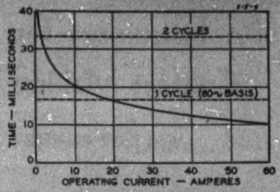

Figure 9. Typical time-current curve of relay with restraint corresponding to full load

Conclusion

A generator differential relay has been described which approaches the ideal of fast sensitive protection equivalent to that attainable with perfect current transformers. Because the product-restraint principle permits a variable slope without its usually attendant "blind-spot" danger, the relay can be applied with a minimum of effort.

Thus far, the generator differential relay is the only relay to which the product-restraint principle has been applied. This development was completed before December 7, 1941, but further extension of product restraint such as to transformer, pilot wire, or bus differential relays, must await normal times.

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An Instrument for the Determination of Contact Making and Breaking Time

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Synopsis: This paper describes an electronic instrument for the measurement of the arcing time of a contact closing or opening a circuit. This measurement is accomplished by permitting a current of constant value to flow into a capacitor during the time that an arc exists across the contacts under observation; the charge accumulated on the capacitor is therefore a measure of the arcing time. The instrument also contains electronic means of measuring the charge on the capacitor, by determining the voltage existing across it at the end of the measurement. The circuit serving this purpose is a Wheatstone bridge with a tube in one arm, to whose grid the capacitor is connected, and a microammeter in the diagonal which indicates directly the amount of unbalance produced by the capacitor voltage.

The application of the instrument to the investigation of a three-pole contactor is described.

FOR the investigation of contact-making devices such as contactors, relays, and snap switches under various load conditions, a device for the quick determination of the operating time of a contact seemed desirable. As many investigators in the field have found that the making of a circuit often represents a harder service for the contact-making device, because of contact bounce, than the breaking of the circuit, the instrument described in the following paragraphs was originally designed to permit the quick measurement of the bouncing time of a contact, without the need of employing an oscillograph.

The underlying principle of the instrument is very simple; a current of constant and predetermined value is made to flow into a capacitor during the time that the contact is being made or broken, that is, during the time that an arc exists across the contacts. The charge accumulated on the capacitor and with it the voltage appearing across it are therefore proportional to the time of arcing, since the charge is the time integral of the current flowing into the capacitor.

Obviously, mechanical relays cannot be used to connect and disconnect the measuring capacitor to and from a suitable charging circuit during the arcing time of the contact-making device under observation, since the error introduced by these relays would be of the same order of magnitude as the time to be measured. A vacuum tube, on the other hand, is a practically inertialess relay and thus offers a convenient solution to this problem. Furthermore, if a capacitor and a tetrode or pentode, properly biased, are placed in series across a fixed d-c voltage, the current which will flow in this series combination, that is, the charging current of the capacitor, will remain practically constant, as a result of the flat plate characteristics of these types of tubes, until the voltage across the tube has been reduced to a relatively low value. A nearly linear relation between time and accumulated charge is thus obtained.

Figure 1 shows that part of the circuit which accomplishes the charging of the capacitor. It is seen that the capacitor is placed in series with a pentode across a suitable d-c voltage. The capacitor is by-passed by the meter M_1 and the normally closed switch S_2, which is opened just before a measurement is taken and is kept open only until the measurement is completed. The grid bias of the pentode consists of voltage drops caused by currents flowing in the resistors R_1 and R_3, as will be explained in more detail later. During the adjustment of the charging current to a suitable value, as well as during the actual arcing time, no current flows through R_3, and the bias voltage consists only of the voltage drop e_0 across R_1. This voltage is of constant value and such that it permits plate current to flow, the amount being adjustable to any desired value by varying the screen grid voltage obtained from the potential divider P_1. If switch S_2 were opened at this moment the plate current, which up to this instant was indicated by the meter M_1, would flow through the capacitor, charging it linearly with time. Switch S_2 is not opened, however, until additional bias, furnished by a current flowing through resistor R_3, has biased this tube beyond cutoff.

It now becomes evident that means must be provided to maintain a current flow through R_3 sufficient to bias the charging tube to cutoff, except during the arcing time of the contacts under observation, at which time the current flow through R_3 should be reduced to zero, so that the previously adjusted charging current will flow into the capacitor, as already explained. Furthermore, the instrument should indicate directly the charge accumulated on the capacitor during this period. The circuits accomplishing this are shown in Figure 2.

A conventional rectifier system furnishes the necessary d-c voltage; two voltage-regulator tubes in series keep the supply voltage constant. Previous to an actual measurement switch S_1 is opened, which prevents any possible current flow through R_3 and thus permits the adjustment of the plate current, as outlined above. S_2 is kept closed, of course, during this operation. Consider now the two control tubes V_1 and V_2. Resistance R_4 is common to both plate circuits. If either one of these tubes is made sufficiently conducting, the current flow in this resistance will make point H negative with respect to point F, or, in other words, the voltage across the control tubes will be less than the voltage across R_3. With switch S_1 closed, current will therefore

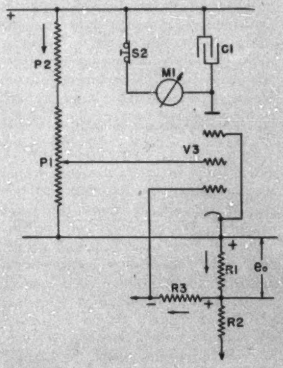

Figure 1. Detail of capacitor charging circuit

V_3—6C6 tube
C_1—0.1-, 0.25-, 0.5-, 1.0-microfarad mica capacitor (depending upon desired range)
R_1—250-ohm 10-watt wire-wound resistor
R_2—5,000-ohm 10-watt wire-wound resistor
R_3—150,000-ohm 1-watt carbon resistor
P_1—5,000-ohm wire-wound potential divider
P_2—1,000-ohm wire-wound potential divider
M_1—0–1-milliampere d-c meter

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flow from point *F* through R_3 and the diode to point *H*. This provides the additional bias voltage needed to cut off the current in the charging tube. If the current flow through R_4 is reduced to such a value that point *H* becomes positive with respect to point *F*, the current through R_3 ceases, since the diode prevents a reversal of current, and the charging tube passes the previously adjusted current.

The contact to be investigated in series with a suitable load is connected across a d-c source with a polarity as shown in Figure 2. A resistor R_{10}, acting similarly to a shunt, is included in the circuit. Its resistance should be such as to produce a voltage drop of at least 0.25 volt with the lowest value of load current. This voltage drop furnishes a negative bias to one of the control tubes, whereas the second control tube receives a negative grid voltage when a voltage exists across the contacts. It is evident, therefore, that before the contact closes, V_2 will be biased negatively, whereas V_1 will have zero grid voltage (since no current flows in the shunt); V_1 will consequently be conducting, its plate current passing through R_4. After the contact is established firmly, however, V_1 will become nonconducting as a result of the bias produced in the shunt, whereas V_2 will now become conducting. The inclusion of a dry cell in the grid circuit of this tube serves to overcome any possible effects caused by contact resistance. It is seen that during the period of arcing both control tubes will be biased negatively, V_1 by the voltage drop across the shunt and V_2 by the arc voltage existing across the contacts. Current flow through R_4 is therefore reduced or ceases entirely during the arcing period, which in turn permits current flow in the charging tube, as explained above. Since S_1 is opened just before a measurement is taken, a charge will accumulate on the capacitor.

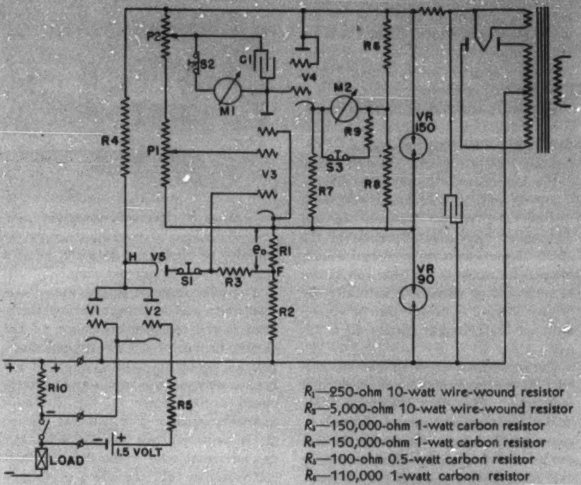

Figure 2. Circuit diagram of instrument

V_1—6F5 tube
V_2—6F5 tube
V_3—6C6 tube
V_4—38 tube
V_5—6H6 tube
C_1—0.1-, 0.25-, 0.5-, 1.0-microfarad mica capacitor (depending upon desired range)

R_1—250-ohm 10-watt wire-wound resistor
R_2—5,000-ohm 10-watt wire-wound resistor
R_3—150,000-ohm 1-watt carbon resistor
R_4—150,000-ohm 1-watt carbon resistor
R_5—100-ohm 0.5-watt carbon resistor
R_6—110,000 1-watt carbon resistor
R_7—225,000 1-watt carbon resistor
R_8—One-megohm 1-watt carbon resistor
R_9—100-ohm 0.5-watt carbon resistor
R_{10}—Shunt (see text)
P_1—5,000-ohm wire-wound potential divider
P_2—1,000-ohm wire-wound potential divider
M_1—0–1-milliampere d-c meter
M_2—0–200-microampere d-c meter

The remaining circuit element serves to measure the voltage across the capacitor. Any device for this purpose—unless a ballistic method were to be considered—must draw no appreciable current for the measurement. An electrostatic voltmeter would be the ideal instrument, of course, but for the relatively low voltage to be measured this type of instrument is very delicate. Special tubes, called electrometer tubes, such as General Electric *FP-54* are available, the grid current of which is about 10^{-15} amperes, with a corresponding input resistance of about 10^{16} ohms. Since a charged capacitor will lose its voltage as a result of its own leakage resistance, however, it is obvious that not much is gained by employing an instrument with an input resistance two or three powers of ten higher than the leakage resistance of the capacitor and its associated circuits.

Of the common radio tubes, the type 38 has a grid current of about 10^{-9} amperes when operated with a filament voltage of four volts and with about 30 volts on the screen and plate. This tube is used in the instrument forming one arm of a Wheatstone bridge. The rate of change of capacitor voltage, caused by the combined action of leakage and grid current, is so low in the instrument that the drift of the meter does not exceed one scale division in three or four seconds, so that ample time for taking a reading is available.

Before a measurement is made, the bridge is balanced (that is, meter M_2 is brought to zero) by adjustment of potential divider P_2, which furnishes grid voltage for V_4. Switch S_2 is closed during this adjustment, of course. The meter M_2 can once and for all be calibrated in terms of voltage across the capacitor by simply applying known voltages across it, keeping S_2 open during this calibration. With the capacity of the capacitor known, the charging current adjusted to a known value, and the meter M_2 calibrated in terms of capacitor voltage, all factors for the calculation of the duration of current flow are therefore known. Since the measuring circuit is highly degenerative, because of cathode resistor R_7, the indications of the meter M_2 are practically linear with respect to the capacitor voltage. As a matter of interest, it may be stated that a capacitor voltage of approximately 20 volts produces full-scale deflection on the 200-microampere meter M_2.

Since the action of the instrument depends on the negative bias applied to the two control tubes, the contact under observation can be tested only with direct current. At first sight this might be considered as a serious limitation of the instrument. The addition of relatively simple rectifying circuits, however, would make the use of the instrument on a-c circuits possible. This was not deemed necessary, or even desirable, for the following reason: the performance of a contact in an a-c circuit will naturally depend on the exact instant of the a-c cycle when the contact opens or closes. Thus, a number of readings taken on the same contact will vary widely, unless care is taken to synchronize the contacts to a particular instant of the cycle. If the test is made with direct current, the latter can be adjusted to any value, thus simulating any desired "instantaneous" value of alternating current.

Figure 3 is a picture of the instrument. A tap switch not shown on the diagram is provided to insert various values of capacitance, thus changing the time range of the instrument. These values are 1.0, 0.5, 0.25, and 0.1 microfarad, and, with the charging current adjusted to one milliampere, full-scale deflection of the meter M_2 represents 20, 10, 5, and 2 milliseconds respectively. These values can be increased or decreased, naturally, by further changes of capacitance or adjustment of the charging current to a different value.

The instrument has been used successfully in the investigation of relays and contactors. The effect of changes made in the mechanical structure of the contactor and the effect of changes in the load can be studied quickly without having to wait for the development and measurement of films taken with an oscillograph. The instrument can also be used for purposes other than determining the arcing time of a contact. Thus, the operating time of relays or solenoids can be determined by inclusion of contacts at the beginning and the end of the travel, furnishing suitable voltages to the terminals of the instrument in place of those produced by the shunt or the arc. The measurement of the L/R ratio of an inductive load can also be carried out with the aid of a relatively simple external circuit.

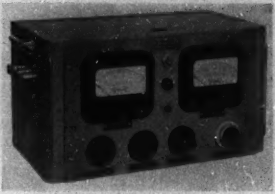

Figure 3. Electronic time gauge

A specific example of the use of this instrument and the benefits derived therefrom is the recent investigation of the closing bounce of a 1,200-ampere three-pole a-c contactor. Observations of the bounce were made as each set of contacts closed a 100-ampere 115-volt d-c noninductive circuit. The nominal voltage of the operating coil was 208 volts, but measurements were included at 195 and 220 volts, so that the effect of voltage variations might be ascertained.

Duration of bounce of the set of contacts behaving most poorly under the original adjustment of the contactor was 4.8, 5.6, and 6.4 milliseconds with coil voltages of 195, 208, and 220 volts respectively.

By increasing the initial contact pressures from the original value to a somewhat higher one, the respective bouncing times were reduced to 2.8, 3.4, and 4.5 milliseconds.

After increasing the wear allowance of the two outside sets of contacts slightly, setting the middle movable contact ahead of the others by one-eighth inch, and closing an orifice in the dashpot which allowed quick motion at the start of the armature stroke, the durations of bounce were 1.2, 1.4, and 1.7 milliseconds, respectively.

And, finally, after decreasing the clearance between the piston and cylinder of the dashpot assembly, the bounce durations were found to be 0.3, 0.3, and 0.5 millisecond.

Within a few hours time, therefore, it was possible to observe the effect of several variables upon the action of the contactor and to reduce the duration of closing bounce to about seven per cent of its original value. To accomplish this by means of an oscillograph would obviously have required a considerably greater expenditure of time and effort.

In another case, it became necessary to reduce the time required for a small relay to interrupt a circuit. Observations were made thereupon of the duration of the opening arc both with and without blowout assemblies. The arcing time without a blowout, for example, was found to be 14 milliseconds, with a coil-type blowout 7.5 milliseconds, with a tool steel magnet blowout 2.6 milliseconds, and with an Alnico magnet 2.2 milliseconds. The effectiveness of numerous magnet designs was thus readily observed, and an educated selection made of the most suitable one for the purpose.

Correlation of System Overvoltages and System-Grounding Impedance

AIEE COMMITTEE ON PROTECTIVE DEVICES
Working Group on Correlation of System-Grounding Impedance

Synopsis: This report summarizes available data and engineering opinions on the subject of power-system overvoltages, particularly as to their correlation with system-grounding impedance. It is hoped that the report will be useful in connection with overvoltage problems for all types of power-system apparatus.

THE characteristics of power-system connections with respect to ground have long been recognized as having an important bearing on many phases of system operation, including overvoltages between line conductors and ground.[1,2] As early systems (which were of the ungrounded type) became extended, operating experience became unsatisfactory because of the occurrence of multiple faults at the same or different locations resulting from an initial ground fault. Since troubles of this nature were frequently thought to be associated with arcing faults, such faults became broadly classified as "arcing grounds." Difficulties thought to be attributable to this type of fault were one of the main factors leading to the general practice of grounding power systems. However, the correlation of system overvoltages with the various forms of grounding and the values of grounding impedances was so complicated that many years elapsed before much progress was achieved, and even now many conditions are incompletely classified.

Causes of System Overvoltage

In a study of system overvoltage phenomena and their correlation with system-grounding impedance, it is convenient to list the causes. Causes of system overvoltage (other than lightning) may be classified as follows:

1. Conditions resulting in sustained overvoltage:
(*a*). Increase of operating voltage level.
(*b*). Generator overspeed.
(*c*). Self excitation.

2. Circuit unbalances:
(*a*). Open phase or phases.
(*b*). Unequal pole opening of breakers.
(*c*). Fuse blowing.

3. Faults:
(*a*). Solid.
(*b*). Resistive.
(*c*). Arcing with forcing of current zero.
(*d*). Arcing with restriking.
(*e*). Distortion from saliency effects.

4. Switching operations:
(*a*). Simple.
(*b*). Arcing with forcing of current zero.
(*c*). Arcing with restriking.
(*d*). Neutral inversion.

5. Extraneous influence from other sources:
(*a*). Conduction, metallic cross or ground potential, from other power circuits.
(*b*). Induction, magnetic or electric, from other power circuits.
(*c*). Electrostatic charging of ungrounded systems by wind-borne particles, such as sand or dust.

Frequently overvoltages result from combinations of two or more of the foregoing factors. In order to define the various causes of system overvoltages, a brief discussion of each type of phenomenon is given.

CONDITIONS RESULTING IN SUSTAINED OVERVOLTAGE

On ordinary power systems overvoltages at fundamental frequency may be produced by:

(*a*). Increase of operating voltage level.
(*b*). Generator overspeed from loss of load.
(*c*). Self excitation of machines.

Five per cent tolerance for emergency operation is the maximum overvoltage above rated circuit voltage recognized by the joint committee on preferred voltage ratings of the National Electrical Manufacturers' Association and the National Electric Light Association.[3] Application procedures for standard rated apparatus are based on this maximum circuit voltage. Sustained overvoltages generally are, under normal conditions, of little importance in regard to voltage stress on apparatus and functioning of protective equipment, but they may become important during or following an abnormal condition, such as produced by a lightning discharge or fault to ground.

It is believed that generator overspeeding and loss of load as single factors without faults or unbalances will seldom give rise to dangerous overvoltages. However, overspeeding may be a contributing factor, especially if it occurs during ground faults. The voltage amplitude is a function of governor characteristics and of means for controlling the excitation. With water-wheel generators without regulation of speed or excitation, the maximum voltage may be as high as 160 to 180 per cent of normal under full-load rejection; whereas with machines under control of modern governors and generator voltage regulators, the voltage will probably be limited to 120 to 140 per cent of normal.

Self-excitation is an infrequent source of overvoltage on systems. It can occur, however, under abnormal circuit conditions which leave relatively small synchronous or induction machines connected to circuits with relatively large capacitance, supplied from lines or capacitors. The possibility of setting up the conditions for self-excitation should be considered in the various stages in the disconnection of faulted circuits.

Sustained overvoltages under unbalanced load conditions may also be produced by saliency effects in machines.[4,5] Solid faults, other than balanced three-phase, sometimes cause the generation of dangerously high harmonic overvoltages in addition to increased fundamental-frequency voltages. These overvoltages are obtained when the system electrical constants and loads are of such proportions as to provide partial resonance to one of the lower harmonics. Magnitudes of overvoltage of this type depend not only on the type of unbalanced fault and the method of grounding but on other system constants as well. Such phenomena are most frequently encountered on systems with water-wheel generators without adequate damper windings, under conditions in which the machines are connected to unloaded or lightly loaded transmission lines with appreciable capacitance and subjected to unbalanced faults.

CIRCUIT UNBALANCES

On three-phase power systems the opening of one or more phase conductors will, under some circuit conditions, produce large overvoltages between line conductors and ground. These voltages are normally observed on the part of the

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Personnel of working group: W. W. Lewis, *chairman;* J. R. Eaton, R. D. Evans, P. A. Jeanne, J. W. Milnor, H. A. Peterson.

system which is partially separated from the main power source, under conditions with circuit constants such as to provide approximate resonance at fundamental frequency for some path between line conductors and ground. In general, these voltages are encountered on isolated-neutral systems or on systems having relatively high impedance between phase conductors or between phase conductors and ground. These circuit unbalances may arise from open-phase operations, such as occur as a result of conductor breaking, fuse blowing, or the opening of one or more breaker poles. Extreme conditions are produced with two synchronous systems which are out of step but are connected on one phase, the highest voltages being produced with both of the systems ungrounded and one of them subjected to a line-to-ground fault on either of the other two phases.

FAULTS

Faults cause the most frequently encountered and usually the most important power-system overvoltages. When a balanced three-phase system is subjected to a single or double line-to-ground fault, overvoltages may be produced on the unfaulted phase or phases. The amount of overvoltage may readily be calculated if the system constants, including nonvariable fault resistances, are known. If the fault takes place through an arc, the overvoltages may be increased by two phenomena:

1. The forcing of current zero.
2. Restriking.

Both of these phenomena are discussed at length subsequently.

SWITCHING OPERATIONS

Transients resulting in overvoltages of important magnitudes are produced by simple circuit changes involving a single transition from "make" to "break" or vice versa, as may occur on the energizing or de-energizing of a line section. Circuit changes may produce unbalances and thus result in overvoltages resulting from both unbalances and circuit changes. When circuit changes take place through an arcing path, there is also the possibility of increasing system overvoltages through the action of "forcing current zero" or of restriking as mentioned previously in connection with arcing faults. Since switching operations can take place in many ways in opening or closing the several poles of the circuit breaker, and since many transients may be produced for each of these steps, many combinations are possible. Switching operations are normally encountered in combination with other factors including faults and circuit unbalances, so that such transient overvoltages are usually the result of a combination of several factors.

EXTRANEOUS INFLUENCE

Occasionally a circuit is subjected to voltages which are impressed by conduction from metallic cross or ground-potential effects or by electric or magnetic induction from an adjacent power circuit, usually of higher voltage and capacity. Electrostatic charging of conductors during sand, dust, and sometimes snow storms, occurs in some regions. These effects are readily studied in a particular case if this cause of overvoltage is recognized.

Correlation of Overvoltages With System-Grounding Impedance

The various types of system overvoltages when appearing separately can be correlated with system-grounding impedance, except those that produce overvoltage under balanced conditions, such as emergency operating voltage, sudden loss of generator load, self-excitation of machines, and saliency effects (on line-to-line faults). However, in specific cases each of these types can be so correlated when the overvoltage occurs in combination with a fault to ground or other unbalances with respect to ground.

For correlation with system-grounding impedance, it is convenient to use a different classification from that just used for listing the causes of system overvoltage, basing the classification upon the type of transient phenomenon involved:

1. Simple circuit changes:
   (*a*). Ground faults.
   (*b*). Switching operations.
2. Complex circuit changes through arcing paths:
   (*a*). Forcing current zero.
   (*b*). Restriking arcs.

By a "simple circuit change" is meant a single circuit change that takes place with abrupt transition from "circuit open" to "circuit closed" condition or from "circuit closed" to "circuit open" condition, the opening taking place at a "normal current zero." By a "complex circuit change" is meant one that takes place through an arcing path which introduces additional circuit voltage transients either by

1. Forcing current zero.
2. Re-establishment of a conducting path as with intermittent arcs.

Complex circuit changes in general involve the superposition of a second transient before the effect of the first transient has disappeared.

SIMPLE CIRCUIT CHANGES

Calculation of Fundamental-Frequency Overvoltage

The simplest and most important, although not necessarily the most severe, types of system overvoltage produced on a power system occur as the result of the application of a single or double line-to-ground fault on a normal balanced three-phase power system. Upon such an occurrence, the sound phases may rise to fundamental-frequency overvoltage with respect to ground as determined by the sequence impedance constants of the system. In addition, there is a transient term which, in simple systems without loss, is equal to the difference between the fundamental voltages before and after the application of the fault. The fundamental-frequency components of overvoltage are of controlling importance in lightning-arrester applications, and the ratios of the fundamental-frequency voltages under fault condition to the normal voltages permit estimating the transient overvoltage magnitudes.

The fundamental-frequency overvoltages that appear on a balanced three-phase system upon the application of a single or double line-to-ground fault are readily calculated for the point of fault by well-known methods based on symmetrical components using the normal voltage before the fault and the sequence impedance constants of the system as viewed from the fault location, including X_0, X_1 and X_2, and the fault resistance or resistances.[6,8,9,11] Voltages at locations other than the point of fault will not as a rule appreciably exceed those at the point of fault, except possibly in case of a very long line, the far end of which has an isolated neutral.

It has been found possible to compare the grounding characteristics of systems by expressing overvoltages to ground in terms of normal voltage (usually on a line-to-neutral voltage base) and by expressing the zero-sequence resistance and reactance as ratios to the positive-sequence reactance.[6] When this procedure is adopted, it is possible to group systems which give the same ratio of overvoltage, although the characteristics of the systems may be radically different. In addition, it is sometimes desirable to give the ratio of the zero- to positive-sequence capacitive reactances. The effect of fault resistance is to increase the magni-

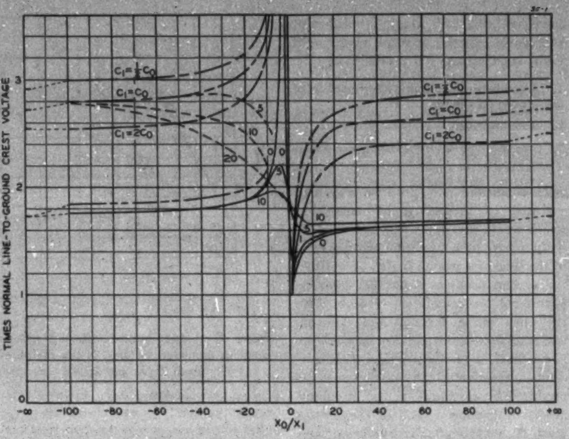

Figure 1. Transient and sustained fundamental-frequency voltages on unfaulted phases during single line-to-ground faults on a three-phase system

$Z_1 = Z_2 = R_1 + jX_1 = 0 + jX_1$
Z_1 = positive-sequence impedance
Z_2 = negative-sequence impedance
R_1 = positive-sequence resistance
X_1 = positive-sequence reactance (subtransient)
X_0 = zero-sequence reactance
C_1 = positive-sequence capacitance
C_0 = zero-sequence capacitance
R_f = fault resistance
All viewed from the fault location

Solid line curves—maximum sustained fundamental-frequency voltage to ground on unfaulted phases with solid single line-to-ground fault. No fault resistance. Numbers on curves are R_0/X_1 ratios

Dash-dot-dot-dash curve—maximum sustained fundamental-frequency voltage to ground with the value of fault resistance which gives the maximum voltage to ground for any value of R_0/X_1. Curve plotted only on negative side, as fault resistance is not as important on the positive side, and additional curves would lead to confusion

Dash-dot-dash curves—maximum transient voltage to ground on unfaulted phases following sudden application of a single line-to-ground fault. $R_0/X_1 = 0$

Dashed curves—maximum transient voltage to ground on unfaulted phases following sudden application of a single line-to-ground fault. $C_1 = C_0$. Numbers on curves are ratios of R_0/X_1. The dash-dot-dash curve for $C_1 = C_0$ is one of this family of curves for $R_0/X_1 = 0$

tude of overvoltage under some conditions and decrease it in others.

Correlation With Different Forms of System Grounding

The preceding discussion has shown the complexity of the problem of correlating overvoltages with system-grounding impedances. To avoid some of these difficulties it has been found expedient to consider five classes of system grounding as follows:

(*a*). Isolated neutral system.
(*b*). Ground-fault neutralizer system.
(*c*). Reactance grounded system.
(*d*). Resistance grounded system.
(*e*). Solidly grounded system.

(*a*). *Isolated-Neutral System.* For isolated-neutral systems of small capacitance to ground and small extent the maximum line-to-ground voltage for a single line-to-ground fault will be equal to the line-to-line voltage before the fault. As the capacitance to ground increases with increasing extent of the system, the maximum voltage to ground will rise above the normal line-to-line voltage and will reach very high values for systems with the ratio of X_0/X_1 in the vicinity of -2. This condition corresponds to approximate resonance in a circuit consisting of the positive- and negative-sequence reactances and the zero-sequence capacitive reactance, all in series. For a double line-to-ground fault the voltages on the sound phase will in general be less than for a single line-to-ground fault except for the range of X_0/X_1 between -2 and 0. In this range there is the possibility of resonance in a circuit consisting of the positive-sequence reactance in series with the negative-sequence and zero-sequence capacitive reactances in parallel. In considering the magnitude of the system overvoltages, it is, of course, essential to consider all circuit conditions and fault locations.[23] Usually the highest overvoltages are encountered on some abnormal condition which leaves a small amount of generating capacity connected to a system of large extent. Practical curves[6,11] for the estimation of the magnitude of overvoltages on isolated-neutral systems subjected to ground faults are available for zero and unit ratios of line resistance to X_1.

(*b*). *Ground-Fault Neutralizer System.* The voltage from a sound phase to ground on a ground-fault neutralizer system will rise to the normal line-to-line voltage in the event of a single line-to-ground fault. For double line-to-ground faults the voltages from the sound phase to ground will be less than for a single line-to-ground fault. If the ground-fault neutralizer system is out of tune, there will be some increase in the voltage to ground on the sound phase, depending upon the constants of the system and the departure from the tuned condition.

(*c, d, e*). *Impedance-Grounded Systems.* When power systems are grounded, the layouts are normally made to provide a ground-fault current which is greater than the zero-sequence charging current for normal line-to-neutral voltage on the zero-sequence network. This, of course, is done to provide sufficient current to secure positive relay action on ground faults, which is one of the principal advantages of the grounded-neutral system. When power systems are laid out in this manner, the systems generally have positive or inductive reactance from line to ground in the zero-sequence network when viewed from the fault location. This is in contrast to the negative or capacitive reactance in the zero-sequence network for the isolated-neutral system. For all impedance-grounded systems, including resistance, reactance, and solidly grounded systems, it is practical to estimate the overvoltages from sets of curves[6,8,9,11] which give the maximum line-to-ground voltage for various ratios of R_0/X_1 and X_0/X_1 for single and double line-to-ground faults.

When fault resistances are of an appreciable magnitude, their effect on the overvoltages must be taken into account.[24] Fault resistance will in some cases increase and in other cases decrease the maximum line-to-ground voltages at fundamental frequency. Curves[6,11] are available which take into account the overvoltages which are plotted on the basis of the value of fault resistance giving the highest overvoltage. When fault resistances are of important magnitude, it is necessary to select the points between which the overvoltages are to be considered. If tower-footing resistance or ground resistance at the point of fault is high, it may be necessary to distinguish between the voltages from unfaulted to faulted conductor and from unfaulted conductor to ground. Thus, for protector tubes, the important overvoltage during fault may differ from the important overvoltage for an arrester application, because in one case the fault is to the protective apparatus ground and in the other case it may not be.

Sometimes it is desirable to obtain only an indication of a ground on a system. For this purpose, a potential transformer may be connected between an available neutral point and ground, or three transformers may be connected in wye with the neutral point grounded, the secondaries being connected in wye or broken delta. The latter connection is sometimes also used for metering or relaying. Either method would fall in the class of high reactance grounding. In addition the latter method leads to the possibility of another type of overvoltage known as "neutral inversion," unless suitable means for stabilizing are employed.[26]

Transient Overvoltages on Simple Circuit Changes

The application of a ground fault to a normally balanced three-phase power system gives rise to changes in the fundamental-frequency voltage as discussed in the preceding section. In addition, there is a transient voltage produced by the circuit change which is frequently taken as the difference between the initial and final fundamental voltage. This conception is accurate for a simple circuit without loss. The overvoltages will, in general, be less than these values because of decrement and because of the resolution into oscillating components of different frequencies. However, if the change in fundamental-frequency voltage is known, the natural-frequency overvoltage superposed on the fundamental frequency can be estimated, thus providing a method of evaluation sufficient for most practical cases.

Ground Faults on Unbalanced Systems

If a power system is unbalanced with respect to ground, because of either series impedances or shunt admittances, there will be a tendency to increase the voltages on some phases and to decrease them on others. If these unbalances occur at the same time as ground faults, the voltages to ground may be importantly increased.[7] These double unbalances frequently arise as the result of a ground fault, such as may be produced by

1. A conductor breaking with grounding of the broken conductor.
2. Fuse blowing.
3. The opening of one breaker pole ahead of the others.

Such conditions may produce very high voltages to ground under conditions which would give relatively low voltages if the open-phase condition did not exist at the time of a ground fault. Sometimes the system provides a parallel resonant path from the conductors of other than the open phase through the shunting paths to the open conductor beyond the phase break to ground. Under these conditions excessively high voltages may be produced by resonance action at fundamental frequency. Each of these cases must be investigated individually, and it is not practical to set up general curves for the determination of the magnitude of system overvoltages that occur under these double unbalances. The calculation of the overvoltage for a particular case of double unbalance can be carried out by the use of the method of symmetrical components, using one of the equivalent networks for simulating the double unbalances.

COMPLEX CIRCUIT CHANGES THROUGH ARCING PATHS

Circuit changes through arcing paths may give rise to system overvoltages, either as a result of

1. Forcing current zero.
2. Restriking through the previously conducting path.

These phenomena give rise to additional transients, because of the variable resistance which is characteristic of the arc, and thus introduce additional complications in overvoltage calculations.

Forcing Current Zero

If an arcing path is rapidly deionized, there may be "forcing of current zero"[12,25] by which is meant a more rapid change of current than would occur in the circuit if certain characteristics of the arc were not present. This action produces transient voltages which increase those produced by the circuit change itself. If current is flowing through the inductive elements of the circuit at the time an arc is suppressed, there is a tendency for the current to continue, completing the circuit through shunt capacitances, thus greatly increasing the voltages across them.

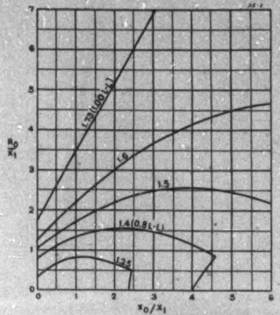

Figure 2. Line-to-ground sustained voltage chart for grounded systems, in the region of small X_0/X_1 and R_0/X_1

Figures on curves are times normal line-to-neutral voltage. In parentheses are given times line-to-line voltage for two significant values. Curves are for $X_2 = X_1$ and $R_1 = R_2 = 0$

The rate of increase of voltage and the magnitude reached depend upon the current at the time of sudden interruption, the inductance and capacitance of the circuit elements, and the dielectric strength that can be established. Such voltages can be calculated for simple circuits from the stored energy relation $LI^2 = CE^2$, using for I the value of current at the instant of sudden interruption. If considerable current is flowing and the inductance is relatively high, the transient overvoltage produced by forcing the current zero will be very large if the shunt capacitance is small. From a practical point of view, the overvoltage which may be impressed on a circuit as a result of forcing of current zero is usually limited in an arcing path because of its dielectric characteristics. The arcing path cannot instantly change from (1) low resistance corresponding to heavy current flow to (2) high dielectric strength at a very short time after the suppression of current flow. If restriking takes place, transient overvoltages may be built up or limited as discussed subsequently under the subject of "restriking".

Arcing paths in air over insulator strings and apparatus bushings rarely result in important overvoltages as a result of the forcing of current zero. However, forcing of current zero may take place in circuit breakers of either the air or oil type because of the means used to deionize the arc path rapidly. Failure of solid types of insulation may force current zero because of the high pressure built up in the fault path. In this connection it is, of course, pertinent to re-

Table I. Comparison of Various Methods of Power-System Grounding

	Isolated Neutral	Ground-Fault Neutralizer	Reactance Grounded*	Resistance Grounded	Solidly Grounded ($\frac{X_0}{X_1} < 3.0$ and $\frac{R_0}{X_1} < 1.0$, by Definition)
1. Overvoltage	Highest	Satisfactory	Satisfactory (Opinions range for generator bus from 3 to 10 and for transmission system from 3 to 40 for X_0/X_1)**	Good if neutral resistance does not exceed ground fault neutralizer ohms	Lowest
2. Ground-fault current	Very low	Negligible	Limited	Low	Highest, but seldom appreciably larger than the 3-phase short circuit current. Limit is 150 per cent of 3-phase value
3. Relaying	Difficult in most cases	Transitory line-to-ground faults are self-clearing. Relaying of other faults is satisfactory	Good	Good for low values and satisfactory for high values	Good
4. Protective level	Highest	Same as for isolated neutral	Same as for isolated neutral or better	Same as for isolated neutral or better	Lowest

* Exclusive of systems that can be classified as solidly grounded.

** Applications in the higher range may require special study.

member that, when pressure has been dissipated in this fault path, the dielectric strength is reduced below its normal value.

The interruption of current in a circuit breaker in forcing current zero is conveniently analyzed in terms of extinction voltage.[14,17] By extinction voltage is meant the voltage that appears across a circuit breaker at the end of the last half-cycle of current flow; it includes the increases in arc voltage and the increases resulting from diversion of current to shunting paths. Extinction voltages are usually relatively low but may approach the normal circuit voltage. The effect of the extinction voltage is to increase the magnitude of the transient overvoltage by an amount that is approximately equal to the extinction voltage itself.

Restriking Arcs

When current flows through any arcing path in a power system, there is a tendency for the arc to go out at every current zero and to restrike immediately or within a slight interval after normal current zero. In some cases the current is interrupted at current zero, and there is a definite "current pause" before current flow is re-established following a restrike through the arc path. This interval may be in microseconds or may last for a half-cycle of the fundamental frequency of the circuit or longer before restriking takes place.

Re-establishment of arc voltage across an arc path is controlled by the characteristic of system recovery voltage that is impressed across the arc path and tends to cause dielectric breakdown and the dielectric recovery characteristic of the arc path. If the dielectric recovery strength is at all times greater than the transient recovery voltage, then restrike will not take place. The magnitude of the resultant transient overvoltage is, therefore, greatly affected by those system characteristics that control the shape of the transient recovery voltage-time curve of the system and by the dielectric voltage-time curve of the arc path. If the arc path is of certain characteristics, restriking will take place at substantially the same potential under all conditions. Under other system transient recovery voltage and dielectric recovery characteristics, breakdown may occur under conditions for the arcs to produce cumulative oscillations[12,13] resulting in high voltage across the arcing path.

Analysis by Means of Miniature-System Setups

The preceding discussion has shown the difficulty of analyzing the effects of arcs in increasing transient voltages as a result of either forcing of current zero or as a result of restriking. In fact, the only practical method[16,18,19] available for the laboratory study of such problems is by the use of a miniature power system which is set up to simulate the actual system and which is subjected to corresponding circuit changes. In this method the various circuit elements are set up to have the corresponding positive-, negative-, and zero-sequence series impedances and shunt admittances. Distributed circuit elements, such as a transmission line, are simulated by equivalent circuits of lumped or semidistributed constants. The characteristic of the arc path can be simulated, or the arc path can be re-established as determined by the assumed dielectric characteristic of the breaker, or, as is more frequently done, the arc can be re-established at the point on the system voltage wave which would result in the highest transient disturbances. Several restrikes are possible under some conditions. However, practically all of the voltage studies to date have assumed that the probable maximum overvoltage would be obtained as a result of two restrikes occurring at such intervals as to produce the greatest possible rate of increase in system overvoltage. In the operation of a circuit breaker many restrikes may occur in the process of opening. In this connection it is to be pointed out that unless the magnitude and time of the dielectric recovery voltage curve have just the right values to cause breakdown by system recovery voltage, the restrikes will not occur at such intervals as to produce maximum cumulative action. Frequently the transient overvoltages, which cannot be avoided on the energizing of a circuit, are comparable to the voltages produced on opening a circuit with many restrikes.

System overvoltages as a result of arcing faults to ground or arcs through switching equipment in the process of opening or closing the circuit are affected by the method of system grounding and the magnitude of the grounding impedance, because these voltages are controlled by the magnitude of the sequence-impedance constants to ground and their relation to the normal circuit impedance.

Miniature-system setups have been used for the study of transients on systems grounded in different ways.[8,16,19] These investigations of the transient overvoltages for single or double line-to-ground faults or switching operations on ground-faulted systems show that the solidly grounded system is the best, and the isolated system is the worst. In addition, the investigations of ground-fault neutralizer systems show that

1. For arcing faults to ground that are cleared without switching, the transient overvoltages are very low.
2. For switching a grounded line, the transient voltages are comparable to those of an isolated-neutral system.*

For ground faults on reactance-grounded systems, the range of transient voltages has two minimum values corresponding to the solidly grounded system and to the ground-fault neutralizer system, and two maximum values, one corresponding to the isolated-neutral system and the other corresponding to a reactance intermediate between zero for the solidly grounded system and the ground-fault neutralizer value. With resistance-grounded systems of low ohmic value, the transient voltages approach those of the solidly grounded system, and with high ohmic values (higher than ground-fault neutralizer values), the voltages approach those corresponding to the isolated-neutral system.

Summary

The foregoing discussion of system overvoltages indicates broadly the scope and complexity of the problem. In this section of the report an attempt will be made to present as concisely as possible the relationship between system overvoltages and the method of neutral grounding.

In the preceding classification of the causes of system overvoltage (see section on causes of system overvoltage) items 1*a*, *b*, and *c* deal with phenomena which are independent of the method of system grounding.

Items 2*a*, *b*, and *c* do not lend themselves to a generalized relationship with neutral-grounding impedances. However, it can be said that the isolated-neutral system is likely to give more trouble from this source than any other type of system.

The overvoltage most frequently encountered in power system operation is that which occurs as a consequence of a fault to ground (items 3*a* and *b*). These overvoltages are definitely related to the amount of grounding impedance as indicated in Figure 1. The curves of this figure are based on the sequence series reactances X_1, X_2, X_0, the zero-sequence resistance R_0, the sequence shunt capacitances C_1, C_2, C_0, and the values of fault resistance R_f, all viewed from the point of fault. The values of negative-sequence reactance and shunt capacitance are taken, in line with conventional practice, as equal to the corresponding positive-sequence quantities, although slightly higher values of voltage would be obtained if X_2 were greater than X_1. The solid curves of Figure 1 show the sustained fundamental-frequency voltages (neglecting decrement in machine fluxes) that appear from line to ground for a single line-to-ground fault. These curves are important mainly, because they are the basis for determining arrester ratings required for a particular system. A double line-to-ground fault need not be considered generally in the practical range of system constants, since voltages to ground are less than for a line-to-ground fault except as indicated in the discussion of the curves of Figure 2. If X_0/X_1 is small and positive, sustained overvoltages resulting from faults are low when R_0/X_1 is also low.

The region in which X_0/X_1 and R_0/X_1 are both small is of considerable importance. This region has been plotted in Figure 2 in a different way[11] for the purpose of showing more clearly the effect of zero-sequence impedance upon the maximum sustained voltages to ground for any type of fault and for any value of fault resistance (in Figure 1 fault resistance was neglected for positive values of X_0/X_1). These curves are for $X_2=X_1$ and $R_1=R_2=0$. They show the maximum zero-sequence impedance ratios R_0/X_1 and X_0/X_1 for which a definite voltage will not be exceeded between any phase and ground.

The curves of Figure 2 are irregular, because they are composed of parts that represent limiting voltages on different phases or for different types of faults. Thus, assuming counterclockwise rotation in *a*, *b*, *c* order, the principal parts of the curves are arcs of circles corresponding to the voltage on phase *c* for a single line-to-ground fault on phase *a* with zero fault resistance. For parts of the 1.25 and 1.40 curves near the R_0/X_1 axis, the effect of introducing fault resistance is to increase the voltage on phase *c*. For the 1.25 and 1.4 curves the portions between the X_0/X_1 axis and the points of abrupt change in the curves are determined by the voltage on phase *b*, for a single line-to-ground fault on phase *a* with the fault resistance such as to give maximum voltage. Part of the 1.25 curve near the X_0/X_1 axis is determined by the voltage on phase *a* for a double line-to-ground fault on phases *b* and *c*.

With reference to Figure 1 again, it is evident that sustained overvoltages can be very high if the ratio X_0/X_1 is in the vicinity of -2. However, this is of little practical significance, since systems would not normally operate with such constants. Most systems normally operated with isolated neutral (X_0/X_1 negative) would have a negative value of X_0/X_1 greater than 40. Consequently the region of highest voltages would not be of importance except possibly during a system disturbance in the course of which a large portion of generating capacity is lost, or the major neutral grounding points are separated from the system as a result of switching.

Isolated neutral systems have negative values of the ratio X_0/X_1 which usually are in the range between -40 and minus infinity. From Figure 1 it will be seen that for a ground fault the fundamental-frequency sustained voltage from line to ground equals 1.73 times line-to-ground voltage for the ratio of X_0/X_1 equal to minus infinity, and, that as the ratio is decreased to -40, the voltage increases to about five per cent above this value. For ground faults on isolated-neutral systems the effect of fault resistance (item 3*b* in the previous classification) causes further increase in the voltages to ground. The maximum sustained voltages to ground for any value of fault resistance are given in Figure 1 by the curve made up of long dashes separated by two short ones. In the application of protective devices, such as lightning arresters, this circumstance theoretically requires the use of devices with ratings higher than the emergency-rated voltage of the system. For some isolated-neutral systems this procedure is necessary. However, there exists considerable experience on many isolated-neutral systems with lightning arresters of ratings equivalent to the emergency-rated circuit voltage. The damage to the arrester resulting from this practice has usually been negligible, and it is probably more economical to take the risk of an occasional lightning-arrester failure from this cause than to apply arresters of higher rating.[10,11]

When a fault suddenly occurs on one phase of a system, a transient voltage occurs on the other phases. The maximum transient voltage depends on the ratio of C_1/C_0, system configuration, the amount of reactance in the circuit (X_0/X_1), the amount of resistance in the circuit (R_0/X_1), and the instant at which the fault occurs relative to the voltage wave, a particular instant being required to give the maximum disturbance. The

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[*] This of course does not apply when the ground-fault neutralizer is short-circuited before switching is done.

effects of these factors are indicated in Figure 1, and from them an estimate can be made of the maximum transient voltage that can be obtained for most system operating conditions. The curves in Figure 1 made up of long dashes separated by one short dash are for $R_0/X_1=0$. In particular, it should be noted that as C_0 increases relative to C_1, the maximum transient voltage increases. In most cases C_1 will be greater than C_0, however.

Zero-sequence resistance (R_0/X_1) is seen to be helpful in reducing these transient oscillations. Curves for $C_1=C_0$ and for various ratios of R_0/X_1 are shown as a series of short dashes in Figure 1. While no curves are shown for values of R_0/X_1 other than zero for positive values of X_0/X_1, any resistance will tend to reduce the overvoltage from the maximum values shown for $R_0/X_1=0$.

Items 3*c*, *d*, and *e* do not lend themselves to generalized summarizing, such as shown by the curves for items 3*a* and *b*. Any of these items may be present to a certain degree and may in some cases add considerably to the amount of overvoltage shown by the curves of Figures 1 and 2.

Switching operations collectively (item 4) cannot be related definitely to system-grounding impedances. The isolated-neutral system on the one extreme is subject to the highest switching surges, while the solidly grounded system on the other extreme is subject to the lowest switching surges, generally speaking. Studies of particular systems and particular switching operations have been made, and these are helpful in pointing to circuit conditions and methods of grounding to be avoided. Certain values of neutral grounding reactance have been shown to give rise to high transient voltages during switching.[16–19] Relatively high values of neutral grounding resistance are permissible without encountering dangerous overvoltages.[17,19] In cases for which the published results are not sufficiently indicative, it is recommended that further detailed studies be made. In many cases, the *probability* of occurrence may be small, although the *possibility* of getting destructive voltages of the types classified earlier in this report may actually exist.

Of the influences listed under item 5, only "(*c*). Electrostatic charging of ungrounded systems by wind-borne particles, . . ." is recognized generally as a function of grounding impedance. Cases in which conduction or induction is a recognized cause should be studied individually.

It should be evident that it is not possible to specify any specific number of ohms of resistance or reactance as being adequate for grounding and for eliminating the possibility of all overvoltages, since the magnitude and arrangement of other system constants is directly involved. However, it is possible to limit the overvoltages and thus protect apparatus by means of lightning arresters. Arresters respond to voltage and make no distinction as to whether the cause is lightning or a switching operation. In any case, a voltage protective level is held. In the case of a sufficiently low grounding impedance, permitting the use of grounded neutral rating arresters, this protective level is substantially lower than for higher grounding impedance requiring the ungrounded rating arrester. Consequently there is a gain, from the standpoint of lowering the protective level, in selecting grounding impedances of low ohmic value. However other considerations, such as limitation of short-circuit currents for power-system operating reasons or for inductive coordination, may prevent taking full advantage of this possibility.

It should be emphasized that the number of grounding points is of importance also, since during a system disturbance it is undesirable to have any part of the system with connected generation to be left without a neutral ground. While in the limit, this would mean that every source of short-circuit current should have a neutral ground, practical and economic considerations may dictate a somewhat smaller number of grounding points.

Engineering opinions, as to the choice of resistance or reactance grounding and the proper ohmic value, have not crystallized. This situation results from the complexity of the problem, the intangibility of many factors involved, the range of conditions encountered, and the results to be achieved. However, it is pertinent in connection with this discussion to consider the effect of such factors in the grounding problem as relative dielectric strength of apparatus, current limitation requirements, relaying requirements, and lightning-arrester settings, since they affect the kind and magnitude of grounding impedances.

Many engineers prefer to ground directly all available neutral points, introducing neutral reactance where necessary to avoid increasing machine stresses and breaker ratings on ground faults beyond the values corresponding to three-phase short-circuit currents. This procedure comes within the classification of solidly grounded systems with $X_0/X_1<3$. Other engineers prefer to introduce greater limitation of fault current, which is generally accomplished by neutral resistors. The problems are different for grounding transmission systems through transformers than for grounding generator busses because of the relatively lower dielectric strength of rotating machines. In general, the transient overvoltages with resistance grounding are more favorable than with moderate values of reactance grounding, and the maximum value of resistance which may be used is generally considered to be that which corresponds to the tuned reactance value of the ground-fault neutralizer. The maximum permissible value of reactance grounding is considerably more speculative, and discussions have not reached a conclusion. The permissible value of the ratio X_0/X_1 is affected by the extinction voltage, the sequence series reactance and sequence shunt capacitance constants of the system, since they affect the conditions for which a high-frequency current zero may be obtained and thus control the conditions for restriking. Engineering opinion on the maximum permissible value of X_0/X_1 ranges from 3 to 10 for generator grounding. For grounding transmission systems, higher ratios of X_0/X_1 have been considered, ranging from 10 to 40, the highest values having been considered for grounding transformers applied to transmission circuits with aerial lines. Applications in this range however may require special study. Sometimes resistors in parallel with neutral reactors have been used, with the reactor to limit ground current and the resistor to limit transient voltages.

Table I lists in a very general way the advantages and disadvantages of various methods of system grounding.

Classification of Power Systems With Respect to Method of Grounding

The preceding discussion has shown that, from the standpoint of overvoltages at least, the various methods of grounding power systems may be classified according to ground connections and sequence impedance constants* as follows:

1. *An isolated-neutral* (*ungrounded*) *system* is a system without an intentional connection to ground. Isolated-neutral systems may be subjected to various overvoltages, depending upon the ratio of X_0/X_1 as viewed from the fault location.**

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* R_0, X_0, and X_1 are, respectively, the zero-sequence resistance, the zero-sequence reactance taken as positive if inductive and negative if capacitive, and positive-sequence subtransient reactance. All these impedances are those of the system as viewed from the fault location.

** A system grounded through potential transformers is not an isolated-neutral but a reactance-grounded system, according to this classification.

2. *A grounded system* is a system in which at least one conductor or point (usually the middle wire or neutral point of transformer or generator windings) is intentionally grounded, either solidly or through impedance. Grounded systems may be subjected to various overvoltages, depending upon the ratios of X_0/X_1 and R_0/X_1 as viewed from the fault location.

3. *A solidly grounded system or portion of a system* is a system or specified portion thereof that has for all fault locations zero- to positive-sequence ratios of $X_0/X_1<3$ and $R_0/X_1<1$ for any condition of operation and amount of connected generator capacity.[8,10,11,20]

4. *A resistance-grounded system* is a system grounded through an impedance, the principal element of which is resistance.

5. *A reactance-grounded system* is a system grounded through an impedance, the principal element of which is reactance.

6. *A ground-fault neutralizer system* is a system grounded by means of a grounding device which provides an inductive reactive component of current in a ground fault that is substantially equal to, and therefore neutralizes, the rated frequency capacitive component of the ground-fault current.

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Equivalent Circuits for Oscillating Systems and the Riemann–Christoffel Curvature Tensor

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Synopsis: Many electromechanical systems that are being studied in connection with the war effort have so many degrees of freedom that it is totally impractical to analyze their performance by direct calculation. It is, therefore, increasingly important to develop equivalent circuits for these multiple oscillating systems, that can be put on the a-c calculating board and so solved by direct reading of instruments.

In this paper it is shown that a necessary (though not sufficient) condition for the existence of a physical model, corresponding to a given set of equations, is that the set should be a tensor equation. This follows from the fact that only quantities that are tensors can be measured by instruments. The conclusion is therefore reached that only equations that are in tensor form can be set up on the a-c calculating board.

The principle is illustrated by setting up equivalent circuits with the aid of tensorial hunting equations for the determination of the steady-state, hunting, and self-excitation performance of two interconnected instrument-Selsyns, of two salient-pole synchronous machines, and of a capacitor-compensated transmission line connected to a salient-pole synchronous machine. A companion paper, "Self-Excited Oscillations of Capacitor-Compensated Long-Distance Transmission Systems"[18] contains the results of an extended study made on the a-c network analyzer with the aid of one of the equivalent circuits developed here.

Difficulties in Establishing Equivalent Circuits for Hunting

In trying to set up equivalent stationary networks for the *hunting* performance calculation of interconnected systems, it was found to be impossible to establish networks that correspond to the equations of the systems, even though the latter were sets of linear algebraic equations of the form $e = Z \cdot i$. All known methods of tranformation of reference frames or of variables led to expressions that could not be measured by instruments.

The apparently insurmountable difficulties were finally overcome by the following reasoning:

The equations of small oscillation under consideration were the standard Lagrangean equations of motion, in which x^{α} was replaced by $x^{\alpha} + \Delta x^{\alpha}$ as is the universal custom. It is however well known that, while the Lagrangean equations are tensor equations, the equations of small oscillations, derived from the former by standard methods, are no longer tensor equations. Since a tensor is a measurable physical entity, it seems plausible that the standard equations of hunting do not show correctly the physics of the problem—even though they do give correct numerical answers—nor do they express the hunting phenomena in terms of measurable quantities. Hence, no physical model corresponding to a set of non-tensor equations may exist.

The tensorial form of the equations of small oscillations of electrical machinery already has been established[8]. These tensor equations differ from the standard equations by having certain terms added to and subtracted from the latter. In other words, if the standard hunting equations are expressed as

$$\boldsymbol{A} + \boldsymbol{B} = 0$$

(where none of the sets of expressions $\boldsymbol{A}$ or $\boldsymbol{B}$ nor the combination $\boldsymbol{A} + \boldsymbol{B}$ are tensors), the tensorial hunting equations may be expressed as

$$\boldsymbol{A} + \boldsymbol{B} + \boldsymbol{C} - \boldsymbol{C} = 0$$

where $\boldsymbol{C}$ represents a set of expressions, that otherwise would cancel.

Now while neither $\boldsymbol{A}$ nor $\boldsymbol{C}$ are tensors, the combination $\boldsymbol{A} + \boldsymbol{C}$ is a tensor, so is the combination $\boldsymbol{B} - \boldsymbol{C}$. Hence each expression in the equation

$$(\boldsymbol{A} + \boldsymbol{C}) + (\boldsymbol{B} - \boldsymbol{C}) = 0$$

is a tensor. Therefore $\boldsymbol{A} + \boldsymbol{C}$ must be a measurable quantity and so must $\boldsymbol{B} - \boldsymbol{C}$.

If the reasoning is valid then this last equation and not the simplified equation must be the proper physical form, that admits an equivalent circuit. If $\boldsymbol{C}$ is cancelled, then the resultant equation still would give correct numerical answers, though it would represent an incomplete physical picture, hence a physically unrealizable equivalent circuit.

This reasoning proved to be correct in every detail. When the additional terms $\boldsymbol{C}$ were calculated, the equivalent circuit of the oscillating system followed immediately and in a perfectly straightforward manner.

In machines for which the standard hunting equations did give an equivalent circuit outright, the terms $\boldsymbol{A}$ and $\boldsymbol{B}$ each were already tensors (measurable quantities) because of the simplicity of the physical system. Hence the hunting equation itself $\boldsymbol{A} + \boldsymbol{B} = 0$ was already a tensor equation.

The First Principle of the General Theory of Relativity

In 1905 Einstein came forward with the special theory of relativity. One of the consequences of this theory is the postulate that the three co-ordinates of space x, y, z, and time t are not two distinct physical entities

$$\begin{array}{r|c|c|c|} \diagdown m & 1 & 2 & 3 \\ \hline x^m = & x & y & z \\ \hline \end{array} \qquad t = \boxed{t}$$

but are components of a single tensor, a single physical entity

$$\begin{array}{r|c|c|c|c|} \diagdown \alpha & 1 & 2 & 3 & 4 \\ \hline x^{\alpha} = & x & y & z & jct \\ \hline \end{array}$$

With this assumption it could be shown, for instance, that the separate electric and magnetic fields were only components of one underlying physical reality, the electromagnetic field. (Namely, $\boldsymbol{B} = B_{\alpha}$ and $\boldsymbol{H} = H^{\alpha}$ are components of one tensor $F_{\alpha}^{\cdot\beta}$.)

In 1916 Einstein announced the general theory of relativity. One of the outgrowths of this more general theory is the postulate that both the gravitational and the electromagnetic fields are components of only one underlying physical reality. The first principle of this theory may be formulated for the present purpose as follows:[1]

"All laws of nature are tensor equations"*

For instance, the equations of Lagrange are tensor equations; so are the field equations of Maxwell in special reference frames. For more general reference frames Maxwell's equations have been brought into tensorial form by Minkowsky.

The question whether the group of transformations under which the laws of

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* "Die allgemeinen Naturgesetze sind durch Gleichungen auszudrucken, die für alle Koordinatensysteme gelten, d.h. die beliebigen Substitutionen gegenüber kovariant (allgemein kovariant) sind."

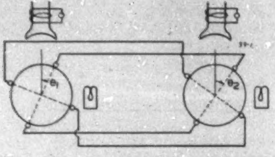


Figure 1. Two single-phase instrument Selsyns

nature are invariant is the Lorentz group (as postulated by Einstein), or the Galilean group, has no bearing upon the argument of this paper.

A Principle to Establish Models for Physical Systems

Of course not only the grandiose differential equations that describe the curvature of light as it passes near the sun are tensor equations, but also the simple linear differential or algebraic equations that represent the performance of such a lowly creation as a stationary network. Hence if a stationary network is given, and its set of linear algebraic equations $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$ is established by Ohm's and Kirchhoff's laws, the set always comes out as a tensor equation.

But let now the problem be reversed. Let a set of linear algebraic equations $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$ be given: Are or are they not the equations of a stationary network? In other words, is it possible to establish a physical model whose performance corresponds term by term to the given set of equations?

Table I. Torque Formulas of Two Instrument Selsyns

Valid If Either Machine Hunts

(a). Steady-State Torques

$$i^f E_{f1}^* = W_1 \qquad i^f E_{f2}^* = W_3 \qquad T_1 = W_1 + W_2$$
$$i^b E_{b1}^* = W_2 \qquad i^b E_{b2}^* = W_4 \qquad T_2 = W_3 + W_4$$

(b). Damping and Synchronizing Torques of the Second Machine (Receiver)

$$i^f \Delta E_{f2}^* = W_5 + jQ_5 \qquad i^b \Delta E_{b2}^* = W_7 + jQ_7 \qquad T_{s2}' = \frac{W_5 + W_6 + W_7 + W_8}{2}$$
$$\Delta i^f E_{f2}^* = W_6 + jQ_6 \qquad \Delta i^b E_{b2}^* = W_8 + jQ_8 \qquad T_{d2}' = \frac{(Q_6 + Q_8) - (Q_5 + Q_7)}{2h}$$

Repeat the same measurements with $-h$ hunting network.

$$T_{s2} = T_{s2}' + T_{s2}'' \qquad T_{d2} = T_{d2}' - T_{d2}''$$

(c). Damping and Synchronizing Torques of the First Machine (Transmitter)

$$i^f \Delta E_{f1}^* = W_9 + jQ_9 \qquad i^b \Delta E_{b1}^* = W_{11} + jQ_{11} \qquad T_{s1}' = \frac{W_9 + W_{10} + W_{11} + W_{12}}{2}$$
$$\Delta i^f E_{f1}^* = W_{10} + jQ_{10} \qquad \Delta i^b E_{b1}^* = W_{12} + jQ_{12} \qquad T_{d1}' = \frac{(Q_{10} + Q_{12}) - (Q_9 + Q_{11})}{2h}$$

Repeat the same measurements with $-h$ hunting network.

$$T_{s1} = T_{s1}' + T_{s1}'' \qquad T_{d1} = T_{d1}' - T_{d1}''$$

In consequence of the first principle of general relativity, it is safe to conclude that a primary and necessary condition for the establishment of a model is that the set of equations should be a tensor equation. If it is not a tensor equation, then the set definitely cannot represent any physically existing structure, such as a stationary network.

This deduction was rather emphatically proved in attempting to establish equivalent circuits by use of the nontensor hunting equations $\boldsymbol{e}=\boldsymbol{Z}\cdot\boldsymbol{i}$.

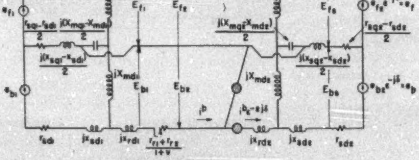
This simple fact may be generalized to include all physical phenomena by the following principle:

> "A set of equations, representing the performance of any physical system (be it mechanical, electrical, thermal, or any other system) may be represented by a physical model (equivalent circuit) only if the set of equations is a tensor equation."

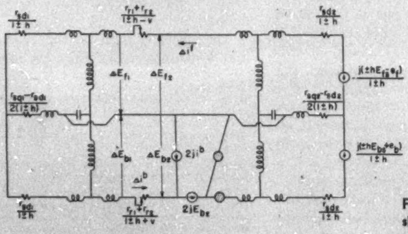
Even if no model of a system is built, the engineer is interested in the objects that he can measure. Or if he does not care to measure certain quantities, he may still be interested in a mental "physical picture" of the phenomena taking place in the various parts of the system. As a corollary of the above principle it may be stated that:

> "In order that a set of equations should express the performance of a physical system in terms of measurable and visualizable quantities, the set of equations must be a tensor equation."

Examples of sets of nontensor equations are those that represent small oscillations of dynamical systems, if these equations

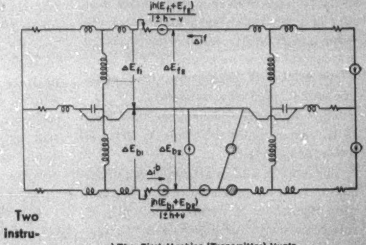

r_{sd1}
jx_{sd1}
jx_{rd1}
$\frac{r_{r1}+r_{r2}}{1-v}$
jx_{rd2}
jx_{sd2}
r_{sd2}
i^f
e_{f1}
jX_{md1}
E_{f1}
E_{f2}
jX_{md2}
E_{f2}
$e_{f2}\epsilon^{-j\delta}=e_f$
$\frac{r_{sq1}-r_{sd1}}{2}$
$\frac{j(X_{mq1}-X_{md1})}{2}$
$\frac{j(X_{mq2}-X_{md2})}{2}$
$\frac{r_{sq2}-r_{sd2}}{2}$
$\frac{j(x_{sq1}-x_{sd1})}{2}$
$\frac{j(x_{sq2}-x_{sd2})}{2}$
e_{b1}
jX_{md1}
E_{b1}
E_{b2}
jX_{md2}
E_{b2}
$e_{b2}\epsilon^{-j\delta}=e_b$
i^b
$i^b\epsilon^{-j\delta}$
r_{sd1}
jx_{sd1}
jx_{rd1}
$\frac{r_{r1}+r_{r2}}{1+v}$
jx_{rd2}
jx_{sd2}
r_{sd2}

a) Steady-State Network


$\frac{r_{sd1}}{1\pm h}$
$\frac{r_{r1}+r_{r2}}{1\pm h-v}$
$\frac{r_{sd2}}{1\pm h}$
Δi^f
ΔE_{f1}
ΔE_{f2}
$\frac{j(\pm hE_{f2}+e_f)}{1\pm h}$
$\frac{r_{sq1}-r_{sd1}}{2(1\pm h)}$
$\frac{r_{sq2}-r_{sd2}}{2(1\pm h)}$
ΔE_{b1}
ΔE_{b2}
$2ji^b$
$\frac{j(\pm hE_{b2}+e_b)}{1\pm h}$
Δi^b
$\frac{r_{sd1}}{1\pm h}$
$\frac{r_{r1}+r_{r2}}{1\pm h+v}$
$2jE_{b2}$
$\frac{r_{sd2}}{1\pm h}$

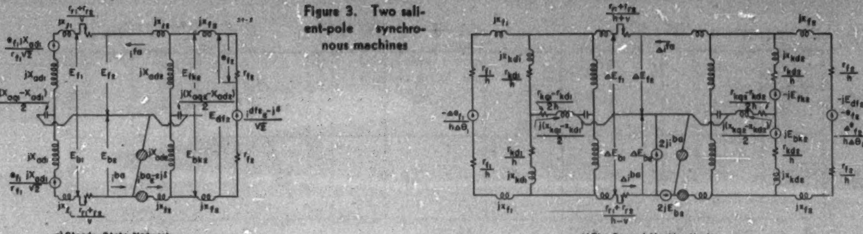
b) The Second Machine (Receiver) Hunts

Figure 2. Two single-phase instrument Selsyns


$\frac{jh(E_{f1}+E_{f2})}{1\pm h-v}$
Δi^f
ΔE_{f1}
ΔE_{f2}
ΔE_{b1}
ΔE_{b2}
Δi^b
$\frac{jh(E_{b1}+E_{b2})}{1\pm h+v}$

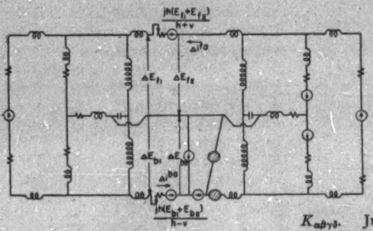
c) The First Machine (Transmitter) Hunts

Figure 3. Two salient-pole synchronous machines



a) Steady-State Network

b) The Second Machine Hunts



c) The First Machine Hunts

are derived from the equation of Lagrange by standard methods. Other nontensor equations are those of magnetrons, ultra-high-frequency tubes, and so forth, in the analysis of which small variations are taken in Maxwell's equations.

Many of the theories used in electrical engineering for the study of rotating machines and stationary networks are also nontensor equations. Such theories are usually valid for one particular machine and even then only for one particular reference frame. In contrast with these in Park's analysis of the synchronous machine, all basic equations (except those of hunting) may be shown to be tensor equations, and to this very fact their success must be attributed. Fortescue's method of symmetrical components is a tensorial transformation; hence its value and permanence.

The conclusion of the reasoning is that even though a set of linear algebraic equations $\mathbf{e}=\mathbf{Z}\cdot\mathbf{i}$ is available, it does not follow that an equivalent stationary network can be established for it. A necessary (though not sufficient) condition is that the equation should be a tensor equation. If it is not, then it cannot correspond term by term to any physically realizable network.

The change of a set of linear algebraic equations $\mathbf{e}=\mathbf{Z}\cdot\mathbf{i}$ into a tensor equation consists—in the present case when it represents small oscillations—of the introduction of such tensorial concepts as the Riemann-Christoffel curvature tensor $K_{\alpha\beta\gamma\delta}$. Just how to change a set of equations into a tensor equation always depends on what the equations represent physically. The equations of each type of physical phenomenon require separate treatment.

The Affine Connection

The most general form of connection used in affine differential geometry and also in the study of electrical machinery is

$$\Gamma_{\alpha\beta,\gamma}=\frac{1}{2}\left(\frac{\partial a_{\beta\gamma}}{\partial x^{\alpha}}+\frac{\partial a_{\gamma\alpha}}{\partial x^{\beta}}-\frac{\partial a_{\alpha\beta}}{\partial x^{\gamma}}\right)-\Omega_{\alpha\beta,\gamma}+\Omega_{\gamma\beta,\alpha}+\Omega_{\gamma\alpha,\beta}+S_{\alpha\beta\gamma}-S_{\gamma\beta\alpha}-S_{\gamma\alpha\beta}+\frac{1}{2}(Q_{\alpha\beta\gamma}+Q_{\beta\gamma\alpha}-Q_{\gamma\alpha\beta}) \quad (1)$$

where

$S_{\alpha\beta\gamma}$ = torsion tensor, skew-symmetric in α and β

$Q_{\alpha\beta\gamma}$ = "Weyl tensor," symmetric in α and β

$\Omega_{\alpha\beta,\gamma}$ = nonholonomic object, skew-symmetric in α and β

In the special types of electrical machines considered in this paper the Weyl tensor $Q_{\alpha\beta\gamma}$ is zero.

In the dynamical equations of Lagrange $\Gamma_{\alpha\beta,\gamma}$ is always multiplied by $\dot{x}^{\alpha}=i^{\alpha}$ twice, as $\Gamma_{\alpha\beta,\gamma}i^{\alpha}i^{\beta}$. As a consequence some of the above terms cancel or become equal, so that the affine connection simplifies to

$$\Gamma_{\alpha\beta,\gamma}=\left(\frac{\partial a_{\beta\gamma}}{\partial x^{\alpha}}-\frac{1}{2}\frac{\partial a_{\alpha\beta}}{\partial x^{\gamma}}\right)+2\Omega_{\gamma\alpha\beta}-2S_{\gamma\alpha\beta}$$
$$=[\alpha\beta,\gamma]-2S_{\gamma\alpha\beta} \quad (2)$$

where $[\alpha\beta,\gamma]$ is the *nonholonomic* Christoffel symbol.

It is emphasized that the above simplified form of $\Gamma_{\alpha\beta,\gamma}$ can be used only in the equation of motion and in its variations, such as the equations of small oscillations. In any other equation, such as the field equations of Maxwell, the complete form of $\Gamma_{\alpha\beta,\gamma}$, as it appears in equation 1 must be used.[6]

The Three Types of Transformations

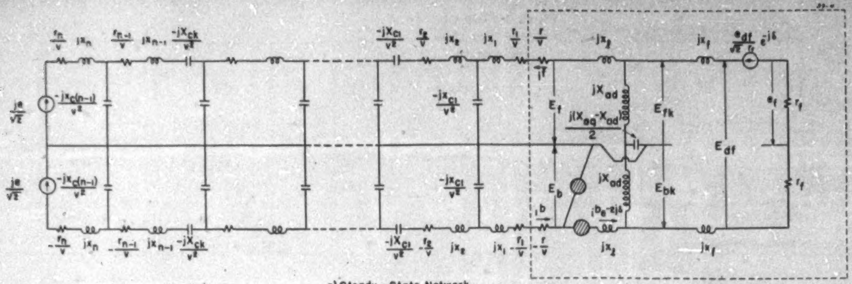
In establishing equivalent networks for electrical machines there are at least three types of transformations used:[3]

1. Co-ordinate transformations.

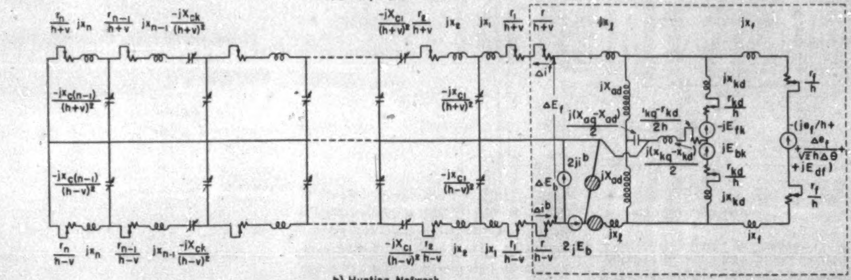
Table II. Torque Formulas of Two Synchronous Machines

Valid If Either Machine Hunts

Steady-State Torques	Damping and Synchronizing Torques	
First Machine	**First Machine**	**Second Machine**
$i^{fa}E_{f1}^{*}=W_1$	$i^{fa}\Delta E_{f1}^{*}=W_5+jQ_5$	$i^{fa}\Delta E_{f2}^{*}=W_9+jQ_9$
$i^{ba}E_{b1}^{*}=W_2$	$\Delta i^{fa}E_{f1}^{*}=W_6+jQ_6$	$\Delta i^{fa}E_{f2}^{*}=W_{10}+jQ_{10}$
$T_1=W_1+W_2$	$i^{ba}\Delta E_{b1}^{*}=W_7+jQ_7$	$i^{ba}\Delta E_{b2}^{*}=W_{11}+jQ_{11}$
	$\Delta i^{ba}E_{b1}^{*}=W_8+jQ_8$	$\Delta i^{ba}E_{b2}^{*}=W_{12}+jQ_{12}$
Second Machine		
$i^{fa}E_{f2}^{*}=W_3$		
$i^{ba}E_{b2}^{*}=W_4$	$T_{s1}=-(W_5+W_6+W_7+W_8)$	$T_{s2}=-(W_9+W_{10}+W_{11}+W_{12})$
$T_2=W_3+W_4$	$T_{d1}=\frac{(Q_5+Q_7)-(Q_6+Q_8)}{h}$	$T_{d2}=\frac{(Q_9+Q_{11})-(Q_{10}+Q_{12})}{h}$


$\frac{r_n}{v}$
jx_n
$\frac{r_{n-1}}{v}$
jx_{n-1}
$\frac{-jX_{ck}}{v^2}$
$\frac{-jX_{c1}}{v^2}$
$\frac{r_2}{v}$
jx_2
jx_1
$\frac{r_1}{v}$
$\frac{r}{v}$
jx_l
jx_f
$\frac{e_{df}}{\sqrt{2}\,r_f}\varepsilon^{-j\delta}$
$\frac{je}{\sqrt{2}}$
$\frac{-jx_{c(n-1)}}{v^2}$
i^f
E_f
jX_{ad}
$\frac{j(X_{aq}-X_{ad})}{2}$
E_{fk}
E_{df}
e_f
r_f
E_b
E_{bk}
i^b
$i^b\varepsilon^{-2j\delta}$

a) Steady-State Network


$\frac{r_n}{h+v}$
jx_n
$\frac{r_{n-1}}{h+v}$
jx_{n-1}
$\frac{-jX_{ck}}{(h+v)^2}$
$\frac{-jX_{c1}}{(h+v)^2}$
$\frac{r_2}{h+v}$
jx_2
jx_1
$\frac{r_1}{h+v}$
$\frac{r}{h+v}$
jx_l
jx_f
$\frac{-jx_{c(n-1)}}{(h+v)^2}$
$\frac{-jx_{c(n-1)}}{(h-v)^2}$
Δi^f
ΔE_f
jX_{ad}
$\frac{j(X_{aq}-X_{ad})}{2}$
$\frac{r_{kq}-r_{kd}}{2h}$
$\frac{j(x_{kq}-x_{kd})}{2}$
jx_{kd}
$\frac{r_{kd}}{h}$
$-jE_{fk}$
jE_{bk}
$\frac{r_f}{h}$
$-(je_f/h+\frac{\Delta e_f}{\sqrt{2}h\Delta\theta}+jE_{df})$
$2ji^b$
ΔE_b
Δi^b
$2jE_b$
$\frac{r_n}{h-v}$
$\frac{r_{n-1}}{h-v}$
$\frac{-jX_{ck}}{(h-v)^2}$
$\frac{-jX_{c1}}{(h-v)^2}$
$\frac{r_2}{h-v}$
$\frac{r_1}{h-v}$
$\frac{r}{h-v}$

b) Hunting Network

Figure 4. Transmission line with series capacitors connected to salient-pole synchronous machine

All reference axes rotate uniformly with the infinite bus

2. Spin transformations.
3. Gauge transformations.

Co-ordinate transformations are used in passing from the stationary *a*, *b*, *c* axes to the rotating *d*, *q*, *o* axes or in interconnecting the reference axes. Spin transformations are employed in introducing the hypothetical symmetrical component frame. Gauge transformations enter in eliminating some of the phase shifters that would otherwise appear in the equivalent circuits.

In consequence of all these necessary transformations, the theories of spinor analysis must be resorted to, in addition to those of tensor analysis.

Table III. Torque Formulas of Synchronous Machine Connected to Transmission Line

Steady-State Torques	Damping and Synchronizing Torques		
$i^f E_f^* = W'$	$i^f \Delta E_f^* = W_1 + jQ_1$	$\Delta i^f E_f^* = W_3 + jQ_3$	$T_s = W_1 + W_2 + W_3 + W_4$
$i^b E_b^* = W''$	$i^b \Delta E_b^* = W_2 + jQ_2$	$\Delta i^b E_b^* = W_4 + jQ_4$	$T_d = \dfrac{(Q_3+Q_4)-(Q_1+Q_2)}{h}$
$T = W' + W''$			

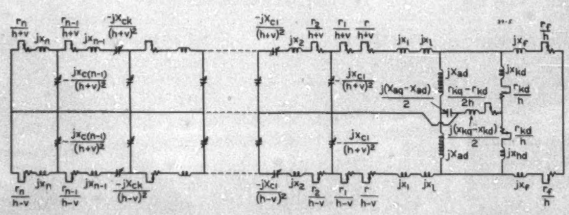

$\frac{r_n}{h+v}$
jx_n
$\frac{r_{n-1}}{h+v}$
jx_{n-1}
$\frac{-jX_{ck}}{(h+v)^2}$
$\frac{-jX_{c1}}{(h+v)^2}$
jx_2
$\frac{r_2}{h+v}$
$\frac{r_1}{h+v}$
$\frac{r}{h+v}$
jx_1
jx_l
jx_f
$\frac{r_f}{h}$
$-\frac{jx_{c(n-1)}}{(h+v)^2}$
$-\frac{jx_{c1}}{(h+v)^2}$
$\frac{j(X_{aq}-X_{ad})}{2}$
jX_{ad}
jx_{kd}
$\frac{r_{kq}-r_{kd}}{2h}$
$\frac{r_{kd}}{h}$
$\frac{j(x_{kq}-x_{kd})}{2}$
$\frac{r_n}{h-v}$
$\frac{r_{n-1}}{h-v}$
$\frac{-jX_{ck}}{(h-v)^2}$
$\frac{-jX_{c1}}{(h-v)^2}$
$\frac{r_2}{h-v}$
$\frac{r_1}{h-v}$
$\frac{r}{h-v}$

Figure 5. Self-excitation network of the transmission system (mass = infinite)

Spinor Form of the Equation of Motion

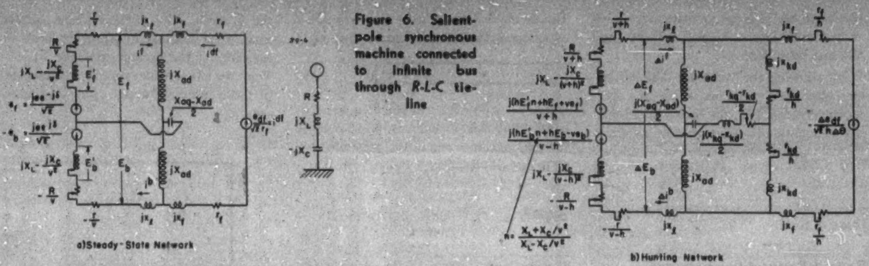
When the components of $\Gamma_{\alpha\beta,\gamma}$ contain complex numbers or complex functions, it has been shown[3] that eight different $\Gamma_{\alpha\beta,\gamma}$ may occur in the most general case, namely $\Gamma_{\alpha\beta,\bar\gamma}$, $\Gamma_{\alpha\bar\beta,\gamma}$, $\Gamma_{\bar\alpha\beta,\gamma}$ $\Gamma_{\alpha\beta,\gamma}$ and their conjugates. It has been found by the writer that in the Lagrangean equation, or at least in electrical machinery, six of the above eight expressions are zero, and only the following two have nonzero components, $\Gamma_{\alpha\beta,\bar\gamma}$ and $\Gamma_{\bar\alpha\beta,\bar\gamma}$. In particular it was found, that of the two components of $\Gamma_{\alpha\beta,\gamma}$ namely

$$\Gamma_{\alpha\beta,\gamma} \pm [\alpha\beta,\bar\gamma] - 2S_{\bar\gamma\bar\alpha\beta} \tag{3}$$

the nonholonomic Christoffel symbol $[\alpha\beta,\bar\gamma]$ has only its free index barred, while the torsion tensor $S_{\bar\alpha\bar\beta\bar\gamma}$ has its skew-symmetric indexes both barred. Hence in interchanging α and β in the latter, the components change signs only, but do not assume conjugate values.

From this property the following conclusions may be drawn, to be used as a laborsaving device:

In analyzing electrical machinery in complex variables (for instance when sym-



a) Steady-State Network

Figure 6. Salient-pole synchronous machine connected to infinite bus through *R-L-C* tie-line

b) Hunting Network

metrical components are introduced or when polyphase machines are studied) the usual tensor equations containing the unbarred $\Gamma_{\alpha\beta,\gamma}$ may be used in all manipulations, even though the variables are complex. However when $\Gamma_{\alpha\beta,\gamma}$ is replaced by its component objects, then $[\alpha\beta,\bar\gamma]$ should have its free index barred and $S_{\bar\alpha\bar\beta\gamma}$ its two skew-symmetric indexes.

Accordingly the equation of motion of Lagrange

$$e_\alpha=R_{\alpha\beta}i^\beta+a_{\alpha\beta}\frac{di^\beta}{dt}+\Gamma_{\beta\gamma,\alpha}i^\beta i^\gamma \qquad (4)$$

may be written for complex variables as

$$\boxed{e_{\bar\alpha}=R_{\bar\alpha\beta}i^\beta+a_{\bar\alpha\beta}\frac{di^\beta}{dt}+[\beta\gamma,\bar\alpha]i^\beta i^\gamma-2S_{\bar\alpha\bar\beta\gamma}i^{\bar\beta}i^\gamma} \qquad (5)$$

where $[\beta\gamma,\bar\alpha]$ is the nonholonomic Christoffel symbol.

From this notation automatically follows for instance that in calculating the torque one of the currents must be changed to its conjugate. It should be remembered that:

1. $e_{\bar\alpha}$ is normally barred.
2. i^α is normally unbarred.
3. The metric tensor $a_{\bar\alpha\beta}$ is no longer symmetrical, but hermitian

$$a=a_t^* \text{ or } a_{\bar\alpha\beta}=a_{\beta\alpha} \qquad (6)$$

4. When in the transient equations the $p=d/dt$ are replaced by $j\omega$, the spin indices do not refer to these new complex numbers. For that reason p should be replaced by $k\omega$ where k plays a similar role to j, still it remains distinct from it.
5. When the free index is a geometrical index s the equation represents torques. Hence only the real part of the torque equations is calculated.

The Equations of Hunting

Making small changes in the equation of motion of Lagrange (equation 4) the conventional nontensorial equation of hunting is

$$E_\alpha+\frac{\partial e_\alpha}{\partial x^\beta}\Delta x^\beta=\frac{\partial R_{\alpha\beta}}{\partial x^\gamma}i^\beta\Delta x^\gamma+R_{\alpha\beta}\Delta i^\beta+ a_{\alpha\beta}\frac{d(\Delta i^\beta)}{dt}+\Gamma_{\beta\gamma,\alpha}\Delta i^\beta i^\gamma+\Gamma_{\beta\gamma,\alpha}i^\beta\Delta i^\gamma+ \frac{\partial a_{\alpha\beta}}{\partial x^\gamma}\frac{di^\beta}{dt}\Delta x^\gamma+\frac{\partial\Gamma_{\beta\gamma,\alpha}}{\partial x^\delta}i^\beta i^\gamma\Delta x^\delta \qquad (7)$$

where E_α is any small impressed force. In the special case when $a_{\alpha\beta}$ and $\Gamma_{\alpha\beta,\gamma}$ are constant, the last two terms disappear, and the equation becomes a tensor equation.

The tensorial equation of small oscillations and of their geometrical analogue, the disturbed geodesic is well known.[5-7] Its derivation, when applied to electrical machinery, has been given in reference 8, equation 161 as

$$\boxed{E_\alpha+\delta e_\alpha=R_{\alpha\beta}\delta i^\beta+a_{\alpha\beta}\frac{\delta(\delta i^\beta)}{dt}+K_{\delta\gamma\beta\alpha}i^\beta i^\delta dx^\gamma+R_{\alpha\beta\gamma}i^\beta dx^\gamma} \qquad (8)$$

Each symbol in the equation is a tensor.

Replacing each tensor by its component geometric objects, the above equation may be written as (reference 8, equation 162):

$$\begin{array}{l|l}
E_\alpha+\delta e_\alpha \quad \Big\{ & E_\alpha+\frac{\partial e_\alpha}{\partial x^\beta}\Delta x^\beta+\Gamma_{\gamma\delta,\alpha}e^\gamma\Delta x^\delta \\
=\delta(R_{\alpha\beta}i^\beta) \quad \Big\{ & =R_{\alpha\beta}\Delta i^\beta+\frac{\partial R_{\alpha\beta}}{\partial x^\gamma}i^\beta\Delta x^\gamma+\Gamma_{\gamma\delta,\alpha}R^\gamma_{\cdot\beta}i^\beta\Delta x^\delta \\
 & +\Gamma_{\gamma\delta,\alpha}e^\gamma\Delta x^\delta-\Gamma_{\gamma\delta,\alpha}R^\gamma_{\cdot\beta}i^\beta\Delta x^\delta+a_{\alpha\beta}\frac{d}{dt}\Delta i^\beta+\frac{\partial a_{\alpha\beta}}{\partial x^\gamma}\frac{di^\beta}{dt}\Delta x^\gamma+\Gamma_{\beta\gamma,\alpha}\Delta i^\beta i^\gamma \\
+a_{\alpha\beta}\frac{\delta(\delta i^\beta)}{dt} \quad \Bigg\{ & +\Gamma_{\beta\gamma,\alpha}i^\beta\Delta i^\gamma+\left(\frac{\partial\Gamma_{\beta\gamma,\alpha}}{\partial x^\delta}+\Gamma_{\lambda\delta,\alpha}\Gamma_{\beta\gamma}{}^\lambda-\Gamma_{\lambda\gamma,\alpha}\Gamma_{\beta\delta}{}^\lambda-2\Gamma_{\beta\lambda,\alpha}\Omega_{\delta\gamma}{}^\lambda\right)i^\beta i^\delta\Delta x^\gamma \\
+K_{\delta\gamma\beta\alpha}i^\beta i^\delta dx^\gamma \Big\{ & +\left(\frac{\partial\Gamma_{\beta\delta,\alpha}}{\partial x^\gamma}-\frac{\partial\Gamma_{\beta\gamma,\alpha}}{\partial x^\delta}+\Gamma_{\lambda\gamma,\alpha}\Gamma_{\beta\delta}{}^\lambda-\Gamma_{\lambda\delta,\alpha}\Gamma_{\beta\gamma}{}^\lambda+2\Gamma_{\beta\lambda,\alpha}\Omega_{\delta\gamma}{}^\lambda\right)i^\beta i^\delta\Delta x^\gamma
\end{array} \qquad (9)$$

The equation given in the reference is generalized here to include nonholonomic reference frames, as all examples considered below involve nonholonomic systems.

The Set to Be Added and Subtracted

The tensorial equation of hunting (9) consists of the standard equation of hunting (7) with a set of terms ΔE_α *added* and *subtracted*. The set is

$$\Delta E_\alpha=-\Gamma_{\beta\gamma,\alpha}(e^\beta-R^\beta_{\cdot\delta}i^\delta-\Gamma_{\epsilon\delta}{}^\beta i^\epsilon i^\delta)\Delta x^\gamma+ \left(\Gamma_{\lambda\delta,\alpha}\Gamma_{\beta\gamma}{}^\lambda+\frac{\partial\Gamma_{\beta\gamma,\alpha}}{\partial x^\delta}+2\Gamma_{\beta\lambda,\alpha}\Omega_{\delta\gamma}{}^\lambda\right)i^\beta i^\delta\Delta x^\gamma \qquad (10)$$

This set may be simplified by noting that by the equation of motion (4)

$$e^\beta-R^\beta_{\cdot\delta}i^\delta-\Gamma_{\epsilon\delta}{}^\beta i^\epsilon i^\delta=\frac{di^\beta}{dt}=pi^\beta \qquad (11)$$

Hence the set to be added and subtracted from the standard equations of hunting is

$$\boxed{\Delta E_\alpha=-\Gamma_{\beta\gamma,\alpha}\frac{di^\beta}{dt}\Delta x^\gamma+\left(\Gamma_{\lambda\delta,\alpha}\Gamma_{\beta\gamma}{}^\lambda+2\Gamma_{\beta\lambda,\alpha}\Omega_{\delta\gamma}{}^\lambda+\frac{\partial\Gamma_{\beta\gamma,\alpha}}{\partial x^\delta}\right)i^\beta i^\delta\Delta x^\gamma} \qquad (12)$$

It should be noted that in electrical machinery Δx^γ must always have a geometrical index s as Δx^s, since all objects are functions only of x^s. Also in all the examples considered here, the two machines *run at the same speed*, $x^s=\delta_2-\delta_1=\text{constant}$, hence $i^s=p(\theta_2-\theta_1)=0$, and the last term is zero.

By allowing the *free* index α to assume the electrical variables m or the geometrical variables s, the equation of hunting automatically separates into an equation of voltage and an equation of torque. The latter may be expressed in terms of geometric objects of valence two $\boldsymbol{G}$, $\boldsymbol{V}$, and so forth, instead of objects of valence three or four.

Tensor Form of the Voltages and Torques

1. If $\alpha=\bar{m}$, the remaining terms may be expressed as

$$\Delta E_{\bar{m}}=-\Gamma_{\beta s\bar{m}}\frac{di^{\beta}}{dt}\Delta x^s+(\Gamma_{\lambda\delta,\,\bar{m}}\Gamma_{\beta s}{}^{\lambda}+2\Gamma_{\beta\lambda,\,\bar{m}}\Omega_{\delta s}{}^{\lambda})i^{\beta}i^{\delta}\Delta x^s$$

$$=-\Gamma_{ns,\,\bar{m}}pi^n\Delta x^s+2\Gamma_{\bar{k}n,\bar{m}}\Omega_{\bar{k}s}{}^{n}i^{\bar{s}}i^{k}\Delta x^s+\Gamma_{ns,\,\bar{m}}\Gamma_{\bar{k}s}{}^{n}i^{\bar{k}}i^{\bar{s}}\Delta x^s$$

$$=-V_{\bar{m}n}pi^n\Delta\delta-G_{\bar{m}n}L^{n\bar{r}}V_{\bar{r}k}i^kp\theta\Delta\delta+V_{\bar{m}n}L^{n\bar{r}}V_{\bar{r}k}i^kp\delta$$

$$\boldsymbol{\Delta E}=-\boldsymbol{V}\cdot p\boldsymbol{i}\Delta\delta-\boldsymbol{G}\cdot\boldsymbol{L}^{-1}\cdot\boldsymbol{V}\cdot\boldsymbol{i}p\theta\Delta\delta+\boldsymbol{V}\cdot\boldsymbol{L}^{-1}\cdot\boldsymbol{V}\cdot\boldsymbol{i}p\delta \qquad (13)$$

If the two machines run at the same speed $\boldsymbol{V}p\delta=0$, and the last term is zero.

Hence *the additional term to the equation of voltage is* (to be added and subtracted)

$$\boxed{\boldsymbol{\Delta E}=(-\boldsymbol{V}\cdot p\boldsymbol{i}-\boldsymbol{\eta}_t{}^*\cdot\boldsymbol{V}p\theta\cdot\boldsymbol{i})\Delta\delta} \qquad (14)$$

where $\delta=\pm(\theta_2-\theta_1)$. It must be remembered that this formula is valid only if the two machines run at the same steady-state speed, $p\theta$.

2. If $\bar{\alpha}=\bar{s}(\bar{\delta}=n$ and $\beta=k)$

$$\Delta E_{\bar{s}}=(\Gamma_{mn,\,\bar{s}}\Gamma_{\bar{k}s}{}^{m}+2\Gamma_{\bar{k}m,\,\bar{s}}\Omega_{ns}{}^{m})i^{\bar{k}}i^{n}\Delta x^s \qquad (15)$$

The first term is zero and the second term is

$$2\Gamma_{\bar{k}m,\,\bar{s}}\Omega_{ns}{}^{m}i^{\bar{k}}i^{n}\Delta x^s=-i^{\bar{k}}G_{\bar{k}n}L^{mr}V_{rn}i^n\Delta\delta=-\boldsymbol{i}\cdot\boldsymbol{G}\cdot\boldsymbol{L}^{-1}\cdot\boldsymbol{V}\cdot\boldsymbol{i}\Delta\delta \qquad (16)$$

Hence *the additional term of the equation of torque is* (to be added and subtracted)

$$\boxed{\Delta T=-\boldsymbol{i}\cdot\boldsymbol{\eta}_t{}^*\cdot\boldsymbol{V}\cdot\boldsymbol{i}\Delta\delta} \qquad (17)$$

Example of Two Instrument Selsyns

In interconnecting two slip-ring induction motors, Figure 1 (or synchronous machines) running at an angle $\delta=\theta_2-\theta_1$ from each other, their equivalent circuit requires two phase shifters. One with $\epsilon^{j\delta}$ in the f_r mesh, another with $\epsilon^{-j\delta}$ in the b_r mesh. One of these phase shifters may be eliminated by rotating first all the axes of the second machine by $\epsilon^{j\delta}$. Then the two machines are interconnected (if both are expressed along sequence axes) by

$$\boldsymbol{C}=\begin{array}{c|c|c|c|c|c|c|}
 & f_1 & b_1 & f_r & b_r & f_2 & b_2 \\
\hline
f_{s1} & 1 & & & & & \\
f_{r1} & & & 1 & & & \\
b_{s1} & & 1 & & & & \\
b_{r1} & & & & 1 & & \\
f_{s2} & & & & & 1 & \\
f_{r2} & & & 1 & & & \\
b_{s2} & & & & & & 1 \\
b_{r2} & & & & -\epsilon^{-2j(\theta_2-\theta_1)} & & \\
\hline
\end{array} \qquad (18)$$

The various tensors of the individual selsyns are given in reference 10. The resultant transient $\boldsymbol{Z}$ tensor of the interconnected system is found by

$$\boldsymbol{Z}'=\boldsymbol{C}_t{}^*\cdot\boldsymbol{Z}\cdot\boldsymbol{C}+\boldsymbol{C}_t{}^*p\boldsymbol{L}\cdot\boldsymbol{C} \qquad (19)$$

given in equation 21. The corresponding equivalent circuits are shown in Figure 2. The voltages $\boldsymbol{\Delta E}$ impressed on the hunting networks are given in equation 22. The torques are found by

$$\Delta T=\boldsymbol{i}\cdot\boldsymbol{\Delta E}^*+\Delta\boldsymbol{i}\cdot\boldsymbol{E}^*+\boldsymbol{i}^*\cdot\frac{\partial\boldsymbol{G}}{\partial\delta}\cdot\boldsymbol{i}\Delta\delta \qquad (20)$$

$$\boldsymbol{Z}=\frac{1}{4}\times\begin{array}{c|c|c|c|c|c|c|}
 & f_1 & b_1 & f_r & b_r & f_2 & b_2 \\
\hline
f_1 & r_{sd1}+r_{sq1}+(L_{sd1}+L_{sq1})p & r_{sd1}-r_{sq1}+(L_{sd1}-L_{rq1})p & (M_{d1}+M_{q1})p & (M_{d1}-M_{q1})p & & \\
\hline
b_1 & r_{sd1}-r_{sq1}+(L_{sd1}-L_{sq1})p & r_{sd1}+r_{sq1}+(L_{sd1}+L_{sq1})p & (M_{d1}-M_{q1})p & (M_{d1}+M_{q1})p & & \\
\hline
f_r & (M_{d1}+M_{q1})(p-jp\theta_1) & (M_{d1}-M_{q1})(p-jp\theta_1) & 2r_{r1}+(L_{rd1}+L_{rq1})\cdot(p-jp\theta_1)+2r_{r2}+(L_{rd2}+L_{rq2})\cdot(p-jp\theta_1) & (L_{rd1}-L_{rq1})(p-jp\theta_1)+(L_{rd2}-L_{rq2})\epsilon^{-2j\delta}[p-j(2p\theta_2-p\theta_1)] & -(M_{d2}+M_{q2})\cdot(p-jp\theta_1) & -(M_{d2}+M_{q2})\cdot(p-jp\theta_1) \\
\hline
b_r & (M_{d1}-M_{q1})(p+jp\theta_1) & (M_{d1}+M_{q1})(p+jp\theta_1) & (L_{rd1}-L_{rq1})\cdot(p+jp\theta_1)+\epsilon^{2j\delta}(L_{rd2}-L_{rq2})\cdot[p+j(2p\theta_2-p\theta_1)] & 2r_{r1}+(L_{rd1}+L_{rq1})\cdot(p+jp\theta_1)+2r_{r2}+(L_{rd2}+L_{rq2})\cdot(p+jp\theta_1) & -\epsilon^{2j\delta}(M_{d2}-M_{q2})[p+j(2p\theta_2-p\theta_1)] & -\epsilon^{2j\delta}(M_{d2}+M_{q2})\cdot[p+j(2p\theta_2-p\theta_1)] \\
\hline
f_2 & & & -(M_{d2}+M_{q2})\cdot[p+j(p\theta_2-p\theta_1)] & -(M_{d1}-M_{q2})\cdot\epsilon^{-2j\delta}[p-j(p\theta_2-p\theta_1)] & r_{sd2}+r_{sq2}+(L_{sd2}+L_{sq2})\cdot[p+j(p\theta_2-p\theta_1)] & r_{sd2}-r_{sq2}+(L_{sd2}-L_{sq2})\cdot[p+j(p\theta_2-p\theta_1)] \\
\hline
b_2 & & & -(M_{d2}-M_{q2})\cdot[p+j(p\theta_2-p\theta_1)] & -(M_{d2}+M_{q2})\epsilon^{-2j\delta}\cdot[p-j(p\theta_2-p\theta_1)] & r_{sd2}-r_{sq2}+(L_{sd2}-L_{sq2})[p+j(p\theta_2-p\theta_1)] & r_{sd2}+r_{sq2}+(L_{sd2}+L_{sq2})\cdot[p+j(p\theta_2-p\theta_1)] \\
\hline
\end{array} \qquad (21)$$

$$\Delta E=\frac{1}{4}\times\begin{array}{c|l|l}
 & \Delta\theta_2 & \Delta\theta_1 \\
\hline
f_1 & & \\
b_1 & & \\
f_r & (L_{rd2}-L_{rq2})\epsilon^{-2j\delta}\{-2ji^{br}p-2j[p-j(2p\theta_2-p\theta_1)]i^{br}\} & 4E_{f1}p-j[(X_{rd2}+X_{rq2})i^{fr}-(X_{md2}+X_{mq2})i^{f2}-(X_{md2}-X_{mq2})i^{b2}]p+(L_{rd2}-L_{rq2})\epsilon^{-2j\delta}\{ji^{br}p+2j[p-j(2p\theta_2-p\theta_1)]i^{br}\} \\
b_r & (L_{rd2}-L_{rq2})\epsilon^{2j\delta}\{2ji^{fr}p+2j[p+j(2p\theta_2-p\theta_1)]i^{fr}\}+(M_{d2}-M_{q2})\epsilon^{2j\delta}\{-2ji^{f2}p-2j[p+j(2p\theta_2-p\theta_1)]i^{f2}\}+(M_{d2}+M_{q2})\epsilon^{2j\delta}\{-2ji^{b2}p-2j[p+j(2p\theta_2-p\theta_1)]i^{b2}\} & 4E_{b1}p+j(X_{rd2}+X_{rq2})i^{br}p+(L_{rd2}-L_{rq2})\epsilon^{2j\delta}\{-ji^{fr}p-2j[p+j(2p\theta_2-p\theta_1)]i^{fr}\}+(M_{d2}-M_{q2})\epsilon^{2j\delta}\{ji^{f2}p+2j[p+j(2p\theta_2-p\theta_1)]i^{f2}\}+(M_{d2}+M_{q2})\epsilon^{2j\delta}\{ji^{b2}p+2j[p+j(2p\theta_2-p\theta_1)]i^{b2}\} \\
f_2 & (M_{d2}-M_{q2})\epsilon^{-2j\delta}\{ji^{br}p+2j[p-j(p\theta_2-p\theta_1)]i^{br}\}+4je_{f2}\epsilon^{-j\delta}-j[(X_{md2}+X_{mq2})i^{fr}-(X_{sd2}+X_{sq2})i^{f2}-(X_{sd2}-X_{sq2})i^{b2}]p & (M_{d2}-M_{q2})\epsilon^{-2j\delta}\{-ji^{br}p-2j[p-j(p\theta_2-p\theta_1)]i^{br}\}-4je_{f2}\epsilon^{-j\delta}+j[(X_{md2}+X_{mq2})i^{fr}-(X_{sd2}+X_{sq2})i^{f2}-(X_{sd2}-X_{sq2})i^{b2}]p \\
b_2 & (M_{d2}+M_{q2})\epsilon^{-2j\delta}\{ji^{br}p+2j[p-j(p\theta_2-p\theta_1)]i^{br}\}+4je_{b2}\epsilon^{-j\delta}-j[(X_{md2}-X_{mq2})i^{fr}-(X_{sd2}-X_{sq2})i^{f2}-(X_{sd2}+X_{sq2})i^{b2}]p & (M_{d2}+M_{q2})\epsilon^{-2j\delta}\{-ji^{br}p-2j[p-j(p\theta_2-p\theta_1)]i^{br}\}-4je_{b2}\epsilon^{-j\delta}+j[(X_{md2}-X_{mq2})i^{fr}-(X_{sd2}-X_{sq2})i^{f2}-(X_{sd2}+X_{sq2})i^{b2}]p
\end{array}\qquad(22)$$

It should be noted that no matter how the expressions in ΔE are grouped, they cannot be measured on the steady-state network. However if the following matrix (equation 14) is added to ΔE

$$\frac{1}{4}\times\begin{array}{c|l|l}
 & \Delta\theta_2 & \Delta\theta_1 \\
\hline
f_1 & & \\
b_1 & & \\
f_r & (L_{rd2}-L_{rq2})(p-jp\theta_2)\epsilon^{-2j\delta}2ji^{br}-(L_{rd2}-L_{rq2})(p-jp\theta_2)\epsilon^{-2j\delta}2ji^{br} & -(L_{rd2}-L_{rq2})(p-jp\theta_2)\epsilon^{-2j\delta}2ji^{br}+(L_{rd2}-L_{rq2})(p-jp\theta_2)\epsilon^{-2j\delta}2ji^{br} \\
b_r & (L_{rd2}+L_{rq2})(p+jp\theta_2)2ji^{br}-(L_{rd2}+L_{rq2})(p+jp\theta_2)2ji^{br} & -(L_{rd2}+L_{rq2})(p+jp\theta_2)2ji^{br}+(L_{rd2}+L_{rq2})(p+jp\theta_2)2ji^{br} \\
f_2 & -(M_{d2}-M_{q2})\epsilon^{-2j\delta}2jpi^{br}+(M_{d2}-M_{q2})\epsilon^{-2j\delta}2jpi^{br} & (M_{d2}-M_{q2})\epsilon^{-2j\delta}2jpi^{br}-(M_{d2}-M_{q2})\epsilon^{-2j\delta}2jpi^{br} \\
b_2 & -(M_{d2}+M_{q2})\epsilon^{-2j\delta}2jpi^{br}+(M_{d2}+M_{q2})\epsilon^{-2j\delta}2jpi^{br} & (M_{d2}+M_{q2})\epsilon^{-2j\delta}2jpi^{br}-(M_{d2}+M_{q2})\epsilon^{-2j\delta}2jpi^{br}
\end{array}\qquad(23)$$

then in the presence of these additional expressions the components of ΔE may be regrouped and expressed in terms of measurable voltages and currents as

$$\Delta E=\frac{1}{4}\times\begin{array}{c|l|l}
 & \Delta\theta_2 & \Delta\theta_1 \\
\hline
f_1 & & \\
b_1 & & \\
f_r & -2j(X_{md2}-X_{mq2})\epsilon^{-2j\delta}(1+h-v)ji^{br} & 2j(X_{md2}-X_{mq2})\epsilon^{-2j\delta}(1+h-v)ji^{br}+4j(E_{f1}+E_{f2})h \\
b_r & -j(X_{rd2}+X_{rq2})2ji^{br}(1+h+v)+8jE_{b2}(1+h+v) & j(X_{rd2}+X_{rq2})2ji^{br}(1+h+v)-8jE_{b2}(1+h+v)+4j(E_{b1}+E_{b2})h \\
f_2 & 2j(X_{md2}-X_{mq2})\epsilon^{-2j\delta}(1+h)ji^{br}-4jE_{f2}h+4jE_{f2}\epsilon^{-j\delta} & -2j(X_{md2}-X_{mq2})\epsilon^{-2j\delta}(1+h)ji^{br}+4jE_{f2}h-4je_{f2}\epsilon^{-j\delta} \\
b_2 & 2j(X_{md2}+X_{mq2})\epsilon^{-2j\delta}(1+h)ji^{br}+4jE_{b2}h+4je_{b2}\epsilon^{-j\delta} & -2j(X_{md2}+X_{mq2})\epsilon^{-2j\delta}(1+h)ji^{br}-4jE_{b2}h-4je_{b2}\epsilon^{-j\delta}
\end{array}\qquad(24)$$

Similarly, it should be noted that in the torque equation 20 the last term cannot be measured off the hunting network. Hence, if to the torque equation the following matrix (equation 17)

$$-\gamma_t^{*}\cdot V\cdot i+\gamma_t^{*}\cdot V\cdot i=\begin{array}{c|l|l}
 & \Delta\theta_2 & \Delta\theta_1 \\
\hline
f_r & -j(X_{rd2}-X_{rq2})\epsilon^{-2j\delta}2ji^{br}+j(X_{rd2}-X_{rq2})\epsilon^{-2j\delta}2ji^{br} & j(X_{rd2}-X_{rq2})\epsilon^{-2j\delta}2ji^{br}-j(X_{rd2}-X_{rq2})\epsilon^{-2j\delta}2ji^{br} \\
b_r & j(X_{rd2}+X_{rq2})2ji^{br}-j(X_{rd2}+X_{rq2})2ji^{br} & -j(X_{rd2}+X_{rq2})2ji^{br}+j(X_{rd2}+X_{rq2})2ji^{br}
\end{array}\qquad(25)$$

is added, then the torque equation may be regrouped, expressed as

$$\Delta T=i\cdot\Delta E'^{*}+\Delta i\cdot E^{*}\qquad(26)$$

and measured off the hunting network as usual.

The corresponding equivalent circuits are given in Figure 2 and the torque measurements in Table I. There are two similar hunting networks for each case of hunting, one containing everywhere $+h$ the other $-h$.

The two shaded circles represent a phase shifter, one shifting the current, the other the voltage by an angle 2δ. In writing equations for the armature mesh b_r by tracing out the equivalent circuit, it has to be remembered that only one equation need be written since the current generators form only junction pairs.

It should be noted that the curvature tensor enters only in the establishment of the voltage generator $2jE_b$ and the current generator $2ji^b$ that appear at the terminals of the machines.

Synchronous Machines

The same method of reasoning applies when two salient-pole synchronous machines are interconnected. Their equivalent circuits are given in Figure 3 and the torque measurements in Table II.

When a synchronous machine is connected to a transmission line, two cases are possible:

1. The reference frame is rigidly connected to the field pole, and the frame takes part in the oscillation. This case was considered in reference 11. The disadvantage of the resulting equivalent circuit of hunting is that it requires as many generators as there are meshes in the transmission line.

2. The reference frame is rigidly connected to the infinite bus. Since the latter is not assumed to hunt, the transmission network is free of generators. They appear now in the synchronous-machine field meshes, whose number is however few. The equivalent circuits for this case are given in Figure 4 and the torque measurements in Table III. The two Figures 5 and 6 are reproduced from reference 11.

The results of a study made on Figures 4–6 are given in a companion paper.[12]

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Abnormal Overvoltages Caused by Transformer Magnetizing Currents in Long Transmission Lines

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As power-transmission distances have increased, new problems, both technical and economic have been encountered, analyzed, and taken into consideration in the design and operation of power systems. As transmission distances continue to increase it is logical to expect that in addition to the already exposed problems, new ones may arise which previously have not required consideration. It is the purpose of this paper to present an analysis of a technical problem which falls in the latter category.

In the course of studying various problems associated with long-transmission-line phenomena, it has been found by means of the transient analyzer that certain combinations of transmission-line length and system reactance can give rise to overvoltages of considerable magnitude when a lightly loaded transformer is connected to that transmission line. It is shown in the paper that such a system is most susceptible to overvoltage phenomena when the transformer is connected to the receiving end of that transmission line, although, theoretically, similar phenomena may occur for any transformer location including the transformer at the sending end. These overvoltages are in addition to the normal fundamental-frequency voltage rise and are of a combination subharmonic and harmonic nature. In magnitude, this overvoltage may be twice as high as that which can be calculated by conventional methods when transformer magnetizing currents are neglected.

A comprehensive analysis of representative circuits with and without series-capacitor compensation has been made to determine the regions of abnormal behavior and the effectiveness of various practical methods for safely limiting such overvoltages. Such practical means for controlling the overvoltage include proper switching and relaying arrangements, the synchronous condenser, shunt reactors, and resonant shunts.

The results presented include a physical picture of the underlying mechanism which gives rise to the phenomenon. It is felt that these results will be of considerable importance from the standpoint of designing and operating systems involving the transmission of power over distances in the practical range above 200 miles.

Summary and Conclusions

The phenomena discussed in this paper are more likely to occur when the transformer is located at the receiving end and is lightly loaded or carrying no load. While an unloaded or lightly loaded transformer located at any point along a transmission line can give rise to these overvoltage phenomena the system is most susceptible to such disturbances when the transformer is at the receiving end of the line because of the normal frequency rise in voltage with increasing distance from the source end. It should be borne in mind that either end of a long transmission line may become the receiving end in the sense employed in this study. The condition would probably be most serious when accompanied by generator overspeeding immediately following sudden loss of load caused by low side switching, which would simultaneously remove any receiving-end synchronous-condenser capacity otherwise normally present.

In designing a power system requiring transmission distances in the practical range above 200 miles, consideration should be given to the possibility of overvoltages such as these discussed in the paper. The more important factors the effects of which should be evaluated are

1. System frequency (including range due to generator overspeeding).
2. System reactance.
3. Line length and losses.
4. Receiving-end transformer size and saturation characteristics.
5. Switching arrangement
   (*a*). Use of high side breakers, with proper relaying, may prevent unloaded transformers from every being on the receiving end of the line.
   (*b*). If no high side breakers are used, care should be taken so that the receiving-end synchronous-condenser capacity is operated as an integral part of the line and receiving-end transformer.
6. Series capacitors.

Remedial measures which are effective in eliminating the overvoltages for cases falling in the danger region include the following:

1. Switching arrangements (see 5 above).
2. Synchronous condensers.
3. Shunt reactors.
4. Second harmonic shunts.

Shunt reactors or the second harmonic shunt should be an integral part of the line, preferably located at or near the terminals of the receiving-end transformer for maximum effectiveness. They need not be on the high-voltage side of the transformer. To make the second harmonic shunt effective over a range of frequencies likely to be encountered as a result of possible overspeeding, it would be preferable to have it tuned for a frequency slightly higher than the second harmonic. However, with series capacitors, the second harmonic shunt alone is not capable of stabilizing the voltage at the receiver end.

In some specific cases it may be desirable to set up the system in miniature to

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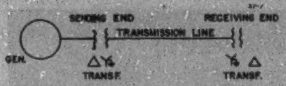


Figure 1. One-line diagram of system studied

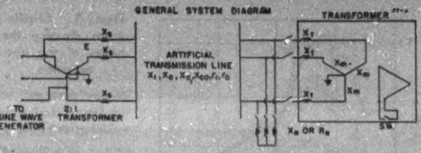


Figure 2. Three-phase miniature system equivalent of the system shown in Figure 1

evaluate accurately the effects of all of the factors involved under various conditions of operation.

System Studied

Figure 1 shows the basic circuit analyzed. In a system such as this, the receiving-end transformer would become considerably overexcited if its load were suddenly lost during a system disturbance severe enough to cause the receiving-end low-voltage circuit breakers to open. This would leave the transformer energized with no load and thus with only exciting current flowing. However, the magnetizing impedance of a transformer, while fundamentally reactive, is a variable quantity because of saturation of the iron core. During periods of saturation, relatively high peak currents may flow momentarily.

Analysis of the problem involving a distributed constant line, and a saturable reactance would be extremely complicated and time-consuming using ordinary methods. Therefore the transient-analyzer method of miniature-system representation, which is ideally suited to the study of such nonlinear circuit phenomena,[3,10,11] was used. For the purpose of analysis by this method, the system of Figure 1 was set up as shown in Figure 2. The artificial transmission line and the miniature transformers used at the receiving end have been described previously,[1,2] and so no space will be devoted to a description of the miniature system in this paper. It will be sufficient to point out that the abruptness of transformer saturation could be varied simply by varying X_t as shown in Figure 3. The values of X_t used were always small relative to X_m so that X_m remained substantially independent of X_t/X_m. Consequently the connected receiving transformer kilovolt-amperes remained essentially independent of X_t/X_m, and only the saturation characteristic of the receiving-end transformer was modified by varying X_t/X_m as shown in Figure 3. Each single-phase transformer used at the receiving end had a magnetizing impedance of $X_m = 13{,}000$ ohms at normal voltage of 110 volts. Two of these units could be put in each phase in parallel, thus halving the value of X_m, or two units could be put in series in each phase (provided values of E *in volts* were doubled) thus doubling the value of X_m. In any case, the receiving-end transformer kilovolt-amperes to be associated with any value of X_m is given by the following relation.

$$X_m = \frac{\text{kv}^2 \times 1{,}000}{\text{kva} \times I_m}$$

where

X_m is the receiving-end transformer magnetizing reactance in ohms per phase at normal voltage
Kv is normal system line-to-line voltage
Kva is the receiving-end transformer-bank rating
I_m is the exciting current in per unit per phase at normal voltage.

Thus, if $X_m = 13{,}000$ ohms, and if a three per cent exciting current and a 230-kv system are assumed, the receiver-end transformer kilovolt-ampere rating represented by the miniature system would be

$$\text{kva} = \frac{(230)^2(1{,}000)}{13{,}000(0.03)} = 135{,}000 \text{ kva}$$

Provision was made for operating the receiving-end transformer with or without the delta open. Shunt reactors or resistors of variable kilovolt-ampere rating, as well as resonant shunts and synchronous condensers, could be switched in at the receiving end. Series capacitors for line-reactance compensation could be inserted as desired. Provision was made for varying the resistance of the transmission line and of the receiving-end transformer and for varying the sine-wave generator voltage, thus changing the operating flux density in the receiving-end transformer as desired. Consequently numerous factors separately or in combination could be carefully studied.

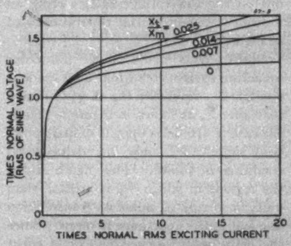


Figure 3. Saturation curves used in this study

D-c resistance of winding expressed as a ratio to X_m is 0.0003. $X_m = 13{,}000$ ohms at 110 volts, 60 cycles, for one transformer

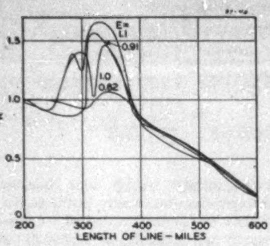
The effect of lightning arresters in limiting these overvoltages and thus protecting terminal equipment was not included in this study. It follows from the results revealing the nature of the phenomenon, however, that lightning arresters may be damaged by such sustained overvoltages.

Nomenclature

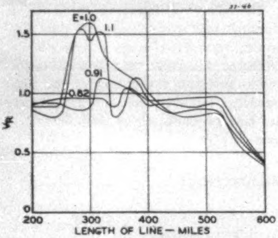
E = voltage back of X_s in per unit
X_s = source reactance
X_t = transformer series reactance (for saturation adjustment)
$x_0/x_1 = \dfrac{\text{zero-sequence line reactance}}{\text{positive-sequence line reactance}}$
$r_1/x_1 = \dfrac{\text{line resistance}}{\text{line reactance}}$ (positive sequence)
$V_R = \dfrac{\text{voltage at receiving end with transformer}}{\text{voltage at receiving end without transformer}}$
[illegible]$_R = \dfrac{\text{(positive-sequence capacitive reactance)}}{\text{shunt reactance}}$
$r_0/x_0 = \dfrac{\text{line resistance}}{\text{line reactance}}$ (zero sequence)
f = cycles per second
X_m = magnetizing reactance in ohms at normal voltage and frequency
X_{c1}, X_{c0} = positive- and zero-sequence shunt capacitive reactance of line respectively
X_{sc} = series-capacitor reactance
X_L = nonsaturable reactance in parallel with X_{sc}
R = resistance in parallel with X_{sc}
X_R = nonsaturable shunt reactance at receiving-end transformer terminals
R_R = shunt resistors at receiving-end transformer terminals

Discussion of Results

With the system set up as in Figure 2, curves as shown in Figures 4a and 4b were obtained. It should be emphasized that the voltage V_R is a ratio of crest voltage to ground at the receiver end with the transformer connected, to the crest voltage at the same location with the transformer disconnected. Thus the normal rise in the line is eliminated from



(a). Delta closed



(b). Delta open

Figure 4. Voltages V_R obtained in the system of Figure 2

$X_s=86$ ohms
$f=60$ cycles per second
$x_0/x_1=4.0$
$r_1/x_1=0.1$
$X_l/X_m=0.014$
$X_m=13{,}000$ ohms
No series-capacitor compensation
No load on system
No shunt reactors

Figure 5. Oscillograms showing the nature of the phenomenon

System constants as in Figure 4a, with 320 miles of line

A. V_R—Phase-A voltage to ground at receiving-end transformer terminals

B. Receiving-end phase-A transformer magnetizing current

C. Voltage to ground at sending end times that which would exist without the transformer

the results, and the effect of the transformer alone is shown by these figures.

The oscillograms shown in Figure 5 are typical illustrations. No two of these oscillograms were taken simultaneously, so it is not possible to correlate instantaneous values of voltage and current. However, the variation in voltage and current wave shapes with time is indicated. The voltage is seen to be made up largely of a second harmonic and subharmonics. The current traces clearly show evidence of subharmonics and even harmonics. The time required for this condition to be reached following sudden loss of load varies from a couple of cycles to a second or two depending upon the value of E and the system constants.

The values of E maintained back of X_s range from a maximum of 1.1 down to values such that the effect of the transformer is negligible from the standpoint of producing overvoltages. It is felt that this covers the practical range, including the case of sudden loss of load at the receiving end. Unit voltage $E=1.0$ defines the normal operating point on the saturation curves of Figure 3 when applied directly to the terminals of the transformer.

It is to be noted that if E is reduced approximately to values such that the receiving-end transformer is not overexcited, no overvoltages will occur. It is of significance also that the region of abnormality is not greatly affected by the delta being opened or closed. Furthermore, the phenomenon is essentially independent of the method of grounding the receiving-end transformer neutral when a closed delta winding is present.

It should be noted that, while the system studied is characterized by a "sending end" and a "receiving end," in the actual case, either end may become the receiving end in the sense employed here. That is, during a system disturbance, either end of the line may be switched out on the low-voltage side, leaving an unloaded system of the type illustrated in Figure 2.

Similar tests made under numerous miniature-system conditions have made it possible to summarize the effect of source reactance X_s as shown in Figure 6. Since the case of the delta open is of little practical significance, only the delta-closed condition is shown. This figure shows the regions in which V_R is greater than unity in terms of miles of transmission line and source reactance in ohms. Since the delta being opened has little effect on the region of abnormality in general, it is to be concluded that the reactance of the tertiary winding is of little consequence. Furthermore the negligible effect of the delta being opened has some significance from the standpoint of the theory involved, as will be shown later.

The curves of Figure 6 indicate that overvoltages are to be expected over a range of line lengths from 200 miles on up to slightly over 400 miles when the system frequency is 60 cycles per second, and source reactances are in the practical range. With lower frequencies the line lengths could be increased inversely as the frequency before encountering the difficulty. The maximum value of V_R obtained was 1.8 with the delta closed, and the length of line producing the maximum overvoltage is shown to increase as the source reactance decreases. Thus, under system conditions corresponding to these, voltages on the order of twice those normally expected may exist at the terminals of the receiving-end transformer if the line length and system source reactance are in the right combination, and if corona and lightning arresters are not voltage-limiting factors.

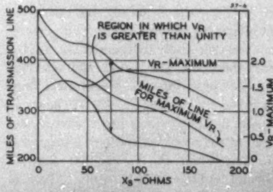


Figure 6. Boundaries of overvoltage region as affected by line length and source reactance with receiving-end transformer delta closed

Frequency 60 cycles per second

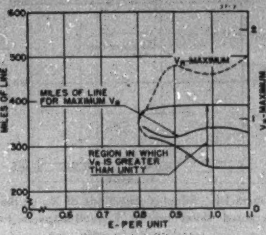


Figure 7. **Effect of sending-end voltage**

System constants as in Figure 4a

While the curves of Figure 6 are for the transformer located at the receiving end of the transmission line, it can be shown (see Appendix II) that the critical combination of length of line and source reactance which is susceptible to these phenomena is independent of the transformer location. Even the sending-end transformer can produce the same phenomena for the same range of constants shown in Figure 6. However, a somewhat higher range of values of E (voltage back of equivalent system reactance, X_s) would be required to give rise to the phenomenon in this case. The receiving-end transformer location renders a given system more vulnerable to such disturbances because of the normal-frequency voltage rise in the line.

The effect of the voltage E maintained within the system is shown in Figure 7 for the same case as in Figure 4. As the system voltage is decreased, the range of system constants capable of giving rise to this abnormality decreases, and at $E=0.8$ the phenomenon disappears entirely. This suggests the theoretical possibility of avoiding such overvoltage phenomena by using low-flux density transformers, although other means may generally be more desirable.

The effect of varying the magnetizing reactance X_m is illustrated in Figure 8. It is believed that this covers the practical range of X_m and shows that the phenomenon is not critical to a particular value of X_m. Obviously if X_m is ∞ the value of $V_R=1$ as shown, and if very high values of X_m are connected to the transmission line, little disturbance of the type under discussion would be expected. In order to make the results more general, values of X_m are given in ohms, but they can readily be converted to specific transformer kilovolt-ampere ratings in systems of given voltage rating, if the magnetizing current in per cent is known or assumed. The formula given in the preceding section can be used for this purpose.

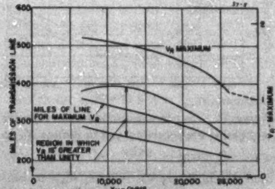


Figure 8. **Effect of varying the receiving-end transformer magnetizing reactance X_m**

System constants as in Figure 4a

The effect of line resistance is illustrated in Figure 9. It appears that, if r_1/x_1 could be as high as 0.4 or higher, the phenomena would not occur. However, practical values of r_1/x_1 in lines such as these under discussion would fall considerably below this value—on the order of 0.2 or less. Therefore line resistance would not be expected to eliminate the overvoltages in a practical case. Furthermore, any attempt to increase the circuit resistance may result in an increased tendency for hunting to take place.[3,4,12]

The effect of steepness of saturation is illustrated in Figure 10. It is evident from this illustration that the phenomenon is not critical to the abruptness of saturation over a practical range, although the more abrupt saturation characteristic in general appears to lead to higher voltages and broaden the range of constants for which they may be obtained.

The effect of generator overspeeding is illustrated in Figure 11. A system normally not in the region of overvoltages may be brought into the region by generator overspeeding as a result of loss of load.

After observing the effects of various factors in producing these overvoltages, means were investigated for controlling them. Among the first means investigated was shunt reactors placed at the terminals of the receiving-end transformer. The ability of reactors to accomplish the desired purpose is illustrated in Figure 12. It is interesting to note that shunt reactor kilovolt-amperes as high as 65 per cent of normal line-charging kilovolt-amperes may be required to keep V_R down to unity. This amount of reactance does not include compensation for the normal rise in the line since V_R as defined is independent of the normal rise in voltage in the line. If generator overspeeding is an appreciable factor, shunt reactors to take care of the normal fundamental-frequency rise in the line may not prevent the occurrence of this phenomenon because of the increase in applied frequency.

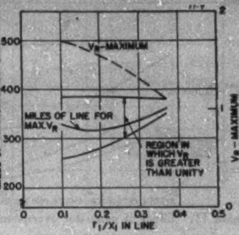


Figure 9. **Effect of line resistance**

System constants as in Figure 4a

Figure 13 indicates that if the receiving-end transformer saturates more abruptly, more reactor kilovolt-amperes may be required to stabilize the voltages

Figure 14 suggests that the maximum required X_c/X_R is likely to increase as the value of X_m decreases. This is representative for the practical range of X_m.

Figure 15 shows that a synchronous condenser is very effective in eliminating the overvoltage hazard. In most actual cases, a condenser at the receiver end of a size greater than the minimum required for overvoltage stability would likely be present. Consequently this is one very practical and effective means of overcoming the difficulty. However, provision must be made for keeping the condenser on the system, even though the load is lost. Thus, in addition to controlling the fundamental-frequency voltage at the receiver end during normal load conditions, a condenser is expected to be effective in eliminating abnormal harmonic and subharmonic voltages such as these during system disturbances.

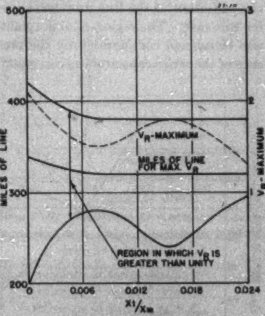


Figure 10. **Effect of abruptness of transformer saturation at receiving end**

System constants as in Figure 4a

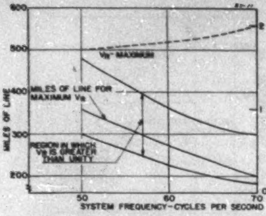


Figure 11. Effect of generator overspeeding

System constants as in Figure 4a

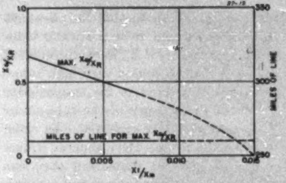


Figure 13. Effect of abruptness of saturation on the required shunt reactor kilovolt-amperes to reduce V_R to unity

System constants as in Figure 4a

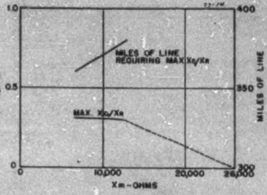


Figure 14. Effect of magnitude of X_m on the required shunt reactor kilovolt-amperes to reduce V_R to unity

System constants as in Figure 4a

The curves of Figure 15 require some discussion, because they show a minimum condenser requirement for the length of line giving rise to the maximum values of V_R. Practically, however, the necessity for reducing V_R at all is greatest when V_R is greatest. Therefore, for practical purposes, the maximum condenser requirements for this given case are more nearly the minimum values shown in Figure 15. In other words, a condenser of 15,000 kva would probably be adequate for keeping V_R from becoming destructive in magnitude for this case.

One other very effective means that has been found for eliminating the overvoltages is the resonant shunt located at the receiver-end transformer. The shunt may be either delta- or wye-connected. If wye-connected, it makes no difference whether the neutral point is grounded or not. Its effectiveness is illustrated in Figure 16. The reason for the effectiveness of the second harmonic shunt is discussed in the following section.

The effect of series capacitors at the mid-point of the transmission line is illustrated in Figure 17. Series capacitors at the mid-point of the line for 50 per cent compensation of the line drop were used for this case. The region of abnormality was broadened considerably by the presence of the series capacitors as can readily be seen by comparing this figure with Figure 4a or 4b. The reason for this broadening of the region of abnormality is that an additional phenomenon, different in nature, is introduced by the presence of series capacitors.[4] This is discussed further in the section headed "Theory."

It is possible to eliminate the phenomenon entirely by using shunt reactors located at the terminals of the receiving-end transformer. The amount of reactance required is shown for one case in Figure 18. In this figure also is shown the amount of shunt resistance required for the same case.

The possibility of using a resistor in shunt with the series capacitor has previously been given some consideration.[4] A reactor in parallel with the series capacitor also is effective as shown in Figure 19, although for this case, the effectiveness of either a resistor or reactor so placed decreases rapidly for lengths of line above 280 miles.

In Figure 20, the curves show the amount of reactance or resistance required in parallel with the series capacitor for the same case, the only difference being that a second-harmonic resonant shunt has been connected to the terminals of the receiving-end transformer. Reactance is seen to be quite effective as a means of eliminating the phenomenon in this case for line lengths up to well over 400 miles, while resistance is not nearly so effective. Furthermore, resistance is undesirable since it introduces losses during normal load conditions which in the proportions required would be intolerable in some cases.

A condenser of practical size at the receiving end is very effective in eliminating the voltage instability, either with or without series capacitors.

Theory

The cause of the phenomena discussed in the preceding section can best be understood by first considering a single-phase circuit as shown in Figure 21. Here the essential elements of the three-phase circuit of Figure 2 are present, the transmission line being represented by a capacitor, X_c, and the source reactance by X_L. The transformer X_m which can be connected or disconnected by the switch, corresponds to the receiving-end transformer of Figure 2 where X_t and X_m define the transformer characteristics.

With V_R defined as in the preceding discussion the results obtained by varying X_c/X_L and X_m/X_L are shown in Figure 22. Here two peaks of overvoltage are

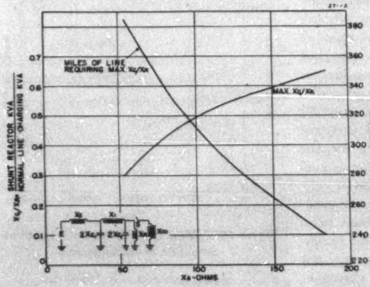


Figure 12. Maximum shunt reactance kilovolt-amperes at receiving-end transformer terminals required to reduce V_R to unity

Shunt-reactor kilovolt-amperes expressed as a ratio to normal line-charging kilovolt-amperes in accordance with the insert. System constants as in Figure 4a

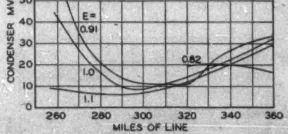


Figure 15. Synchronous-condenser capacity required at the receiving end to reduce V_R to unity

System as in Figure 4a, with $X_t/X_m = 0$, transient reactance of condenser assumed to be 30 per cent on rating in 230-kv system. Voltage back of transient reactance of condenser, 133 per cent

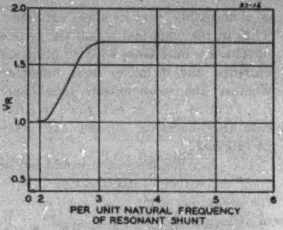


Figure 16. Effect of resonant shunt on V_R

Shunt consists of reactor and capacitor in series located at the terminals of the receiving-end transformer. Reactor 6.0 henrys. Capacitance varied. System constants as in Figure 4a with 320 miles of line

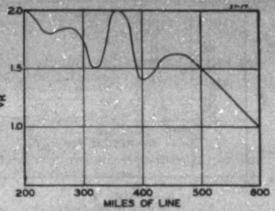


Figure 17. Relationship between V_R and miles of line with series capacitors

Series-capacitor kilovolt-amperes at mid-point of line correcting for 50 per cent of line reactance drop. System constants as in Figure 4a. $E \leq 1.1$

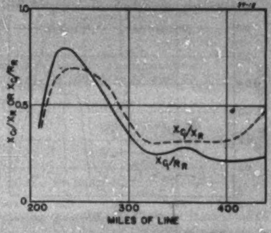


Figure 18. Shunt reactance or resistance required at the receiving-end transformer terminals to hold V_R down to unity for $E \leq 1.1$

System constants as in Figure 4a with 50 per cent series-capacitor line-reactance drop compensation at the mid-point of the line

shown, one largely of second-harmonic and subharmonic nature for X_c/X_L just below 4 and the other largely of third-harmonic nature for X_c/X_L just below 9. The higher values of X_m/X_L cause the peaks of overvoltage to move closer to 4 and 9 respectively. Now when $X_c/X_L = 4$, the circuit viewed from the switch location with the switch open is tuned to the second harmonic, and when $X_c/X_L = 9$, it is tuned to the third harmonic.

For the case of X_c/X_L just below 9, it is not difficult to see why overvoltages will occur. If $X_c/X_L = 9$, the impedance of the circuit viewed from the switch with the switch open is infinite to the third harmonic neglecting resistance. Consequently, in trying to supply the required third to the transformer, a large third-harmonic drop is encountered, and this shows up at the transformer terminals as indicated in the curves. If X_c/X_L is lower than about 7.5 and higher than about 12, the impedance to the third harmonic required by the transformer is low enough essentially to eliminate the overvoltages of the third-harmonic type.

It is to be noted that the overvoltages of this type are a maximum when X_c/X_L is slightly less than 9. This means that the system impedance is capacitive and of a high value to the third harmonic. This is to be expected since, with the switch closed, the path for third harmonics includes X_m also, which, while difficult to evaluate under these saturated conditions, nevertheless is fundamentally reactive to the third and probably compensates for the resultant capacitive reactance of the system to bring the overall system more nearly in tune to the third harmonic at the point giving rise to the maximum V_R shown in Figure 22.

With regard to the peak values of V_R for X_c/X_L slightly less than 4, the explanation is more difficult, because, while the system impedance with the switch open is infinite (neglecting resistance) to the second harmonic when $X_c/X_L = 4$, the transformer in normal steady-state operation with sinusoidal voltage applied does not require a second harmonic to flow. Consequently the preceding explanation, while satisfactory for the region of third-harmonic overvoltages is not satisfactory for the region of second-harmonic overvoltages.

In order to visualize the cause of the second-harmonic overvoltages, it is desirable to substitute the equivalent circuit of Figure 23 for the circuit of Figure 21. In this figure, the two reactances X_L and X_c correspond to the reactances so labelled in Figure 21. The reactances X_m and X_s are constant (not saturable) and represent the transformer of Figure 21.

In this equivalent circuit, the saturation of the transformer is represented by two straight lines as shown in Figure 23b. The effect of saturation of this type is introduced in the equivalent circuit by closing switch S at every instant that the current i into the equivalent transformer exceeds a critical value and opening it at every instant that the current i falls below this critical value. In the illustration of Figure 23b, this critical value has been arbitrarily taken to be 0.9 of the crest value which would be taken by X_m if the switch S were never closed. On this basis the switch S would be opened and closed every half-cycle of fundamental frequency, or the switching cycle would be repeated at a frequency corresponding essentially to second-harmonic frequency, and, since the circuit made up of X_c and X_L is approximately tuned to the second harmonic, it is to be expected that the transient oscillations will be those corresponding at least approximately to the natural frequency as dictated by X_c/X_L.

Furthermore, this concept aids greatly in visualizing the production of subharmonics as well as the second harmonics. Consider the equivalent circuit as operating in a steady-state condition with switch S open. Then assume that on some half-cycle, saturation is to be introduced in accordance with the preceding theory. This will mean that switch S must be closed when i reaches 0.9 of the crest value it had been reaching on previous half-cycles. This instant can also be defined with respect to the fundamental-frequency voltage wave. Following the closing of the switch, a high peak of current will flow momentarily, and, when the total i returns to 0.9, switch S will be opened. This will leave a natural frequency oscillation in the circuit, and, when the current i has gone through zero and reaches a value of 0.9 again, switch S

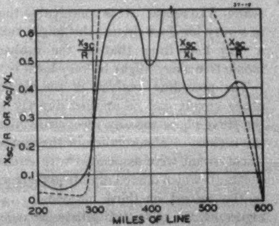


Figure 19. Resistance or reactance required in parallel with series capacitor to hold V_R down to unity for $E \leq 1.1$

X_{sc} = reactance of series capacitors
X_L = reactance in parallel with X_{sc}
R = resistance in parallel with X_{sc}

Other system constants as in Figure 4a

50 per cent series-capacitor line-reactance drop compensation at the mid-point of the line

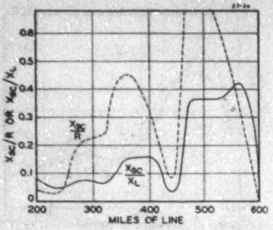


Figure 20. Resistance or reactance required in parallel with series capacitors to hold V_R down to unity for $E \leq 1.1$

X_{sc} = reactance of series capacitors
X_L = reactance in parallel with X_{sc}
R = resistance in parallel with X_{sc}

Second harmonic shunt at receiving-end transformer terminals. Shunt made up of 2,000 ω inductance in series with 0.32-microfarad capacitance wye-connected and ungrounded. Other system constants as in Figure 4a. 50 per cent series-capacitor line-reactance drop compensation at the mid-point of the line

must be closed. This time, however, the instant of closing is not the same with respect to the fundamental-frequency voltage wave as it was on the first half-cycle because of the transient condition. However, the switch must be opened again when the current decreases to 0.9 in accordance with our criterion. Furthermore, the next half-cycle will likely require that switch S be closed at still a different point on the voltage wave so that several cycles may elapse before two cycles are identical or nearly identical. Consequently subharmonics must appear, and the lowest subharmonic will be that corresponding to the period in cycles of fundamental frequency required for the exact initial conditions of closing switch S to repeat. It is conceivable that in a very low loss circuit, this might take a very long time, that is, the subharmonic frequency might be very low.

The preceding analysis of the single-phase circuit can be applied directly to the three-phase case by recognizing that the regions of abnormality in the single-phase circuit were associated with circuit constants which offered infinite impedance to the second or third harmonics. In Figure 24, the relationship between miles of line and source reactance for which infinite impedance is offered to harmonics is shown. Also the region in which V_R was found to be greater than unity (from Figure 6) is shown. It is significant that this region is close to the curve giving the relationship between miles of line and source reactance which offers infinite impedance to the second harmonic. Thus it corresponds to the single-phase case which for X_c/X_L slightly less than 4 gave rise to overvoltages of a similar nature. Furthermore, the maximum values of overvoltage in the single-phase case occurred when X_c/X_L was slightly less than 4. This meant that the impedance of the system was actually capacitive. In Figure 24, the region of abnormality lies above the line defining infinite impedance to the second harmonic. This also means that the reactance in this region is capacitive, thus further establishing the similarity between the three-phase and the single-phase case.

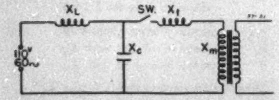


Figure 21. Single-phase lumped-constant circuit studied

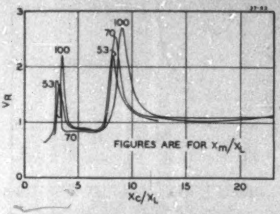


Figure 22. Results obtained in the study of the circuit shown in Figure 21

In Figure 24, a curve is shown for infinite impedance to the third harmonic. Infinite impedance to the third harmonic occurs naturally for shorter line lengths than for the second harmonic for a given source reactance. However, with a receiving-end transformer having a closed delta, the third harmonic is not necessarily supplied from the line, and therefore the system impedance has little effect. If the receiving-end transformer had no closed delta, then a region of overvoltages should be expected here of third-harmonic nature corresponding to the case of X_c/X_L slightly less than 9.0 in Figure 22. This region does exist but is considered of little practical significance in connection with this paper and so is not included in the curves.

The question as to why the delta being opened or closed made little difference in the phenomenon is answered in Figure 24. Curves showing the relationship between miles of line and source reactance for zero impedance viewed from the receiving end are shown. The curve for zero impedance to the third harmonic is shown to fall partially within the region of abnormality, near its upper extremity. Thus in the abnormal region the system offers a low impedance to third-harmonic currents, and it makes little difference whether the receiving-end transformer has a closed delta or not, since in either case third-harmonic current can easily be supplied.

The preceding discussion would not be complete without some mention of why the second-harmonic shunt is so effective in reducing the abnormal condition in the long lines case. In the single-phase case, the third-harmonic region of overvoltages could be eliminated by a third-harmonic resonant shunt, while the second-harmonic region could be eliminated by a second-harmonic shunt. By analogy between the single-phase and three-phase circuits it is evident that the abnormal region shown in Figure 24 or Figure 6 can be eliminated by a second-harmonic shunt. Furthermore, for reasons just mentioned, with the delta closed, no region of abnormality appears for the third harmonic. Consequently, for the delta closed condition, which is the practical case, a second-harmonic shunt is completely effective in eliminating the abnormal condition brought about by exciting power transformers through long lines.

The preceding analysis has dealt with only the second and third harmonics. Higher harmonics, when suppressed by systems offering infinite impedance to them will produce voltage distortions, but the overvoltages produced will in general be of no practical significance as compared to the phenomena as discussed in this paper.

While the foregoing discussion has dealt primarily with long lines carrying magnetizing currents, it is possible that

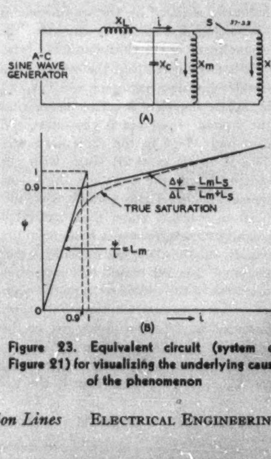


Figure 23. Equivalent circuit (system of Figure 21) for visualizing the underlying cause of the phenomenon

the phenomena observed may be encountered in other cases when the load requires harmonic currents to flow through long lines or from sources which tend to suppress them.

When series capacitors are present, the impedance of the system viewed from the receiving-end transformer terminals remains only slightly changed as far as harmonics are concerned. This is readily understood, because with any practical series-capacitor compensation the reactance of the series capacitor varies inversely as the frequency. Consequently, in the region which caused voltage instability without series capacitors, it is to be expected that instability of the same type will be found with series capacitors. In addition, the presence of series capacitors introduces the possibility of series resonant conditions in a nonlinear circuit consisting essentially of source and line reactance, series capacitor, and saturable reactor (transformer) which accounts for the broadening of the region of abnormality shown in Figure 17. This circuit has been discussed in the technical press, and its behavior is generally well understood.[4–8] It generally requires some form of "shock" excitation in order for the subharmonics to appear. That is, there are several modes of ultimate behavior depending on the initial conditions. This is in contrast to the phenomenon characteristic of the circuit shown in Figure 21 which is discussed in this paper and which generally does not require shock excitation for the phenomenon to appear. The harmonics and subharmonics appear spontaneously, within a few cycles generally, and represent the only mode of steady-state behavior for the circuit. That is, the circuit can respond only in accordance with the curves of Figures 4a and 4b whether the system voltage is raised gradually or whether the system is suddenly energized.

Appendix I

In the equivalent circuit of Figure 19, the per-unit current i_S (base current $=1/L_m$) through the switch following the closing of it at an initial angle θ after a positive voltage crest (assumed to the unity) is

$$i_{S_1}=A_1\sin(t+\theta_1)+B_1\sin(\omega_C t+\phi_1)+D_1 \quad (1)$$

where

$$A_1=\frac{X_m(\omega_o^2-1)}{X_s(\omega_C^2-1)}$$

$$B_1=\frac{X_m(\omega_o^2-\omega_C^2)}{X_s\omega_C^2(1-\omega_C^2)}\sqrt{\sin^2\theta_1+\omega_C^2\cos^2\theta_1}$$

$$D_1=-\frac{X_m\omega_o^2\sin\theta_1}{X_s\omega_C^2}$$

$$\omega_o=\sqrt{\frac{X_c(X_m+X_L)}{X_mX_L}}$$

the circuit natural frequency in per unit with switch S open

$$\omega_C=\sqrt{\frac{X_c(X_sX_m+X_sX_L+X_mX_L)}{X_mX_LX_s}}$$

the circuit natural frequency in per unit with the switch S closed

$$\sin\phi_1=\frac{\sin\theta_1}{\sqrt{\sin^2\theta_1+\omega_C^2\cos^2\theta_1}}$$

$$\cos\phi_1=\frac{\omega_C\cos\theta_1}{\sqrt{\sin^2\theta_1+\omega_C^2\cos^2\theta_1}}$$

The current i flowing into the equivalent transformer is

$$i_{c1}=A_1'\sin(t+\theta_1)+B_1'\sin(\omega_C t+\phi_1)+D_1' \quad (2)$$

where

$$A_1'=\left(1+\frac{X_s}{X_m}\right)A_1$$

$$B_1'=\left(1+\frac{X_s}{X_m}\right)B_1$$

$$D_1'=\sin\theta_1+\left(1+\frac{X_s}{X_m}\right)D_1$$

The voltage across the capacitor is

$$e_{c1}=F_1\cos(t+\theta_1)+G_1\cos(\omega_C t+\phi_1) \quad (3)$$

where

$$F_1=\frac{(\omega_o^2-1)}{(\omega_C^2-1)}$$

$$G_1=\frac{(\omega_o^2-\omega_C^2)}{\omega_C(1-\omega_C^2)}\sqrt{\sin^2\theta_1+\omega_C^2\cos^2\theta_1}$$

Interrupting the current i_S, the voltage across the capacitor is

$$\begin{aligned}e_{o1}=&\cos(t+\theta_1')+H_1\sin\omega_o t+J_1\times\\&\cos(\omega_o t+\alpha_1)+K_1\cos(\omega_o t+\beta_1) \quad (4)\\=&\cos(t+\theta_1')+M_1\cos(\omega_o t+\Sigma)\end{aligned}$$

where

$$H_1=\frac{(\omega_o^2-\omega_C^2)\omega_o\sin\theta_1}{\omega_C^2}$$

$$J_1=\frac{(\omega_o^2-\omega_C^2)}{\omega_C^2(1-\omega_C^2)}\sqrt{\sin^2\theta_1+\omega_C^2\cos^2\theta_1}\times\sqrt{\omega_C^2\cos^2\delta_1+\omega_o^2\sin^2\delta}$$

$$K_1=\frac{(\omega_o^2-\omega_C^2)}{(\omega_C^2-1)}\sqrt{\cos^2\theta_1'+\omega_o^2\sin^2\theta_1'}$$

$$\delta_1=\omega_C t_{c1}+\phi_1$$
$$\theta_1'=t_{c1}+\theta_1$$

t_{c1} is the interval of time in radians during which the switch was closed

$$\sin\alpha_1=\frac{\omega_o\sin\delta_1}{\sqrt{\omega_C^2\cos^2\delta_1+\omega_o^2\sin^2\delta_1}}$$

$$\cos\alpha_1=\frac{\omega_C\cos\delta_1}{\sqrt{\omega_C^2\cos^2\delta_1+\omega_o^2\sin^2\delta_1}}$$

$$\sin\beta_1=\frac{\omega_o\sin\theta_1'}{\sqrt{\cos^2\theta_1'+\omega_o^2\sin^2\theta_1'}}$$

$$\cos\beta_1=\frac{\cos\theta_1'}{\sqrt{\cos^2\theta_1'+\omega_o^2\sin^2\theta_1'}}$$

The current i becomes

$$i_{o1}=\sin(t+\theta_1)-\frac{H_1}{\omega_o}\cos\omega_o t+\frac{J_1}{\omega_o}\times \sin(\omega_o t+\alpha_1)+\frac{K_1}{\omega_o}\sin(\omega_o t+\beta_1) \quad (5)$$

Closing the switch at the next instant when the current i reaches the critical value

$$\begin{aligned}i_{S_2}=&A_1\sin(t+\theta_2)+B_2\sin(\omega_C t+\phi_2)+\\&D_2+M_1S_2\sin(\omega_C t+\psi_2)+W_2M_1 \quad (6)\end{aligned}$$

A_1 defined in equation 1

$$B_2=\frac{X_m(\omega_o^2-\omega_C^2)}{X_s\omega_C^2(1-\omega_C^2)}\sqrt{\sin^2\theta_2+\omega_C^2\cos^2\theta_2}$$

$$D_2=-\frac{X_m\omega_o^2\sin\theta_2}{X_s\omega_C^2}$$

$$\theta_2=t_{s1}+t_{c1}+\theta$$

$$\sin\phi_2=\frac{\sin\theta_2}{\sqrt{\sin^2\theta_2+\omega_C^2\cos^2\theta_2}}$$

$$\cos\phi_2=\frac{\omega_C\cos\theta_2}{\sqrt{\sin^2\theta_2+\omega_C^2\cos^2\theta_2}}$$

$$S_2=\frac{X_m}{\omega_C^2X_s}\sqrt{\omega_o^2\sin^2\Delta_2+\omega_C^2\cos^2\Delta_2}$$

$$\sin\psi_2=\frac{\omega_o\sin\Delta_2}{\sqrt{\omega_o^2\sin^2\Delta_2+\omega_C^2\cos^2\Delta_2}}$$

$$\cos\psi_2=\frac{\omega_C\cos\Delta_2}{\sqrt{\omega_o^2\sin^2\Delta_2+\omega_C^2\cos^2\Delta_2}}$$

$\Delta_2=$ angle of ω_o frequency voltage past positive voltage crest when switch is closed

$$W_2=-\frac{X_m\omega_o\sin\Delta_2}{X_s\omega_C^2}$$

The voltage across the capacitor is

$$e_{c2}=\frac{X_s}{X_m}\frac{di_{S_2}}{dt}$$

and the current i_{c2} into the equivalent transformer is of the form of equation 2.

At the time of interruption, the current i_{o2} takes the form of equation 5 subject to the new boundary conditions. The voltage e_{o2} takes the form of equation 4 also subject to the new boundary conditions.

This procedure can go on for as long as desired, each time imposing the new boundary conditions. It can be seen readily that these successive boundary conditions will not in general be the same for adjacent cycles of fundamental frequency. That is $\theta_1+\pi\neq\theta_2$, $\theta_2+\pi\neq\theta_3$, ... $\theta_n+\pi\neq\theta_{n+1}$. Ultimately, however, $\theta_n+a\pi=\theta_{n+a}$ and a definite subharmonic frequency will appear. Furthermore, the voltage oscillation across the capacitor will be of fundamental frequency plus a natural-frequency oscillation which is predominantly that defined as ω_o, the natural frequency of the system with the switch open. This is brought about by the fact that the switch S is closed in general only a short fraction of a fundamental-frequency period, and that period is generally in the vicinity of fundamental-frequency voltage zero. Therefore, it is to be expected that when circuit losses are low, the natural frequency of the circuit with the switch open and subharmonics will appear across the capacitor in the circuit of Figure 23.

Appendix II

Viewed from the receiving end, the system of Figure 1 or 2 neglecting losses offers an impedance[9]

$$Z_r=j\frac{\omega L_s\cos\omega l\sqrt{LC}+\sqrt{\frac{L}{C}}\sin\omega l\sqrt{LC}}{\cos\omega l\sqrt{LC}-\omega L_s\sqrt{\frac{C}{L}}\sin\omega l\sqrt{LC}}\qquad(1)$$

where

L = inductance per mile in henrys
C = capacitance per mile in farads
L_s = source inductance in henrys
$\omega=2\pi f$
l = length of line in miles

In the transient-analyzer artificial line

$L=0.002$ henry per mile
$C=0.016$ microfarad per mile

For $Z_r=\infty$, the criterion is

$$\cos\omega l\sqrt{LC}-\omega L_s\sqrt{\frac{C}{L}}\sin\omega l\sqrt{LC}=0\qquad(2)$$

or, rewriting

$Zr=\infty$

when

$$\tan\omega l\sqrt{LC}=\frac{1}{\omega L_s}\sqrt{\frac{L}{C}}\qquad(3)$$

This equation was used with the above numerical constants for plotting the curves relating source reactance and miles of line to give infinite impedance in Figure 20.

For $Z_r=0$, the criterion is

$$\omega L_s\cos\omega l\sqrt{LC}+\sqrt{\frac{L}{C}}\sin\omega l\sqrt{LC}=0\qquad(4)$$

or, rewriting

$Z_r=0$

when

$$\omega l\sqrt{LC}=-\omega L_s\sqrt{\frac{C}{L}}\qquad(5)$$

This equation was used with the above numerical constants for plotting the curves relating source reactance and miles of line to give zero impedance in Figure 24.

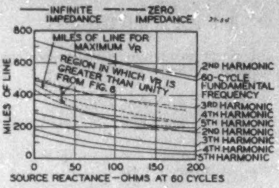


Figure 24. Relationship between miles of line and source reactance which offers infinite or zero impedance to harmonics viewed from any point along the line

Resistance neglected

It can also be shown that the relationship between source reactance ωL_s and line length to give infinite impedance is independent of the point considered along the line. If l is the total length of line considered, and d is the distance between the sending end and the point from which the impedance is to be determined, then

$$Z=\frac{-j\sqrt{\frac{L}{C}}\cot\omega(l-a)\sqrt{LC}\cdot j\times\left[\frac{\omega L_s\cos\omega d\sqrt{LC}+\sqrt{\frac{L}{C}}\sin\omega d\sqrt{LC}}{\cos\omega d\sqrt{LC}-\omega L_s\sqrt{\frac{C}{L}}\sin\omega d\sqrt{LD}}\right]}{-j\sqrt{\frac{L}{C}}\cot\omega(l-d)\sqrt{LC}+j\times\left[\frac{\omega L_s\cos\omega d\sqrt{LC}+\sqrt{\frac{L}{C}}\sin\omega d\sqrt{LC}}{\cos\omega d\sqrt{LC}-\omega L_s\sqrt{\frac{C}{L}}\sin\omega d\sqrt{LC}}\right]}$$

$$-\sqrt{\frac{L}{C}}\cot\omega(l-d)\sqrt{LC}\times$$

$$=\frac{\left[\omega L_s\cos\omega d\sqrt{LC}+\sqrt{\frac{C}{L}}\sin\omega d\sqrt{LC}\right]}{\left[-\sqrt{\frac{L}{C}}\cot\omega(l-d)\sqrt{LC}\right]\times}+$$

$$\left[\cos\omega d\sqrt{LC}-\omega L_s\sqrt{\frac{C}{L}}\sin\omega d\sqrt{LC}\right]\times$$

$$\omega L_s\cos\omega d\sqrt{LC}+\sqrt{\frac{L}{C}}\sin\omega d\sqrt{LC}\qquad(6)$$

$Z=\infty$ when the denominator of equation 6 is zero. Rewriting this denominator

$$-\sqrt{\frac{L}{C}}\cot\omega(l-d)\sqrt{LC}\cdot\cos\omega d\sqrt{LC}+$$
$$\omega L_s\cot\omega(l-d)\sqrt{LC}\cdot\sin\omega d\sqrt{LC}+$$
$$\omega L_s\cos\omega d\sqrt{LC}+\sqrt{\frac{L}{C}}\sin\omega d\sqrt{LC}$$

$$=-\sqrt{\frac{L}{C}}\cos\omega d\sqrt{LC}\times$$
$$\frac{1+\tan\omega l\sqrt{LC}\tan\omega d\sqrt{LC}}{\tan\omega l\sqrt{LC}-\tan\omega d\sqrt{LC}}+\omega L_s\times$$
$$\sin\omega d\sqrt{LC}\,\frac{1+\tan\omega l\sqrt{LC}\tan\omega d\sqrt{LC}}{\tan\omega l\sqrt{LC}-\tan\omega d\sqrt{LC}}+$$
$$\omega L_s\cos\omega d\sqrt{LC}+\sqrt{\frac{L}{C}}\sin\omega d\sqrt{LC}=0$$

$$-\sqrt{\frac{L}{C}}\cos\omega d\sqrt{LC}-\sqrt{\frac{L}{C}}\cos\omega d\sqrt{CL}\times$$
$$\tan\omega l\sqrt{LC}\tan\omega d\sqrt{LC}+\omega L_s\sin\omega d\sqrt{LC}+$$
$$\omega L_s\sin\omega d\sqrt{LC}\tan\omega l\sqrt{LC}\tan\omega d\sqrt{LC}+$$
$$\omega L_s\cos\omega d\sqrt{LC}\tan\omega l\sqrt{LC}-$$
$$\omega L_s\cos\omega d\sqrt{LC}\tan\omega d\sqrt{LC}+$$
$$\sqrt{\frac{L}{C}}\sin\omega d\sqrt{LC}\tan\omega l\sqrt{LC}-$$
$$\sqrt{\frac{L}{C}}\sin\omega d\sqrt{LC}\tan\omega d\sqrt{LC}=0$$

$$\omega L_s[\sin\omega d\sqrt{LC}+\sin\omega d\sqrt{LC}\tan\omega l\sqrt{LC}\times$$
$$\tan\omega d\sqrt{LC}+\cos\omega d\sqrt{LC}\tan\omega l\sqrt{LC}-$$
$$\cos\omega d\sqrt{LC}\tan\omega d\sqrt{LC}]$$
$$=\sqrt{\frac{L}{C}}[\cos\omega d\sqrt{LC}+\cos\omega d\sqrt{LC}\tan\omega l\times$$
$$\sqrt{LC}\tan\omega d\sqrt{LC}-\sin\omega d\sqrt{LC}\tan\omega l\times$$
$$\sqrt{LC}+\sin\omega d\sqrt{LC}\tan\omega d\sqrt{LC}]\omega L_s\times$$
$$\tan\omega l\sqrt{LC}[\sin\omega d\sqrt{LC}\tan\omega d\sqrt{LC}+$$
$$\cos\omega d\sqrt{LC}]=\sqrt{\frac{L}{C}}[\cos\omega d\sqrt{LC}+$$
$$\sin\omega d\sqrt{LC}\tan\omega d\sqrt{LC}]$$

and finally $\tan\omega l\sqrt{LC}=\dfrac{1}{\omega L_s}\sqrt{\dfrac{L}{C}}$

which is identical with equation 3, which was to be proved.

Similarly it can be shown that the condition for $Z=0$ as given by equation 4 holds for any location along the line.

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Self-Excited Oscillations of Capacitor-Compensated Long-Distance Transmission Systems

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THE transmission of electric power over long distances has been the subject of considerable study,[1] and it has been concluded that an effective way of increasing the permissible straightaway transmission distance is by means of series-capacitor compensation of part of the transmission-line inductive reactance. The introduction of longer lines together with capacitor compensation brings up several technical problems which must be considered if proper operation is to be assured. Studies of transient stability and switching times following faults and of abnormal overvoltages which may result from interaction of line capacitance and transformer exciting impedance at no load are presented in references 2 and 3 respectively. This paper presents the results of a study of hunting and self-excitation during normal operation as affected by line and machine characteristics.

Previous work[4] and field experience have indicated the possibility of self-excited oscillations when synchronous machines are connected to capacitor-compensated circuits. It has been shown[5] that a high ratio of line resistance to reactance reduces the machine damping torque, and that the compensation of the line reactance by means of series capacitors increases the effective value of this ratio. It has also been shown[4,6,7] that series-capacitor compensation of line reactance may lead to self-excited electrical oscillations as well as to mechanical rotor oscillations (hunting) under certain circuit conditions.

The series-capacitor-compensated long-distance transmission system is somewhat difficult to analyze directly in terms of previously developed criteria, as in this case there is a considerable distributed shunt capacitance in addition to the series capacitance. This shunt capacitance has been neglected in previous mathematical analyses because of the analytical difficulties involved as well as for the reason that in the previously considered application to distribution circuits[4] the shunt capacitance was relatively small and unimportant. Setting up and solving directly the complete differential equations of the system including a synchronous generator and compensated transmission line are impractical, if the transmission line is represented with any reasonable degree of exactness, as the order is extremely high. The alternative of setting up and studying a laboratory model, as reported in a recent paper[8] and as was done in a previous unpublished study, was not considered desirable because of the difficulty of varying system parameters. Instead it seemed preferable to develop and apply the method of equivalent circuits, using the a-c network analyzer, which had already shown itself to be of practical value in other related studies.[9,10] The equivalent-circuit method has the advantage of ease of varying system parameters over a wide range, as in mathematical analyses, but with a minimum of calculation, as in model testing, since most of the results can be read directly on instruments. The network analyzer is particularly convenient, compared to hand calculations, in the necessary setting up of initial conditions, that is, machine excitations, fluxes, currents, and so forth, which vary with practically every change of any system parameters for a specified system load angle, or other condition.

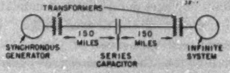

Figure 1. Series-capacitor-compensated transmission system

Transformer reactance = ten per cent (each)
Line reactance = 0.8 ohm per mile
Line resistance = 0.08 ohm per mile
Line capacitance = 5.2×10^{-6} mho per mile
Generator $x_d=110$ per cent, $x_q=74$ per cent
$x_d'=37.7$ per cent
$x_d''=28.1$ per cent, $x_q''=39.3$ per cent (with amortisseur)
Base kilovolt-amperes = $2.5\times(\text{kv})^2$

Conclusions

1. A method of attack using equivalent circuits set up on the a-c network analyzer has been developed which enables one to predetermine from an equivalent circuit the possibility of self-excited oscillations in generators connected to series-capacitor-compensated long-distance transmission lines. A detailed study of any proposed installation can be made by this method, if it appears necessary from a preliminary inspection.

2. In the range of transmission line studied (up to 300 miles and fully compensated) hunting can be prevented by a proper generator amortisseur without encountering electrical self-excitation. An amortisseur is necessary if hunting is to be avoided under all conditions, but self-excitation will not occur unless the line resistance is lower than for usual practical transmission lines.

3. Rotor inertia has little effect on the significant regions of electrical self-excitation.

Procedure

Equivalent circuits for the synchronous generator and for the compensated transmission line during oscillations are developed and described in the companion paper[11] and in reference 12, respectively. Only the procedure used in applying the method to the network analyzer will be discussed here.

The system for which results are presented is shown in Figure 1. It consists of a 300-mile transmission line terminated at both ends by a transformer of ten per cent reactance. A generator having the electrical characteristics of an average water-wheel generator is connected through the transmission line and transformers to an infinite bus. The damping and synchronizing torque coefficients T_d and T_s for a range of oscillation frequencies h have been obtained for several cases. The limits of stable operation are determined in terms of the stable ranges of generator inertia as follows.

For the generator, the torque equation during sinusoidal rotor oscillations may be expressed as:

$$(-Mh^2+jhT_d+T_s)\Delta\delta=\Delta T \qquad (1)$$

where

M = generator rotor inertia, per unit = $4\pi fH$
h = oscillation frequency, per unit
$\Delta\delta$ = rotor angular oscillation
ΔT = applied torque required to produce the oscillation $\Delta\delta$

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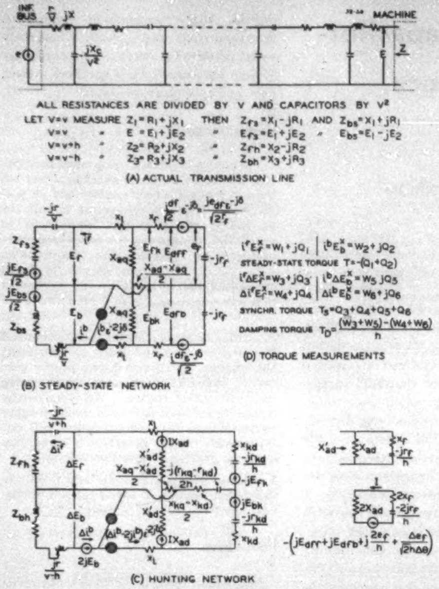

INF. BUS
MACHINE
ALL RESISTANCES ARE DIVIDED BY V AND CAPACITORS BY V²
LET V=v MEASURE Z₁=R₁+jX₁ THEN Z_fs=X₁−jR₁ AND Z_bs=X₁+jR₁
V=v " E=E₁+jE₂ " E_fs=E₁+jE₂ " E_bs=E₁−jE₂
V=v+h " Z₂=R₂+jX₂ " Z_fh=X₂−jR₂
V=v−h " Z₃=R₃+jX₃ " Z_bh=X₃+jR₃
STEADY-STATE TORQUE T=−(Q₁+Q₂)
SYNCHR. TORQUE T_s=Q₃+Q₄+Q₅+Q₆
DAMPING TORQUE T_D=((W₃+W₅)−(W₄+W₆))/h

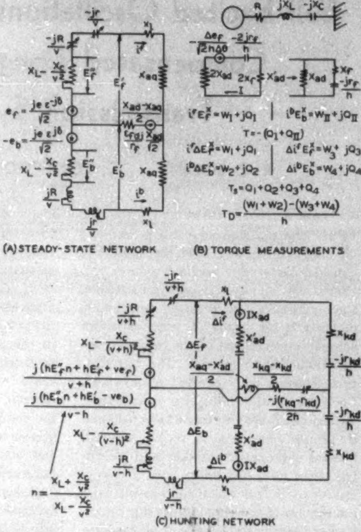
(A) ACTUAL TRANSMISSION LINE

(B) STEADY-STATE NETWORK

(C) HUNTING NETWORK

(D) TORQUE MEASUREMENTS

Figure 2. Equivalent circuits for transmission line connected to salient-pole synchronous machine



(A) STEADY-STATE NETWORK

(B) TORQUE MEASUREMENTS

(C) HUNTING NETWORK

Figure 3. Equivalent circuits for salient-pole synchronous machine connected to infinite bus through *R-L-C* tie

At the boundary between stable and unstable oscillations the total rotor torque must be zero ($\Delta T=0$). Equation 1 then shows that T_d must be zero, and T_s must equal h^2M (or M must equal T_s/h^2). Thus at each point where T_d crosses zero, a limiting value of M may be found. For low values of h and positive T_s it has previously been shown that the system is unstable for negative values of T_d. This provides the information required to distinguish between the stable and unstable ranges of rotor inertia. The magnitude of the negative damping torque coefficient T_d also provides a measure of the degree of instability, just as the magnitude of the positive damping torque coefficient is a measure of the amount of negative damping which can be tolerated in the system. In the region of high oscillation frequency no such simple and foolproof criterion of stability can be found.

From the previous studies (references 4, 6, 7, 13), it is known that electrical self-excitation may occur even aside from rotor oscillations, that is, even with infinite rotor inertia. In this case the electrical-network effective impedance becomes zero[14] at the self-excited oscillation frequency, and the torque developed becomes infinite. If, however, the system will not self-excite except with a finite rotor inertia M, the limiting value of M may be found as described above. With any reasonable value of M the torques in the region of self-excitation are extremely great, and the transition from stable to unstable operation can thus easily be detected.

The equivalent circuits required are shown in detail in references 11, 12, and 15. Figure 2 shows the circuits as applied to the present case. Two points are to be noted in connection with the procedure used. First, the equivalent circuit for the transmission line is set up separately and its characteristic impedances and terminal voltages determined as indicated in Figure 2a over the desired range. These values are then used in the equivalent circuit for the synchronous machine, Figures 2b and 2c. Second, since the machine equivalent circuit as developed in references 12 and 15 can be so adjusted as to contain both positive and negative resistances but no capacitance, all impedance values are multiplied by a factor $-j$, resulting in the circuit shown in Figures 2b and 2c, which contains inductance, capacitance, and positive resistance over a good part of the range. The torque formulas undergo a similar transformation, as may be seen by comparing Figure 2d with the formulas of reference 12. For large values of the oscillation frequency h (that is, for $h>(1-\sqrt{x_c/x_l})$ approximately, where x_l is the total line, transformer and machine armature leakage reactance), the impedance Z_{bh} of Figure 2c has a negative-resistance component X_3. These cases were handled by a method of superposition, by first measuring the circuit impedance viewed from the Δi^b mesh and then making the necessary additional calculations by hand.

Results

Figures 4–7 show some of the results obtained.

Figure 4 gives damping and synchronizing torques for the system with line capacitance neglected. Here there is no possibility of purely electrical self-excitation, but it is seen that for oscillation frequencies in the range $0.5<h<1.0$ the damping torque coefficient is negative, so that hunting will occur for rotor inertias which give natural frequencies in this range. From the formula $M=T_s/h^2$, applied at the points of zero damping, the

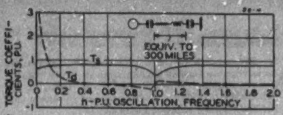
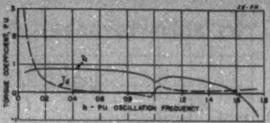
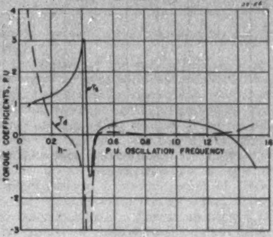


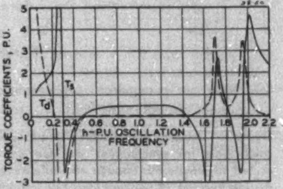
Figure 4. Damping and synchronizing torque coefficients for synchronous machine connected to infinite bus through series resistance and reactance—at no load and with amortisseur



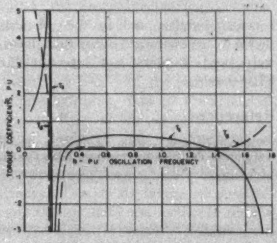
(a). No compensation, $x_c=0$ (see Figure 5e)



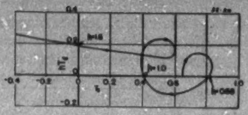
(b). With series-capacitor compensation, $x_c=40$ per cent (see Figure 5f)



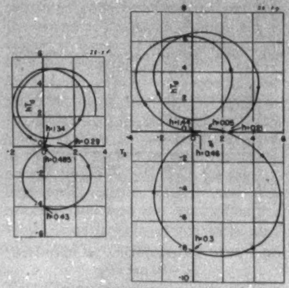
(c). With series-capacitor compensation, $x_c=60$ per cent (see Figure 5g)



(d). With series-capacitor compensation, $x_c=80$ per cent (see Figure 5h)

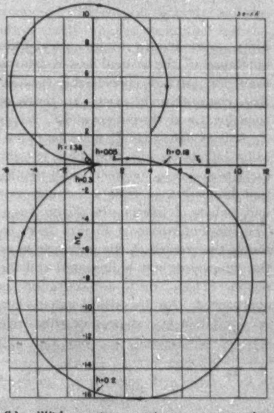


(e). No compensation, $x_c=0$ (see Figure 5a)



(f) (left). With series-capacitor compensation, $x_c=40$ per cent (see Figure 5b)

(g) (right). With series-capacitor compensation, $x_c=60$ per cent (see Figure 5c)



(h). With series-capacitor compensation, $x_c=80$ per cent (see Figure 5d)

Figure 5. Damping and synchronizing torque coefficients for synchronous machine connected to infinite bus through 300-mile transmission line—at no load and with amortisseur

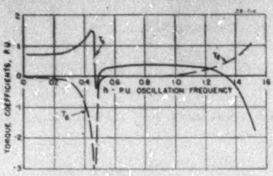
range of rotor inertias for which the system is unstable is $0.43<M<3.24$, or in terms of the conventional inertia constant $H(M=4\pi fH)$, $0.00057<H<0.0043$. These values are extremely small and entirely outside of any practical range, so the conclusion is that the system is stable. This would be expected for the machine considered, since it has an amortisseur.

Figure 5 shows the effect of varying the amount of series-capacitor compensation for the same system as in Figure 4 but now considering the transmission-line capacitance. The line capacitance does not by itself affect the range of negative damping very much, as may be seen by comparing Figure 5a with Figure 4a. Figures 5b to 5d show that one effect of series-capacitor compensation is to extend the lower limit of rotor inertia for which the system is unstable to zero, as seen by the fact that T_s becomes zero while T_d is still negative. However, the upper limit is still extremely small; in the most severe case of full compensation of line and transformer reactance ($x_c=0.8$, Figures 5d and 5h) the greatest rotor inertia for which the system will be unstable is $M = T_s/h^2 = 5.05/(0.19)^2 = 140$, or $H = 0.186$.

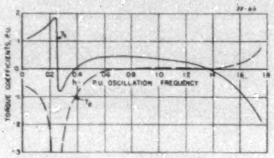
The fact that T_s becomes negative and that resonance points in both T_d and T_s occur at high oscillation frequencies does not mean that the system is unstable. The curves of hT_d versus T_s given in Figures 5e to 5h are useful in this respect as they exhibit a remarkable regularity, forming more or less circular curves in the resonance regions and thus serving as a good indication of the magnitude of the torque coefficients and the probable form of the T_d and T_s versus h curves in these regions.

Figure 6 is similar to Figure 5 but for a generator with no amortisseur. The damping now becomes negative at a much lower oscillation frequency, corresponding to a rotor inertia approaching practical values; Figure 7 shows the effect of load angle on the system of Figure 6. Even with 75 per cent load on the generator the system is still unstable at rather low oscillation frequencies.

In all of the cases considered above the armature and line resistance are so high that electrical self-excitation does not occur, in spite of the fact that a rather low ($r/x=10$ per cent) line resistance was assumed. Previously[12] it had been concluded that the line resistance below which self-excitation could exist with infinite rotor inertia and zero shunt line capacitance was well below the actual probable value. The results presented above lead to this same conclusion for an actual line and considering rotor inertia. As a quantitative check of the effect of rotor inertia the case of Figure 2, reference 12, has been set up and studied by the equivalent-circuit method, using the circuit of Figure 3. The results obtained show that the introduction of a



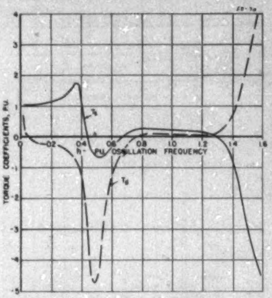
(a). With series-capacitor compensation, $x_c = 40$ per cent



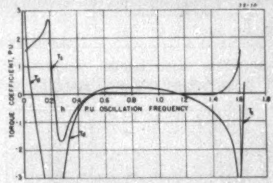
(b). With series-capacitor compensation, $x_c = 80$ per cent

Figure 6. Damping and synchronizing torque coefficients for synchronous machine connected to infinite bus through 300-mile transmission line—at no load and without amortisseur

finite rotor inertia makes very little difference in the critical line resistance even for inertias considerably smaller than could actually exist. In terms of equation 1 and the curves of hT_d versus T_s, it is evident that T_s must be practically infinite at the boundary of self-excitation except for very small oscillation frequency h, since reasonable per unit values of inertia are always large. The series of curves of Figures 5e to 5h shows the growth of the lower circle as the compensation x_c is increased and the region of electrical self-excitation is approached. At the critical value when self-excitation is just possible this circle becomes infinite, and in the unstable region it curves upward rather than downward and so does not intersect the $hT_d = 0$ axis.



(a). With series-capacitor compensation, $x_c = 40$ per cent



(b). With series-capacitor compensation, $x_c = 80$ per cent

Figure 7. Damping and synchronizing torque coefficients for synchronous machine connected to infinite bus through 300-mile transmission line—at 75 per cent load and without amortisseur

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Dielectric-Recovery Characteristics of Large Air Gaps

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Synopsis: In contrast to power arcs in air, for which the dielectric-recovery voltage decreases with increasing arc current, arcs in air produced by surge currents of several hundred microseconds' duration possess dielectric-recovery characteristics that vary only slightly with current magnitude and wave shape. Even when the discharge current is prolonged at low magnitude for as long as a thousand microseconds, the rate of dielectric recovery is little affected. For this condition at times when current as high as one ampere is flowing, the transient voltage required to convert to a high current arc is quite high. The mechanism for such a discharge is different than for the breakdown of virgin air, and the initial arc drop is sufficiently high to record its transient character. It is about ten times the steady-state value at the time the crest current is reached and drops about 50 per cent in the first 20 microseconds.

Photographic studies show that even at short-time intervals successive discharges do not follow exactly the path blazed by the initial discharge but occur in an air column of low dielectric strength expanding radially with time. It is indicated that the high arc temperatures diffusing in this region can be the principal factor determining the rate of recovery after the first 1,000 microseconds.

A comparison is made with the mechanism of natural lightning particularly from recent data obtained on the low-magnitude portions of stroke currents between successive components. Such records show that current does not always flow continuously between multiple-stroke components for strokes to objects of normal height. The maximum interval of recorded zero current is about 23,000 microseconds. The rates of recovery for spark gaps indicate that about 40,000 microseconds is the upper limit for which the dielectric strength of the stroke channel will be sufficiently low to insure successive breakdown in the same general region.

The character of a lightning discharge initiating a power follow does not affect the rate of recovery after the power arc has been extinguished. Circuit reclosing times should be based primarily on the probability of the duration of multiple strokes and the probability of restriking for power arcs.

Correlation of the rate of dielectric recovery of air gaps with rates of system recovery show that an appreciable number of transmission-line lightning flashovers can occur at such a time relative to the normal system voltage cycle that only a very short-duration fault is produced. The number is about 10 to 15 per cent for a typical system having a natural period of 1,000 cycles which corresponds to a line about 40 miles in length. This is about half of the flashovers which normally do not cause outages. The rest are due to cases where power arcs longer than one cycle are produced but extinguish before a circuit-breaker operation.

THE rates at which large air gaps recover dielectric strength after discharging power-frequency currents and short-duration surge currents comparable to those produced by lightning are of fundamental importance in determining power-system performance. Some of the more important considerations are the factors governing the probability of lightning flashovers producing power follow, the probability of power follow producing an outage, and the minimum safe reclosing time after a circuit interruption. Because of the similarity between long arcs and the mechanism of natural lightning, data on the electrical characteristics of the arc path after an initial discharge and the character of subsequent discharges should also give information on the mechanism of multiple strokes. Despite its fundamental value, very meager data are available on the rates of dielectric recovery of large gaps, especially for initial discharges having the character of lightning currents. Most studies have been confined to power arcs in very small gaps where arc conditions are quite different.

For the study of surge current discharges in large air gaps a method of synchronizing two generators has been developed so that surge current discharges can be produced in a gap and controlled surge voltages applied after specified time intervals. Data have thus been obtained for 6- and 11-inch rod gaps on the voltage necessary to produce a second discharge as a function of the time interval after the first. Eleven inches was the maximum spacing which could be tested with the facilities available. With negative polarity throughout, a wide range of initial current magnitudes and wave shapes was studied, the subsequently applied voltage having the standard $1^1/_2$ x 40 wave shape. The same terminology (critical voltage), as used for the minimum breakdown in virgin air, is applied to the minimum breakdown after an initial discharge. This should lead to no confusion as long as the time interval after the initial discharge is specified. By raising the voltage above the critical value, volt-time curves were also obtained in the same manner as those for breakdown in virgin air. Through the use of a high-speed rotating camera and still cameras, data were also obtained on the visual characteristics of the initial and subsequent discharges.

Test Circuit

A schematic diagram of the surge generator circuits is shown in Figure 1. The initial discharge through the test gap is produced by the current generator and the subsequently applied potential by the voltage generator. The two generator trip circuits are synchronized by a rotating switch. As shown in Figure 1, the tripping cycle is started by shorting out gap *A*. When the arm of the rotating switch next passes through gap *B* the tripping circuit for the current generator is discharged. When the arm passes through gap *C*, the tripping circuit for the voltage generator is discharged, providing gap *B* is still passing current, which is assured by making the time constant of the first circuit long enough for the desired range of time intervals. In this way the two generators can be discharged only in the proper sequence. The time interval is controlled by the spacing between gaps *B* and *C* and the speed of the rotating arm driven by a variable-speed motor.

During the discharging of the current generator, the voltage generator must be isolated from the test circuit. The gap *D* of Figure 1 must have a higher breakdown value than the test gap, so that the voltage generator will not be prematurely discharged. The larger gap requires a higher discharge voltage than is to be applied to the test gap, and thus, as shown in the figure, only a portion of the generator voltage is tapped from the potentiometer circuit. If the impedance of the current generator is high enough that it does not distort the voltage of the voltage generator discharging through it in parallel with the test gap, the gap *E*

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may flash over on the second discharge. However, as most high current generators are of low impedance, gap *E* must have a higher breakdown value than the test gap during the second discharge. If such a large gap spacing will not permit the initial discharge of the current generator, a fiber tube and fuse wire can be used to permit the current generator to discharge before isolating it for the voltage generator discharge.

Dielectric-Recovery Voltage for Initial Discharges of Short Duration

The wave shapes of the initial discharge currents studied can be conveniently classified into two groups. The first group had durations less than 100 microseconds and were short compared to the time interval between the initial discharge and the application of voltage. The second had durations of the same order of magnitude as the time intervals, and at the lower range of intervals current was still flowing when voltage was applied. The shortest time interval between the beginning of the current discharge and the application of voltage that could be consistently reproduced with the rotating gap was about 500 microseconds. The initial discharges of short duration will be considered first. In Figure 2 are given the wave shapes of the currents which were used for the two gap lengths studied. As shown by Figure 2a, the currents for the six-inch gap spacing involved crest magnitudes ranging from 1,390 amperes to 23,000 amperes and charges ranging from 0.002 to 0.238 coulomb. Wave shapes of two general types were used—one in which the high current lasted only a fraction of a microsecond, and the other in which the high current was maintained for five or ten microseconds. This was done to determine the effect of the two factors—magnitude and duration.

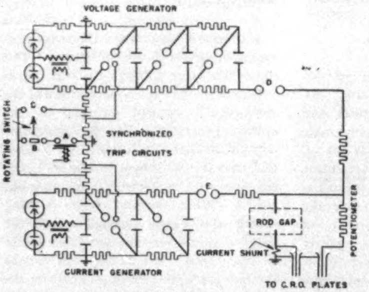


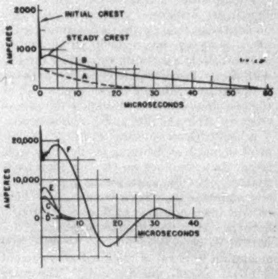
Figure 1. Schematic diagram of surge generators and synchronizing trip circuit

The discharges with only instantaneously high currents were produced with a high-voltage generator of low capacity and a high series resistance in the discharge circuit which limited the current flowing from the generator itself to a relatively low value. However, as previous work has shown, the discharge mechanism for large air gaps is quite similar to that of natural lightning.[1-3] The initial current flowing at the electrodes at the breakdown of an air gap is produced by streamers propagating from one or both electrodes and distributing charge along the channel which they blaze. When these two leaders meet, intense "return streamers" are formed which propagate back to the gap electrodes and rapidly discharge the charge in the gap. For applied voltages similar to the standard $1^1/_2$ x 40 wave, the whole process takes place in less than a microsecond. The high peak in the measured current occurs when the return streamers reach the gap electrodes, and the current which flows for the next fraction of a microsecond depends upon the charge in the air gap. After this initial charge is dissipated, the current is determined by the surge-generator circuit. Thus, for surge generators of low capacity and high discharge resistance, the current rapidly drops to a much lower magnitude designated in Figure 2 as the steady crest magnitude. The current waves *A*, *B*, *D*, and *H* were produced in this way. A high current generator of much greater capacity and lower discharge resistance was used to produce current waves *C*, *E*, *F*, and *I*, for which the sustained current magnitude is approximately the same as the initial crest.

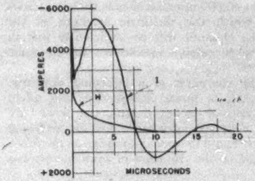
In Figures 3 and 4 are given data obtained on the critical voltage as a function of time interval after the initial discharge, and in Figure 5 are typical volt-time curves. For each time interval these data were obtained in the same manner as normally used for volt-time curves except that no correction was made for atmospheric conditions since the significance of this factor is not known. However, all data were taken under substantially uniform conditions, the relative air density varying between 0.994 and 1.01 and the absolute humidity between 0.152 and 0.426 inch of mercury vapor pressure.

Consistency of Breakdown

Caused principally by variations in ionization conditions, the breakdown of an air gap is not a single fixed value. There is a certain probability which determines the critical breakdown. If virgin air has less ionization than for a given period after an initial discharge, it would be thought that the variation of individual values from the critical would be appreciably less. Although no attempt was made to determine probability curves of breakdown, some information is contained in the data obtained for volt-time curves. For several cases the voltage was first set considerably below the break-



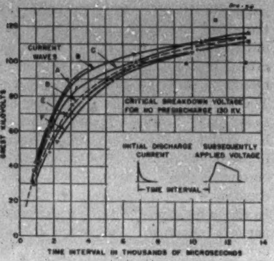
(a). 6-inch rod gap



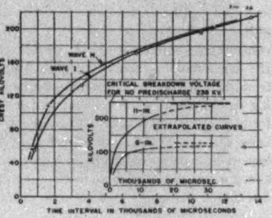
(b). 11-inch rod gap

Figure 2. Wave shapes of initial current discharges of short duration. Negative polarity

Wave	Initial Crest Amperes	Steady Crest Amperes	Charge in Coulombs
A	1,390	532	0.005
B	1,710	850	0.002
C	4,780	5,690	0.024
D	5,560	2,000	0.008
E	6,600	7,720	0.038
F	23,000	19,100	0.238
H	4,530	1,400	0.006
I	4,900	5,680	0.034



(a). 6-inch rod gap



(b). 11-inch rod gap

Figure 3. Critical breakdown voltage as function of time after initial discharges of Figure 2

Second applied voltage negative 1½ x 40 wave

down value, then raised in about two per cent steps, taking in some instances five and in others ten shots at each voltage. This provided data on the percentage variation, either in the minimum voltage that would produce breakdown, or the maximum that would not, from the mean or critical breakdown. These data showed, for time intervals below 1,000 microseconds, a maximum variation of about 20 per cent from the critical breakdown. Above 1,000 microseconds the maximum variation was about 13 per cent, and no trend was observed with time interval. However, in only four cases out of 30 was it over ten per cent and in only 13 over five per cent. Some of this variation might be due to irregularities in time interval as controlled by the rotating switch. However, for a given setting, the time interval did not vary more than about ± 25 microseconds which even at the lower range of time intervals would not, as shown by Figure 3, produce a variation in critical breakdown voltage of more than about two per cent. It is interesting that for intervals above 1,000 microseconds the spread of breakdown voltage for individual shots is no greater than observed when obtaining normal volt-time curves for virgin

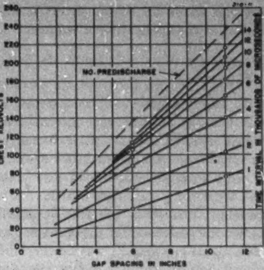


Figure 4. Critical breakdown for various time intervals as function of gap spacing

Wave C for 6-inch gap and wave *I* for 11-inch gap

air. This indicates that little ionization is left in the gap.

Probable Accuracy

The consistency with which the critical breakdown could be duplicated was about five per cent, and this is thought to be a conservative estimate of the probable accuracy of the curves of Figure 3. The accuracy of three points on Figure 3a for wave *D* at time intervals above 9,000 microseconds are considered questionable, because difficulty was experienced with synchronization of the trip circuits when they were obtained.

Effect of Initial Discharge Current

The curves of Figure 3 show that the critical breakdown voltage varies in a somewhat exponential manner with the time interval, increasing much more rapidly during the first 2,000 or 3,000 microseconds than thereafter. However, the maximum variation of the curves for the six-inch rod gap is remarkably small, despite the wide range of initial discharge currents. The maximum deviation from a mean occurs at the lower range of time intervals but is less than 20 per cent. After 5,000 microseconds it is less than ten per cent. Considering a mean curve for the six-inch gap, recovery to 50 per cent of the initial voltage strength occurs in about 2,500 microseconds and to 87 per cent in 13,000 microseconds. Since the deviation of the curves from one another is of the same order of magnitude as the accuracy of the data, it is difficult to determine the effect of current wave shape. As shown in Figures 2a and 3a, the higher the initial crest or the steady crest current, the lower the recovery voltage, except for the curves for waves *C* and *D*. Current magnitude appears to have more effect than charge, since some of the waves of higher initial current have much lower charge.

The manner in which the critical breakdown voltage for various time intervals increases with gap spacing is shown in Figure 4, where 6-inch and 11-inch rod-gap data for as nearly the same initial discharge current as could be reproduced are plotted. The data indicate that for any time interval the critical breakdown is approximately proportional to gap spacing. The volt-time curves of Figure 5 which are typical of those obtained show little relative flattening out with decreasing time interval. It might be expected that as the ionization remaining in the gap increases with decreasing time interval, the volt-time curves would become markedly flatter. Below about 2,000 microseconds they flatten out in regard to absolute differences in voltage but not in relative voltage change. Above

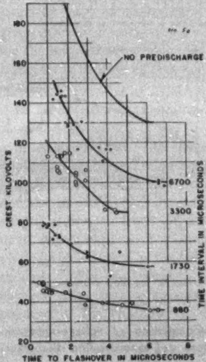
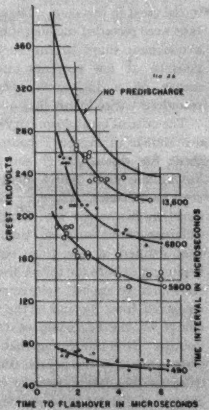


Figure 5. Volt-time curves for second voltage as applied at various time intervals

(a) (left). 6-inch rod gap

(b) (right). 11-inch rod gap



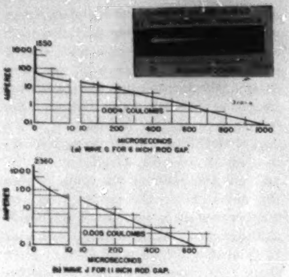


Figure 6. Wave shapes of initial discharges of long duration

3,000 microseconds the curves are approximately the same shape simply displaced in magnitude. This is further indication that little ionization is present at these time intervals.

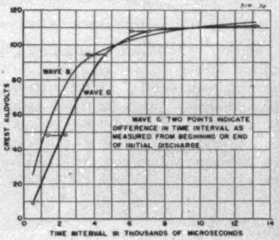
Recovery Voltages for Initial Discharges of Long Duration

The current waves of Figure 6 were produced with the surge generator of low capacity and series resistance consisting of a linear resistance in series with a number of arrester blocks. This provided a relatively low resistance at high currents but a very high resistance at low currents, thus tending to prolong the tail of the discharge. For the six-inch gap the current duration above the lower limit of recording sensitivity of 0.1 ampere is 1,000 microseconds. For the 11-inch gap it was only possible to prolong it for 600 microseconds, because more of the charge was needed in the gap at the instant of breakdown. This is shown by the higher initial crest in this case. The low current tails were recorded oscillographically with a nonlinear shunt having very high impedance at low currents and also by means of a high-speed photographic recorder.[4] (See Figure 6a.)

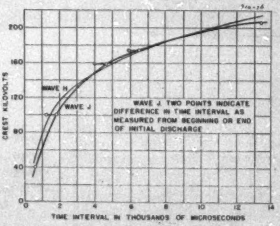
The critical breakdown curves obtained are shown in Figure 7 compared with those for discharges of short duration having the most nearly identical initial crest magnitudes. The data for the long-duration discharges are plotted for two different definitions of time interval; that defined as the time between the beginning of both the initial discharge and subsequently applied voltage and that as measured from the end of the initial discharge to the beginning of the applied voltage. It is interesting that when this shift is made in the points, they lie very close to the curves for the initial discharges of short duration except at time intervals where the initial current is still flowing at the application of voltage.

Breakdown When Current Is Still Flowing

As the time interval was lowered, it was found that although current continued to flow from the initial discharge, an appreciable voltage was still necessary in order to produce a second discharge. The breakdown was more erratic, but a definite critical value could be obtained. At 500 microseconds for the six-inch gap when about one ampere was flowing, the critical voltage was about five kilovolts and, for the 11-inch gap with about 0.4 ampere, 40 kv. The nature of the breakdown under these conditions is also interesting. Oscillograms of the two discharge currents and the second voltage are shown in Figures 8 and 9. During the period of breakdown the decrease in voltage is very gradual requiring several microseconds to drop to a relatively low voltage characterized by a fairly stable arc drop, but during the whole recorded period of voltage, it continues to decrease at a lesser rate. As higher voltages are applied, the period characterized by the more rapid drop in voltage becomes shorter until at twice the critical value it is practically instantaneous. The arc drop for the following period is also much lower, too low to record. As shown by the oscillograms of Figures 9b and 9c, the current in the second discharge rises slowly in the initial period corresponding to the slower rate of drop of voltage and increases more rapidly as the voltage drops more rapidly. The voltage and current relations are shown more clearly in Figure 10a which is a replot of Figures 9b and 9c superimposed. The character of the discharge current is quite different under these conditions than for normal breakdown. In the latter case, the path is first made partially conducting by initial leaders before the breakdown is completed by the return streamer process, which results in a high instantaneous flow of current and practically instantaneous drop in voltage. In this case, however, where considerable ionization exists because of the small flow of current, the transition from the low-conducting to high-conducting conditions is more gradual and uniform. The initial crest current characterizing the final breakdown of the gap is less pronounced and of longer duration. Comparison of the current in the second discharges of Figures 8 and 9 shows that



(a). 6-inch rod gap



(b). 11-inch rod gap

Figure 7. Comparison of voltage-recovery curves for short- and long-duration discharges

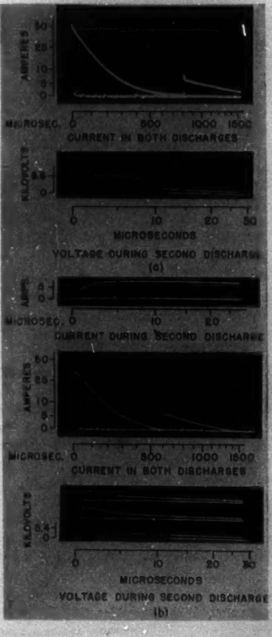


Figure 8. Current and voltage oscillograms when initial discharge is of long duration

6-inch rod gap. Wave G of Figure 6a

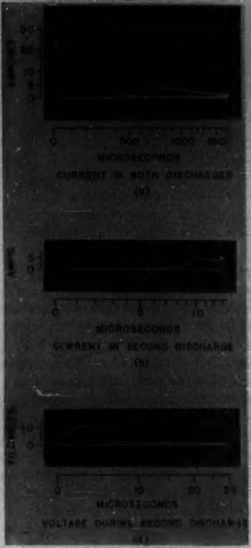


Figure 9. Current and voltage oscillograms when initial discharge is of long duration

11-inch rod gap. Wave *J* of Figure 6b

this initial crest increases with the time interval between discharges. The gradual transition of the arc path may have been influenced by the character of the voltage surge-generator circuit. The series resistance had to be fairly high, since only a small fraction of the potentiometer could be tapped off in producing the low discharge voltages. This could have produced appreciable regulation.

A measure of the effective impedance of the arc during the various stages can be obtained by determining the "effective" resistance from the current and voltage oscillograms. Such data are shown in Figure 10b. Synchronized simultaneous records were not obtained for each discharge; however, as shown by Figure 8b, the results could be duplicated with a good degree of consistency. It was found that for the initial discharges of Figure 6 the steady crest magnitude of the current in the second discharge was about five amperes for both gap spacings and essentially constant for a variation of time interval of as great as 300 microseconds. The arc resistance is, of course, quite high before the start of the second discharge, decreasing to about 1,800 ohms per foot for the 11-inch gap and 1,200 ohms per foot for the six-inch gap at the peak of the discharge current. Then it decreases more gradually. At the end of about 20 microseconds it is reduced by about 50 per cent. It is of interest to compare the arc under these transient conditions with that of stable arcs. After the arc has been established at the crest of the discharge current, the arc drop is about 9,000 volts per foot for the 11-inch gap and 6,000 volts per foot for the 6-inch gap. Although it is decreasing rapidly, it has not at the end of 20 microseconds dropped to anywhere near the normal value of 400 or 500 volts per foot, which is typical of steady-state conditions for arc currents of this magnitude.[1]

Photographic Study of the Arc Path

Photographic studies were made of the arc path by means of the high speed rotating camera and two still cameras mounted so as to photograph the arc from directions 90 degrees apart. Typical records are shown in Figure 11. About 250 microseconds is the shortest time interval for which a record could be obtained because the arc luminosity was too low to photograph for the very small currents resulting in the second discharge. As shown in Figure 11a, the path of the second discharge is not identical with that of the first for times as short as 250 microseconds. It diverges more and more as this time interval is increased. Up to about 2,000 microseconds the second discharge maintains the major bends of the first, but after about 5,000 or 6,000 microseconds it has departed entirely from the characteristics of the original path. Figures 11a and 11b show the second arc path to be more tortuous than the first. As shown by the sets of two still photographs, the second discharge follows a more or less winding path in a given cylindrical volume of air surrounding the original path, the radius of which increases with time as this volume of air with lower dielectric strength expands.

In Figure 12 an attempt was made to show the divergence of the two paths as a function of time. On this curve are plotted points representing the average of at least five records of the maximum three dimensional divergence of any part of the second arch path from the first. These data were obtained from simultaneous photographs taken with the two still cameras. For the short-duration discharges, divergence is more or less independent of the character of the initial discharge or of the gap spacing, and all the points lie close to a single curve for time intervals up to about 7,000 microseconds. After this, they diverge widely. For the long-duration discharges the divergence, when plotted as a function of time interval after the end of the first discharge, also conforms closely to the same mean curve except for the point at lowest time interval, for which current is still flowing. Then, although the arc path is still conducting current, the subsequent path blazed by the second discharge is not identical with that of the first which, as photographed, is probably the arc path formed during the initial high current portion of the discharge. During the longer period in which the lower current magnitude exists, the conducting core is expanding radially, and the current density is probably greatly decreased. This may contribute to some extent to the appreciable critical voltages when current is still flowing.

Mechanism of Recovery

The curve of Figure 12 gives a measure of the rate at which the conditions of low dielectric strength set up by the discharge in the arc path are expanding radially. The two principal conditions which may be contributing to the lower dielectric strength of this expanding region are high temperature and ionization. Both effects will propagate radially into successively larger volumes of air. As this heat and ion diffusion takes place, together with ion neutralization by recombination, the dielectric strength of the air should increase. Since both of these conditions are so interconnected, it has always been difficult to separate them for study. Browne,[6] however, has obtained data indicating that the ionization effect cannot be appreciable after the first 100 microseconds for short a-c arcs between

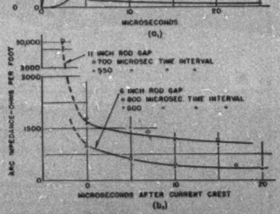
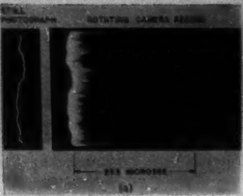
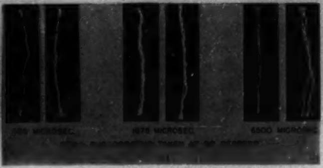


Figure 10. Voltage and current characteristic of discharges produced before or shortly after end of initial discharge





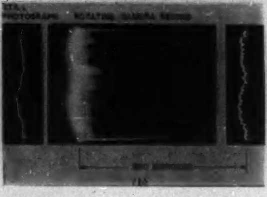


Figure 11. Multiple discharges showing relation between the two arc paths

11-inch rod gap

(a), (b). Wave *I* of Figure 2b
(c). Wave *H* of Figure 2b

parallel brass electrodes. Although this condition is quite different than for long arcs where electrode effects are much less, the consistency of breakdown and the character of the volt-time curves lead one to suspect that the temperature effect is also predominant for long arcs and short surges, at least for time intervals above 1,000 microseconds.

It is realized that turbulence and other factors insufficiently known by the authors make an analytical study of the problem difficult. However, some very approximate calculations have been made of temperature variation with time. Curve *A* of Figure 13 is a mean curve for the rate of voltage recovery of the six-inch rod gap. Curve *B* is a plot of temperature variation with time determined from curve *A* on the assumption of constant pressure throughout the air column and thus, that the dielectric strength of the air is inversely proportional to temperature. The starting point for the calculation was the critical breakdown voltage at normal room temperature. It is interesting that such a curve gives temperatures at the lower range of time intervals that correspond to those normal for arcs.

Any attempt at a rigorous calculation of rate of ion or temperature diffusion could not account for turbulence and would always give the highest ion density or temperature at the original arc core. Thus, successive discharges should always follow the same path blazed by the first. Such calculations for heat diffusion, assuming cylindrical symmetry and constant gas density, specific heat, and thermal conductivity, indicate rates of temperature drop of the same order of magnitude as given by curve *B* of Figure 13, but somewhat more rapid. It is probably the extreme condition of turbulence which produces localized regions of low dielectric strength of a more or less random distribution throughout a certain region whose radius is given by Figure 12.

Because of this, it was thought that perhaps the only analysis which was warranted was merely the very crude one based on the assumption that the heat produced in the original arc column resides at any time interval in a cylindrical column of air given by the radius of Figure 12 for that time interval and that the temperature in the column is uniform. Curve *C* of Figure 13 was calculated on this basis, starting with a temperature of 4,000 degrees Kelvin and a radius of 0.13 inch at a time interval of 250 microseconds. It conforms quite closely to curve *B*. This is not necessarily confirmation of the accuracy of either curve, but it is believed to indicate that the temperature of the air column alone can account for the rate of voltage recovery.

Mechanism of Natural Lightning

Photographs of natural lightning taken with the Boys camera or similar devices imparting the time element to the record indicate that all components of a multiple stroke follow the same path except in rare cases when a component discharge will follow the original path part way to earth, then depart entirely from it. A record of a typical multiple stroke obtained in the Westinghouse investigation is shown in Figure 14. However, deviation of a fraction of an inch or even several inches cannot be distinguished on such records and probably do exist.

The rates of voltage recovery which have been obtained indicate that the time intervals between components can be high enough (ranging from 0.0005 to 0.5 second[9]) that, unless low current flows for an appreciable portion of the intervening period, the subsequent discharge should not always follow the same path. From evidence provided by strokes to very tall objects, Bruce and Golde[10] have concluded that current must flow continuously between components. However, four records have been obtained with the photographic surge-current recorder[4] of multiple strokes to low objects. Two of these records are shown in Figure 15. This device is capable of measuring currents as low as 0.1 ampere, and for all of the records there is an appreciable period between at least two components in which

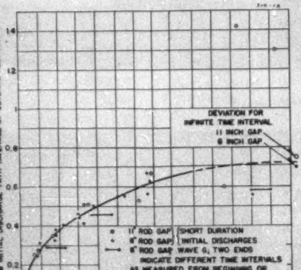


Figure 12 (left). Curve showing variation of second arc from initial arc path as function of time interval between discharges

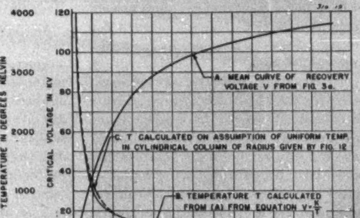


Figure 13 (right). Calculated curves of variation of arc-path temperature with time after initial discharge

Figure 14. Record of multiple stroke to open ground

no current was measured. It is interesting that none of these periods has exceeded about 23,000 microseconds. The extrapolated voltage-recovery curves of Figure 3b indicate that, for stroke currents of the general character studied, 30,000 to 40,000 microseconds should be about the upper time-interval limit for zero current between successive components. Otherwise, the dielectric strength of the original stroke channel will have sufficiently recovered that the subsequent discharge might take a totally different path. However, some of the strokes which have been measured[9] have continuing currents of such high magnitude and duration that their rate of recovery may be appreciably lower.

Records of strokes, such as Figure 14, show extreme fluctuations of luminosity as a characteristic condition in the stroke channel. The long-duration stroke component in Figure 15 shows a similar wide range of fluctuation of current which is probably associated with such luminosity variations.

Comparison With Rate of Dielectric Recovery for Power Arcs

The rapid rate of recovery and small dependence on current magnitude for surges of short duration are in considerable variance with results that have been found for power arcs. Rates of recovery for 60-cycle arcs have been determined only for very short gaps (one to ten millimeters) where electrode effects and field considerations are quite different.[6,7] These data for currents of the order of five to ten amperes show rates of recovery as rapid as those for short-duration surges. However, as the power current is increased, the rate of recovery drops rapidly. For currents above 100 amperes such data indicate that an appreciable fraction of a second is required for recovery to anywhere near the original strength of the gap.

Evidence that this is also the case for long arcs is provided by tests of Griscom and Torok[9] in which the probability of restriking was determined for gap spacings of the order of 10 to 100 inches. Arcs with currents ranging from 800 to 1,500 amperes were started with fuse wire and allowed to continue for a period of about nine cycles, which should have been sufficient to eliminate the effect of the vaporized wire. The arc was interrupted and voltage reapplied after various time intervals to determine the probability of restriking. The voltages were those of transmission lines with conductor spacings equal to the gap spacings and were thus about 25 per cent of the normal gap breakdown voltage. Nevertheless, the probability of restriking was found to be appreciable for periods as long as five or six cycles.

Except for the time element introduced by multiple strokes of appreciable duration, the character of the power-follow current, since it produces such slower rates of recovery than the initiating lightning flashover, should be the principal factor governing circuit-reclosing times. Reclosing times should thus be based on the probability of the duration of multiple strokes[9] and the probability of restriking for power arcs.

Probability of Lightning Flashover Producing Power Follow

The high rate of dielectric recovery for surge-current discharges in air gaps suggests that, for lines where flashover will occur in air at atmospheric pressure, an appreciable percentage of the flashovers that do not cause an outage may be lightning flashovers that produce a disturbance of very short duration. That this is the case is shown by an analysis of system recovery voltage conditions for lightning flashover. Stroke components of sufficiently short duration can cause flashover on a certain portion of the normal conductor voltage cycles and produce a current zero occurring in a relatively short time. The current flow in the gap will be such that the rates of dielectric recovery are comparable to those of Figure 3 and lying above the system recovery volt-time curve.

There are two conditions for which such rapid arc extinction will occur. The first of these is illustrated in Figure 16a for which the lightning flashover occurs at a time (*T*) before and close to the time of normal zero system voltage. If the duration the of lightning discharge were negligible, the fundamental-frequency fault current would have, as shown in Figure 16a, an initially low magnitude followed quickly by a current zero. The magnitude and duration of this current and the magnitude of the fault recovery voltage increase with the time (*T*). The recovery voltage curves as a function of (*T*) have been determined with the a-c network calculator[11] for a typical simple system having a natural frequency of 1,000 cycles per second. This corresponds to about a 40-mile line. The system recovery voltage curves are shown in Figure 16b compared with the dielectric-recovery curves for two ratios of insulation level to normal crest system voltage, four times normal and six times normal. As shown by this figure, the maximum time *T* for which the system recovery curve lies below the dielectric-recovery curve is about 1,100 and 1,700 microseconds for the respective insulation levels. This condition can be produced by stroke currents with polarity either the same or opposite to the initial normal frequency fault current. It can thus occur on either half-cycle. If the duration of the lightning discharge does not tend to shift the normal current zero, these recovery curves can be applied to surges of appreciable duration. A calculation of the probability of flashovers not producing power follow was made on the conservative assumptions that they could be produced by single-component strokes only, having durations less than the time from the instant of flashover to the time of normal-frequency current zero. With the data available on the probability of multiple strokes and stroke-component durations[9] such an analysis indicates probabilities of flashovers not producing power follow of five and nine per cent.

Lightning flashover occurring shortly after normal system voltage zero will produce a current zero in a short time if it is the opposite polarity to the normal-frequency fault current. Thus on the assumption that strokes are predominantly negative there is a certain period on every other half-cycle when this condition can take place. Neglecting the effect of the initiating transient, the system recovery voltage curve, as a function of the time of current zero after normal system voltage zero can be estimated for this case by considering its crest to be twice the normal system voltage at the instant of current zero. It reaches crest in 500 microseconds for the system considered. Such an assumption indicates that the percentage of strokes which will not produce power follow are three and five, respectively, for the two insulation levels. This gives a total of 8 and 14 per cent of strokes causing flashover with disturbances of only short duration. The

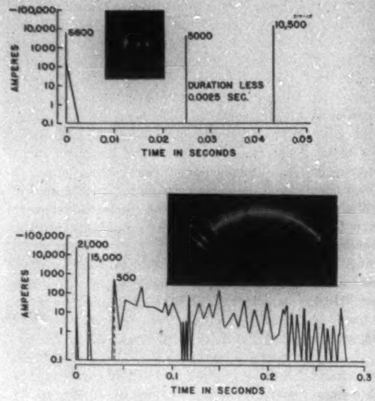
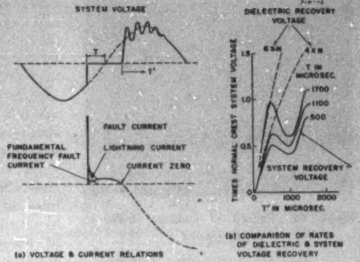


Figure 15 (left). Current in multiple lightning strokes to 85-foot fire towers as obtained with photographic surge-current recorder[4]



(a) VOLTAGE & CURRENT RELATIONS

(b) COMPARISON OF RATES OF DIELECTRIC & SYSTEM VOLTAGE RECOVERY

Figure 16 (right). Condition for lightning flashover on transmission line causing only short-duration disturbance

Dielectric-recovery curves obtained from Figure 3. $4 \times N$ represents air-gap insulation level four times normal crest system voltage

probability of rapid arc extinction will increase with the ratio of the insulation level of the line to the line voltage and with the length of the line or reduction in its natural frequency.

Data[12] have been obtained showing the percentage of system disturbances whose duration is one cycle or less. These data show an average of about eight per cent of such disturbances for steel tower lines and about 20 per cent for wood-pole lines. These short-duration disturbances are probably of the nature of those discussed above. Good agreement is shown for steel lines where the flashover path is confined entirely to air. The higher percentage for wood-pole lines confirms other data indicating that wood insulation has greater extinguishing power than air alone. Additional data[9] available on the ratio of flashovers to outages on steel tower lines indicate a range of from 15 to 35 per cent for the proportion of total flashovers not producing outages. Thus it is indicated that about half of the flashovers not causing a fault are these very short-duration disturbances.

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Formulas for the Calculation of the Inductance of Linear Conductors of Structural Shape

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THE mechanical and electrical advantages stemming from the use of linear conductors of structural shape* are so marked that, at present, most busses for heavy current duty are constructed of such conductors. Thus, standard designs of single and polyphase busses utilize strap conductors of full rectangular cross section, tubular conductors of hollow rectangular cross section, or ventilated conductors of open hollow rectangular cross section—these last built up of two slightly separated channels or four slightly separated angles placed flange to flange, or of multiples of four thin strap conductors. Again, limited construction of a somewhat more specialized type of bus has resulted from Arnold's[1] discovery that minimum eddy-current losses are attained in a single-phase circuit if, of the conductors commercially available, the bus is constructed of two properly chosen channels placed back to back. Finally, the use, in shielded busses, of conductors of T and I cross section is not unknown.

Yet, despite the use of these various types of conductors, explicit formulas for calculating the inductance—and thence the reactive voltage drop, often the determining factor in the design of a heavy current bus—seemingly are available for only the first two types of conductors: those of full and those of hollow rectangular cross section. Papers by Arnold,[2] Carlson,[3] Darling,[4,5] Dwight,[6,7] Gray,[8] Grover,[9] Higgins,[10–12] Levy,[13] Maxwell,[14] Orlich,[15] Rosa,[16–19] Roth,[20] Schurig,[21] Silsbee,[22] Steidinger,[23] Sumec,[24] and others[25,26] contain formulas, curves, and tables that facilitate calculation of the inductance of a single-phase or of a polyphase bus comprised of identical conductors having parallel coplanar axes and parallel-sided full rectangular cross sections (if corresponding sides of two rectangular areas are parallel, but the areas are otherwise arbitrarily located, these areas are termed "parallel-sided"). And papers by Darling,[4,5] Dwight and Wang,[27] and Higgins,[10–12] contain formulas, curves and tables for calculating the inductance of a single-phase or of a polyphase bus comprised of identical conductors having parallel coplanar axes and parallel-sided hollow rectangular cross sections. But, seemingly, there have not been published explicit formulas for calculating the inductance of a bus comprised of full or hollow parallel-sided rectangular conductors arranged other than as just described; nor of a bus comprised of one of certain currently employed arrangements of linear conductors of L, T, [, or I cross section.

Such formulas are derived in this paper, it being assumed that the conductors are nonmagnetic, are of such length that end effects are negligible, are right-cornered, and carry currents uniformly distributed over their cross sections.

Of these four conditions, the first and second commonly are satisfied in practice. The third, however, though essential to tractable analysis, is not always true in practice. For example, strap conductors of full rectangular cross section are usually right-cornered, whereas conductors of hollow rectangular cross section commonly have rounded edges; again, channels built up of strap conductors are usually right-cornered, whereas channels drawn or rolled in one piece usually have rounded edges. However, error introduced by neglecting the rounded edges is usually small; moreover, where necessary the increase in inductance can be approximated by use of semiempirical formulas derived by Dwight and Wang.[27] Finally, neglect of skin and proximity effects is also essential to tractable analysis. However, the effect of these phenomena on the magnitude of the inductance is on the safe side, for the inductance decreases as the frequency is increased; moreover, at power frequencies and normal bus spacings this reduction in inductance is practically negligible (see, for example, the discussion following reference 27 or the experimental results published in reference 2).

If these four postulates are granted, derivation of the desired formulas, though lengthy, is not particularly difficult. First, the formula for the geometric mean distance between two *arbitrarily located* parallel-sided rectangular areas is derived by direct integration (sections I and II). Next, this formula and the fundamental theorem of geometric mean distance theory are conjoined to obtain formulas for the geometric mean distance between two arbitrary configurations comprised of parallel-sided rectangular areas; whence general formulas for the inductance of conductors of such cross sec-

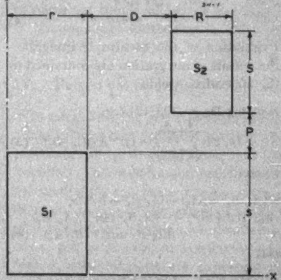

Figure 1

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* As conductors of full or annular circular cross section have been treated exhaustively elsewhere, they are not considered in this paper.

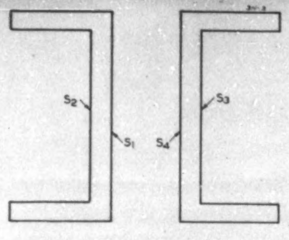

Figure 2

tion follow from application of well-known theory (sections III and IV). Finally, since the category just described embraces conductors of the type under discussion, distinct formulas for the inductance of specific types and arrangements of conductors can be deduced from these general formulas as desired (section V).

I. The Geometric Mean Distance Between Parallel-Sided Rectangles

The geometric mean distance D_{12} of a rectangular area S_1 from a second arbitrarily located rectangular area S_2 is defined by

$$S_1S_2 \log D_{12} = \int_{S_2}\int_{S_1} \log[|X-x|^2+|Y-y|^2]^{1/2} dS_1dS_2 \quad (1)$$

(x, y) being the co-ordinates of a point in S_1 and (X, Y) those of a point in S_2.

If the rectangular areas are parallel-sided, are located as in Figure 1, and if P and D are *positive* (the necessity and the significance of this restriction are discussed in section II), we have by equation 1

$$RSrs \log D_{12} = \int_{D+r}^{D+R+r}\int_{P+s}^{P+S+s}\int_0^r\int_0^s \log[|Y-y|^2+|X-x|^2]^{1/2} dydxdYdX \quad (2)$$

Evaluation of the quadruple integral—the details of integration are contained in the appendix—yields

$$RSrs \log D_{12} = -(25/12)RSrs - (1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}K(A_i, B_j) \quad (3)$$

wherein

$$K(A_i, B_j) = (A_i^4-6A_i^2B_j^2+B_j^4) \times \log(A_i^2+B_j^2)^{1/2} - 4A_iB_j^3\tan^{-1}(A_i/B_j) - 4A_i^3B_j\tan^{-1}(B_j/A_i) \quad (4)$$

and

$$A_1 = D+R+r;\ A_2 = D+R;\ A_3 = D;\ A_4 = D+r \quad (5)$$

$$B_1 = P+S+s;\ B_2 = P+S;\ B_3 = P;\ B_4 = P+s \quad (6)$$

Certain variants of equation 3 and of equation 4 are of value. Recalling that

$$\tan^{-1}(A_i/B_j) + \tan^{-1}(B_j/A_i) = \pi/2 \quad (7)$$

providing $A_i, B_j \geq 0$ we have by equations 4 and 7

$$\begin{aligned} K(A_i, B_j) &= (A_i^4-6A_i^2B_j^2+B_j^4)\times \log(A_i^2+B_j^2)^{1/2} + 4A_iB_j(A_i^2-B_j^2)\tan^{-1}(A_i/Bj) - 2\pi A_i^3B_j \\ &= (A_i^4-6A_i^2B_j^2+B_j^4)\times \log(A_i^2+B_j^2)^{1/2} + 4A_iB_j(B_j^2-A_i^2)\times \tan^{-1}(B_j/A_i) - 2\pi A_iBj^3 \end{aligned} \quad (8)$$

As by equations 5 and 6

$$2\pi A_i^3\sum_{j=1}^{4}(-1)^jB^j = 2\pi B_j^3\sum_{i=1}^{4}(-1)^iA_i = 0 \quad (9)$$

providing *both P and D are positive* as postulated above, substitution of equation 8 in equation 3 yields

$$RSrs \log D_{12} = -(25/12)RSrs - (1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}M(A_i, B_j) \quad (10)$$

wherein

$$M(A_i, B_j) = (A_i^4-6A_i^2B_j^2+B_j^4)\times \log(A_i^2+B_j^2)^{1/2} + 4A_iB_j(A_i^2-B_j^2)\times \tan^{-1}(A_i/B_j) \quad (11)$$

By virtue of the equivalent forms of the right-hand members of equation 8, and similarly those of equation 11, we obtain the useful identities

$$K(A_i, B_j) = K(B_j, A_i);\quad M(A_i, B_j) = M(B_j, A_i) - 2\pi A_iB_j(B_j^2-A_i^2) \quad (12)$$

II. Generalization of P and D

The reason for the restriction, in section I, of P and D to positive values is evidenced by the following remarks on integration in the variable Y. If P and D are positive, we have $Y > y$; whence $(Y-y) \equiv |Y-y|$ and $d(Y-y)/dY \equiv d|Y-y|/dY$ over the domain of integration in Y; and integration in Y with either $(Y-y)$ or $|Y-y|$ in the integrand yields the same value. But if P and D can, separately, take on either positive *or* negative values, then over the range of integration in Y it may be that $(Y-y)$ takes on both positive and negative values. If so, $d|Y-y|/dY = 1$ if $Y > y$; but $d|Y-y|/dY = -1$ if $Y < y$; $|Y-y| = (Y-y)$ if $Y > y$, but $|Y-y| = -(Y-y)$ if $Y < y$; and integration with $(Y-y)$ in the integrand *may* yield a value different from that obtained with $|Y-y|$ in the integrand: for example,

$$\int_0^{2a}(Y-a)dY = 0, \text{ but } \int_0^{2a}|Y-a|dY = a^2;$$

$$\text{contrariwise, } \int_0^{2a}|Y-a|^2dY = \int_0^{2a}(Y-a)^2dY = 2a^3/3$$

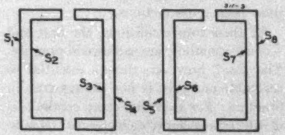

Figure 3

When with this point of analysis in mind the quadruple integral in the right-hand member of equation 2 is evaluated for each possible relative position of the two parallel-sided rectangular areas—the details of integration are discussed in the appendix—it is found that in each case equation 3 yet results *providing that in equation 3 A_i and B_j now indicate the following* (of which, obviously, equations 5 and 6 are special cases)

$$A_1 = |D+R+r|;\ A_2 = |D+R|;\ A_3 = |D|;\ A_4 = |D+r| \quad (13)$$

$$B_1 = |P+S+s|;\ B_2 = |P+S|;\ B_3 = |P|;\ B_4 = |P+s| \quad (14)$$

Further, as equation 7 is satisfied by these generalized values of A_i and B_j, equation 8 is likewise generally valid. Contrariwise, however, equations 10 and 11 are valid only if *both P and D are positive:* nevertheless, as this case frequently occurs in practice, these latter equations are most useful.

With the restriction on P and D thus removed, the foregoing equations afford the solutions to a host of electromagnetic problems (on the calculation of self-inductance and mutual inductance, reactance, line voltage drop, short-circuit bus stresses, and the like) that confront the engineer concerned with the design of single or polyphase busses composed of parallel linear conductors with axes not necessarily coplanar and with right cross sections comprised of arbitrary configurations of parallel-sided rectangles.

III. General Formulas for the Inductance and Reactance of Busses

Though solutions to problems of the type just mentioned can be obtained by direct use of the formulas derived in sections I and II, a considerable and very desirable simplification of the numerical computation inherent to the actual design of a given bus can be effected if these formulas are conjoined with certain very general expressions to be derived by repeated use of the cardinal theorem of geometric mean-distance theory. This theorem states that:

If $S_1, S_2, \ldots, S_m$ are the components of a total area S; if $D_1, D_2, \ldots, D_m$ are their respective geometric mean distances from a second area S'; and if D is the geometric mean distance between S and S', then

$$SS' \log D = S_1S' \log D_1 + S_2S' \log D_2 + \ldots + S_mS' \log D_m \qquad (15)$$

A direct corollary of this theorem is:

Let S' be comprised of component areas $S_1', S_2', \ldots, S_n'$. Then as by equation 15

$$S_1S' \log D_1 = S_1S_1' \log D_{11} + S_1S_2' \log D_{12} + \ldots + S_1S_n' \log D_{1n} \qquad (16)$$

and as $S_2S' \log D_2, \ldots, S_m S' \log D_m$ can be expressed similarly, we have on substituting equation 16 and like forms in equation 15

$$SS' \log D = \sum_{i=1}^{m}(S_iS_1' \log D_{i1} + S_iS_2' \times \log D_{i2} + \ldots + S_iS_n' \log D_{in}) = \sum_{i=1}^{m}\sum_{j=1}^{n} S_iS_j \log D_{ij} \qquad (17)$$

We note for later use that if each of the two cross sections is comprised of identical components numbered alike, $m=n$ and $S_{ij} \log D_{ij} = S_{ji} \log D_{ji}$.

Alternatively, if the components of S' are numbered from $m+1$ to $m+n$ instead of from 1 to n, we obtain from equation 17

$$SS' \log D = \sum_{i=1}^{m}\sum_{j=m+1}^{m+n} S_iS_j \log D_{ij} \qquad (18)$$

For determining the self geometric mean distance of an area to itself equation 17 is preferable; for determining the geometric mean distance between two distinct areas equation 18 is preferable.

Granted the four postulates stated above, equations 15 to 18 inclusive facilitate derivation of convenient and compact expressions for the inductance of circuits comprised of parallel linear conductors of uniform but otherwise arbitrary cross section. Thus, invoking fundamental electromagnetic principles, if S_a and S_b are the cross sections of two conductors comprising a single-phase circuit carrying a current I, we have for the vector potential $A(X, Y)$ at a point (X, Y)

$$A = \text{constant} - 2\iint_{S_a} w_a \log [|X-x|^2 + |Y-y|^2]^{1/2} dxdy - 2\iint_{S_b} w_b \times \log [|X-x|^2 + |Y-y|^2]^{1/2} dxdy \qquad (19)$$

wherein w_a and w_b are, respectively, the current densities in S_a and S_b, taken positive for current in one direction and negative for current in the other direction (hence, w_aw_b is a negative quantity, w_a^2 and w_b^2 positive quantities, and $I = w_aS_a = -w_bS_b$). The energy in the magnetic field associated with the circuit is

$$W = {}^1/_2 \iint_{S_a} A(X, Y) w_a dXdY + {}^1/_2 \iint_{S_b} A(X, Y) w_b dXdY \qquad (20)$$

Substitution of equation 19 in equation 20, and then of equation 1 in the resulting expression yields

$$W = -(2w_aw_bS_aS_b \log D_{ab} + w_a^2S_a^2 \log D_{aa} + w_b^2S_b^2 \log D_{bb}) = I^2 \log (D_{ab}^2/D_{aa}D_{bb}) \qquad (21)$$

Finally, recalling that $W = LI^2/2$, we have from equation 21

$$L = -(2/I^2)(2w_aw_bS_aS_b \log D_{ab} + w_a^2S_a^2 \times \log D_{aa} + w_b^2S_b^2 \log D_{bb}) = 2 \log D_{ab}^2/D_{aa}D_{bb} \qquad (22)$$

If each of the two conductors is comprised of several component conductors connected in parallel, m comprising that with cross section S_a and n that with cross section S_b, substitution of appropriate forms of equations 17 and 18 in equation 22 yields

$$L = -(2/I^2)\Big[\sum_{i=1}^{m}\sum_{j=m+1}^{m+n} 2w_iw_jS_iS_j \log D_{ij} + \sum_{i=1}^{m}\sum_{j=1}^{m} w_iw_jS_iS_j \log D_{ij} + \sum_{i=m+1}^{m+n}\sum_{j=m+1}^{m+n} w_iw_jS_iS_j \log D_{ij}\Big] = -(2/I^2)\sum_{i=1}^{m+n}\sum_{j=1}^{m+n} w_iw_jS_iS_j \log D_{ij} \qquad (23)$$

If we have two distinct circuits comprised of $p+q$ conductors *in toto*, p comprising one circuit and carrying current I_p, q comprising the other circuit and carrying current I_q, a similar analysis yields for the energy associated with the two circuits

$$W = -\sum_{i=1}^{p+q}\sum_{j=1}^{p+q} w_iw_jS_iS_j \log D_{ij} \qquad (24)$$

Recalling that

$$W = MI_pI_q + L_pI_p^2/2 + L_qI_q^2/2 \qquad (25)$$

substitution of equation 24 and of appropriate forms of equation 23 in equation 25 yields for the mutual inductance M

$$M = -(1/I_pI_q)\Big[\sum_{i=1}^{p+q}\sum_{j=1}^{p+q} w_iw_jS_iS_j \log D_{ij} - \sum_{i=1}^{p}\sum_{j=1}^{p} w_iw_jS_iS_j \log D_{ij} - \sum_{i=1}^{q}\sum_{j=1}^{q} w_iw_jS_iS_j \log D_{ij}\Big] \qquad (26)$$

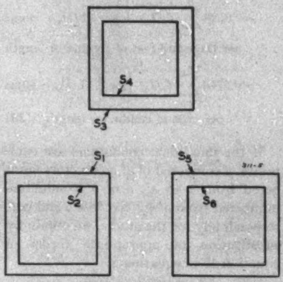

Figure 5

With regard to the physical quantities mentioned to this point, all units are in the absolute system: linear dimensions in centimeters, current I in abamperes, energy W in ergs per centimeter of line length, self inductance L and mutual inductance M in abhenrys per centimeter of line length. The 60-cycle reactance X in ohms per thousand feet of line length is obtained by multiplying a value of inductance by 11.48×10^{-8}.

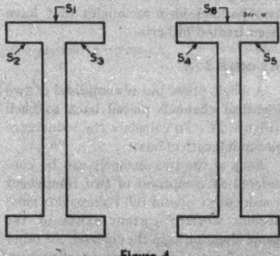

Figure 4

IV. Polyphase Systems

In calculating the performances of polyphase systems it is necessary to know the reactance voltage drops on individual conductors. The well-known formulas for calculating the reactance drops are easily derived. In an n-phase system comprised of n distinct conductors, one conductor, say a, can be considered as carrying current I_a (the currents are expressed as complex numbers), and the remaining $n-1$ conductors can be considered as connected in parallel and carrying the return current $-I_a=(I_b+I_c+\ldots+I_n)$. Accordingly, as the energy associated with conductor a is given by $W_a=\frac{1}{2}\int Aw_a dS_a$, we obtain by selection of the appropriate terms from equation 22 that the associated inductance, $L_a=$ (reactance drop on conductor a)$/(2\pi f I_a)$, is

$$L_a=-(2/I_a^2)(w_a^2S_a^2\log D_{aa}+w_aS_a\sum_{i=b}^{n}w_iS_i\log D_{ai})$$

$$=-2\sum_{i=a}^{n}(I_i/I_a)\log D_{ai}\text{ abhenrys per centimeter of conductor length}\qquad(27)$$

By equation 27 the 60-cycle reactance is

$$X_a=-240\pi\sum_{i=a}^{n}(I_i/I_a)\log D_{ai}\text{ abohms per centimeter of conductor length}$$

$$=0.0528\sum_{i=a}^{n}(I_i/I_a)\log_{10}(1/D_{ai})\text{ ohms per thousand feet of conductor length}$$

$$=0.2794\sum_{i=a}^{n}(I_i/I_a)\log_{10}(1/D_{ai})\text{ ohms per mile of conductor length}\qquad(28)$$

If the individual conductors are composite, a composed of a' conductors numbered from 1 to a', b of b' conductors numbered from $a'+1$ to $a'+b'$, and correspondingly for the others, we obtain by substitution of appropriate forms of equation 16 in equation 27

$$L_a=-(2/I_a^2)\left[\sum_{i=1}^{a'}\sum_{j=1}^{a'}w_iw_jS_iS_j\log D_{ij}+\sum_{i=1}^{a'}\sum_{j=a'+1}^{a'+b'}w_iw_jS_iS_j\log D_{ij}+\ldots+\sum_{i=1}^{a'}\sum_{j=m'+1}^{m'+n'}w_iw_jS_iS_j\log D_{ij}\right]$$

$$=-(2/I_a^2)\left[\sum_{i=1}^{a'}\sum_{j=1}^{a'+\ldots+n'}w_iw_jS_iS_j\log D_{ij}\right]\text{ abhenrys per centimeter length}\qquad(29)$$

Corresponding formulas for the reactance are obvious from equation 28.

V. Some Illustrative Examples

The various parts of example 1 are advanced as corroborative of the fundamental formula given in equation 3, wherein A_i and B_j are as defined in equations 13 and 14. The succeeding examples are advanced as demonstrative of the application of the content of sections I to IV to the design of standard busses of various types.

EXAMPLE 1

Part A. The two rectangular areas of Figure 1 are to be considered as identical and superimposed. For this case: $r=R$; $s=S$; $D=-r$; $P=-s$. Then equations 13 and 14 yield: $A_1=A_3=r$; $A_2=A_4=0$; $B_1=B_3=s$; $B_2=B_4=0$. Whence, recalling that $\lim_{x\to 0}x^4\log x=0$, substitution in equation 3 as indicated yields Maxwell's well-known formula for the geometric mean distance of a rectangular area to itself

$$\log D_{12}=\log(r^2+s^2)^{1/2}-(r^2/6s^2)\log[(r^2+s^2)/r^2]^{1/2}-(s^2/6r^2)\log[(r^2+s^2)/s^2]^{1/2}+(2s/3r)\tan^{-1}(r/s)+(2r/3s)\tan^{-1}(s/r)-25/12\qquad(30)$$

Part B. The two rectangular areas of Figure 1 are to be considered as identical squares $(R=S=r=s)$, so located that $P=D=0$. Then equations 13 and 14 yield: $A_1=B_1=2r$; $A_2=B_2=A_4=B_4=r$; $A_3=B_3=0$. Whence, substitution in equation 3, as indicated, yields the known formula due to Rosa (reference 16, page 13)

$$\log D_{12}=\log r+3\log 2-(7/12)\log 5-4\tan^{-1}(1/2)+\pi-(25/12)$$
$$=\log r+0.34427+\qquad(31)$$

Part C. In similar fashion, if we take $r\neq R$, $s\neq S$, $D=D$, $P=-(s+S)/2$, we obtain Gray's well-known formula for the geometric mean distance between two dimensionally different, symmetrically located rectangles (reference 16, page 6); if we consider two identical squares $(R=S=r=s)$, so located that $D=0$ and $P=P$, we obtain the known formula due to Rosa (reference 16, page 12); and if we take $r\neq R$, $s\neq S$, $D=-(r+R)/2$, $P=-(s+S)/2$, we obtain the writer's formula for the geometric mean distance between two symmetrically superimposed, dimensionally different rectangles (reference 11, page 1049). Accordingly, as verification of the analysis of sections I and II we have that equation 3 yields the known formulas for those special cases of the geometric mean distance between two parallel-sided rectangles that have been treated hitherto.

EXAMPLE 2

A single-phase bus is comprised of two identical channels placed back to back (Figure 2). To calculate the inductance per-unit length of bus:

Each of the two channels can be considered as comprised of two component conductors: one of full rectangular cross section wherein current exists in the same direction as in the channel, the other of smaller rectangular cross section having current in the opposite direction. Hence: $S_1=S_4$; $S_2=S_3$; $w_1=-w_2=w_3=-w_4=I/(S_1-S_2)$. Substituting in equation 23 yields

$$L=-(2/I^2)\sum_{i=1}^{4}\sum_{j=1}^{4}w_iw_jS_iS_j\log D_{ij}\text{ abhenrys per centimeter of bus length}\qquad(32)$$

Substituting in equation 32 as indicated and collecting terms, we have

$$L=-[4/(S_1-S_2)^2][S_1^2(\log D_{11}-\log D_{14})+S_2^2(\log D_{22}-\log D_{23})-2S_1S_2(\log D_{12}-\log D_{13})]\qquad(33)$$

Corroboratively, if $S_2=S_3=0$ we have conductors of full rectangular cross section, and equation 33 reduces to the known formula $L=4\log(D_{14}/D_{11})$.

The six geometric mean distances in the right-hand member of equation 33 can be calculated from equation 3. Commonly, though, the value of the self geometric mean distance of a rectangle or of the geometric mean distance between two rectangles, symmetrically and externally located relative to one another, can be obtained—and usually more quickly than by calculation—from tables contained in certain of the references, especially reference 9. For other than this restricted class, though, equation 3 must be used perforce.

EXAMPLE 3

A single-phase bus is comprised of two identical open hollow rectangular conductors, each conductor consisting of two slightly separated channels placed flange to flange (Figure 3). To calculate the inductance per-unit length of bus:

Each of the two conductors can be considered as comprised of four component conductors: two of rectangular cross section wherein current exists in the same direction as in the split conductor; the remaining two of smaller rectangular cross section having current in the opposite direction. Hence: $S_1=S_4=S_5=S_8$; $S_2=S_3=S_6=S_7$; $w_1=-w_2=-w_3=w_4=-w_5=w_6=w_7=-w_8=I/2(S_1-S_2)$. By equation 23 the inductance of the bus is

$$L=-(2/I^2)\sum_{i=1}^{8}\sum_{j=1}^{8}w_iw_jS_iS_j\log D_{ij}\text{ abhenrys per centimeter of bus length}\qquad(34)$$

Substituting in equation 34 as indicated and collecting terms yields

$$L=-[1/(S_1-S_2)^2][S^2(2\log D_{11}+2\log D_{14}-2\log D_{15}-\log D_{18}-\log D_{45})+S_2^2(2\log D_{22}+2\log D_{23}-2\log D_{26}-\log D_{27}-\log D_{36})-2S_1S_2(2\log D_{12}+2\log D_{13}-2\log D_{16}-\log D_{17}-\log D_{28})]\text{ abhenrys per centimeter of bus length}\qquad(35)$$

Example 4

A single-phase line is comprised of two identical conductors of I cross section (Figure 4). To calculate the inductance per unit of bus length:

Each of the two conductors can be considered as comprised of three component conductors: one of rectangular cross section wherein current exists in the same direction as in the conductors of I cross section, the remaining two of smaller rectangular cross section having current in the opposite direction. Hence: $S_1=S_6$; $S_2=S_3=S_4=S_5$; $w_1=-w_2=-w_3=w_4=w_5=-w_6=I/(S_1-2S_2)$. By equation 23 the inductance of the bus is

$$L=-(2/I^2)\sum_{i=1}^{6}\sum_{j=1}^{6}w_iw_jS_iS_j\log D_{ij}$$

abhenrys per centimeter of bus length (36)

Substituting in equation 36 as indicated and collecting terms yields

$$L=-[4(S_1-2S_2)^2][S_1^2(\log D_{11}-\log D_{16})+S_2^2(2\log D_{22}+2\log D_{23}-2\log D_{24}-\log D_{25}-\log D_{34})-2S_1S_2(2\log D_{12}-\log D_{14}-\log D_{15})]$$

abhenrys per centimeter of bus length (37)

Example 5

A three-phase line is comprised of three equilaterally spaced rectangular tubular conductors (Figure 5). To calculate the inductance associated with one conductor, say a:

Each rectangular tubular conductor can be considered as comprised of two component conductors: one of rectangular cross section wherein current exists in the same direction as in the tubular conductors, the other of smaller rectangular cross section having current in the opposite direction: Hence: $S_1=S_3=S_5$; $S_2=S_4=S_6$; $w_1=-w_2$; $w_3=-w_4=w_1\epsilon^{-120°}$; $w_5=-w_6=w_1\epsilon^{+120°}$. By equation 27 the associated inductance of the conductor a is

$$L_a=-(2/I_a^2)\sum_{i=1}^{2}\sum_{j=1}^{6}w_iw_jS_iS_j\log D_{ij}$$

abhenrys per centimeter of conductor length (38)

Noting that $w_1=I_a/(S_1-S_2)$, we obtain on substituting in equation 38 as indicated and collecting terms

$$L_a=-[2/(S_1-S_2)^2][S_1^2(\log D_{11}+\epsilon^{-j120°}\times\log D_{13}+\epsilon^{+j120°}\log D_{15})+S_2^2(\log D_{22}+\epsilon^{-j120°}\log D_{24}+\epsilon^{+j120°}\log D_{26})+2S_1S_2(\log D_{12}+\epsilon^{-j120°}\log D_{14}+\epsilon^{+j120°}\log D_{16})] \quad (39)$$

Corroboratively, if $S_2=S_4=S_6=0$, the conductors are solid, and equation 39 reduces to the well-known expression

$$L_a=(2/I_a)[I_a\log(1/D_{aa})+I_b\log(1/D_{ab})+I_c\log(1/D_{ac})]$$

abhenrys per centimeter of conductor length (40)

Equation 40 also follows from equation 39 for the case of tubular conductors which are so thin that the cross sections can be considered as but comprised of four line segments. A specific treatment of this particular type of conductor and several numerical examples are to be found in a paper by Dwight and Wang:[21] they consider but square cross sections.

Appendix

Explicit evaluation of the quadruple integral

$$\int_{D+R}^{D+R+r}\int_{P+S}^{P+S+s}\int_0^r\int_0^s\log[|Y-y|^2+|X-x|^2]^{1/2}dydxdYdX$$

varies according as: (*i*) both P and D are positive; (*ii*) one or both of P and D is zero or is negative. In the first case, no logarithmic singularities are encountered in the course of evaluating the integral; whence integration can be effected directly through use of known tabulated integrals. In the second case, however, logarithmic singularities are encountered. These singularities necessitate repeated use of limits; whence the evaluation is considerably complicated. Additionally, as these singularities occur in a manner that varies with the relative position of the two rectangular areas of Figure 1, the process of integration must be modified for each possible distinct relative location; that is, for each possible combination of P and D mentioned in case (*ii*). Each of these various integrations has been effected; the very lengthy, purely formal analysis incident to evaluation is contained in a complementary paper to be published elsewhere. These integrations, however, differ only in mathematical detail from that for case (*i*), which follows. As mentioned, this case $(P, D>0)$ is the simplest to evaluate: even so, the easily reproduced, elementary analysis is so extensive and space filling that it is deemed advisable to present but the end result of successive integrations.

First

$$\int_0^s\log[(Y-y)^2+(X-x)^2]^{1/2}dy=\Big[-\frac{1}{2}(Y-y)\log[(Y-y)^2+(X-x)^2]-(X-x)\tan^{-1}(Y-y)/(X-x)+(Y-y)\Big]_{y=0}^{y=s} \quad (41)$$

From equation 41 we obtain

$$\int_0^r\int_0^s\log[(Y-y)^2+(X-x)^2]^{1/2}dydx=\Big[\Big[\frac{1}{2}(Y-y)(X-x)\log[(Y-y)^2+(X-x)^2]-\frac{1}{2}[(Y-y)^2-(X-x)^2]\times\tan^{-1}(Y-y)/(X-x)+\pi(Y-y)^2/2-(Y-y)(X-x)/2+(Y-y)x\Big]_{y=0}^{y=s}\Big]_{x=0}^{x=r} \quad (42)$$

From equation 42 we obtain

$$\int_{P+s}^{P+S+s}\int_0^r\int_0^s[\log(Y-y)^2+(X-x)^2]^{1/2}dydxdY=\Big[\Big[\Big[\frac{1}{4}(Y-y)^2(X-x)-\frac{1}{12}(X-x)^3\log[(Y-y)^2+(X-x)^2]-[1/6(Y-y)^3-1/2(Y-y)(X-x)^2]\tan^{-1}(Y-y)/(X-x)+\pi(Y-y)^3/6-5(Y-y)^2(X-x)/12+(Y-y)^2x/2\Big]_{y=0}^{y=s}\Big]_{x=0}^{x=r}\Big]_{Y=P+s}^{Y=P+S+s} \quad (43)$$

From equation 43 we obtain

$$\int_{D+r}^{D+R+r}\int_{P+s}^{P+S+s}\int_0^r\int_0^s\log[(Y-y)^2+(X-x)^2]^{1/2}dydxdYdX=\Big[\Big[\Big[\Big[-\frac{1}{48}\times[(Y-y)^4-6(Y-y)^2(X-x)^2+(X-x)^4]\log[(Y-y)^2+(X-x)^2]-\frac{1}{6}[(Y-y)^3(X-x)-(Y-y)(X-x)^3]\times\tan^{-1}(Y-y)/(X-x)-13(Y-y)^2\times(X-x)^2/48+\pi X(Y-y)^3/6+(Y-y)^2Xx/2\Big]_{y=0}^{y=s}\Big]_{x=0}^{x=r}\Big]_{Y=P+s}^{Y=P+S+s}\Big]_{X=D+r}^{X=D+R+r} \quad (44)$$

Finally, substituting the limits in the order indicated and collecting terms, we obtain from equation 44 that

$$\int_{D+r}^{D+R+r}\int_{P+s}^{P+S+s}\int_0^r\int_0^s\log[(Y-y)^2+(X-x)^2]^{1/2}dydxdYdX=-(25/12)-(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}K(A_i,B_j) \quad (45)$$

wherein A_i, B_j, and $K(A_i, B_j)$ designate the quantities defined in equations 4, 5, and 6.

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Steady-State and Transient-Stability Analysis of Series Capacitors in Long Transmission Lines

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IN long-distance large-block power transmission, where is reactance compensation applicable? The complete answer to this question involves the weighing of compensated line performance against its costs, tempered with engineering judgment. This paper presents the results of an analysis made to determine basic performance data of compensated lines under steady-state and transient conditions. The data presented therefore allow the question to be partly answered and, it is believed, to a sufficient degree to permit certain general conclusions to be drawn. The system designer is thus provided with the fundamentally necessary data permitting him to proceed with an economic analysis for his particular case, where the cost factors are available. It is felt that in this field lies the value of these data. The costs of the various factors for a particular case can be weighed with the performance obtained by the same factors. The best design must necessarily be one where per cent line compensation, number of parallel circuits, generator short-circuit ratio, and number of intermediate switching stations are all used to their optimum degree for a given reliability of service. The number of factors which may be varied obviously depends upon whether compensation is to be used for increasing the power limits of a system already in operation or to be used as a factor in the design of a projected system.

The urgency of the times and the requirements the war effort is placing and will place on the transmission systems, and projected, coupled with the necessity of careful use of critical materials, emphasizes the importance and timeliness of the subject. The compensated line concept is not new, but the basic performance data presented are, and they should prove useful in helping to determine the expected gains of reactance compensation in projected and existing systems. A more complete analysis can be made for a particular system when it is necessary to determine in detail the effect of various operating conditions and arrangements peculiar to the system under consideration.

General Conclusions

The data given are specifically performance data, showing what can be accomplished technically with compensated lines. A satisfactory excursion into the economic realms is outside the scope of this paper. The availability of certain materials during the present times—as well as during normal times—obviously will have its influence in the ultimate solution of a transmission problem.

1. The first and obvious conclusion is: Reactance compensation is an effective method for increasing the transient- and steady-state-stability limits of a transmission system.

2. Existing systems, composed of machines with average characteristics and lines as short as 150–175 miles—whose characteristics are fixed, because the machines are already installed—can materially increase their stability limits with acceptable margins by reactance compensation.

3. The relatively large gains available through the use of special generator characteristics, intermediate switching stations, high-speed switching and relaying indicate their fundamental usage in *projected systems* above 200 miles in length whether or not reactance compensation is used. The economic line-length threshold of reactance compensation (not determined in this paper) will obviously vary in projected systems as the relative costs of all factors change.

4. Reactance compensation appears to be an essential requirement for a-c transmission of economic blocks of power with lines above 300 miles in length.

5. Since reactance compensation provides an effective manner of increasing the stability limits above the original design values,

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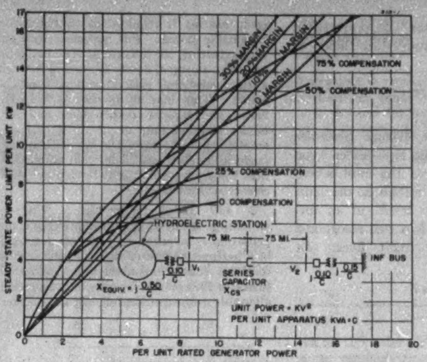


Figure 1. Steady-state stability limit of 150-mile line as a function of rating of hydroelectric generation at sending end

Transformer and low-voltage receiving-system capacitance increased in proportion to generator capacitance. Assumed generator power factor = 0.95. Held $V_1 = 1.0$
$V_2 = 0.95$

their possible use should tend to encourage a consideration of larger conductor sizes in order that the benefits from their use may be fully realized at the heavier system loadings.

6. The voltage rating of the series-capacitor equipment should be determined by—among other factors—the current during a stable-system transient oscillation, if maximum effectiveness is to be realized. This may mean a capacitor with higher than normal dielectric strength.

General Method of Analysis, Base Used, and System Studied

(*a*). The data presented in this paper, particularly those relating to transient stability, were obtained with the aid of an a-c network analyzer. The system was set up, with constants selected as described in the appendix and torque-angle curves obtained for the various conditions of fault on and off. Critical switching times were obtained by the application of the equal-area criterion.[1] Much of the steady-state work was done by well-known graphical methods.[2]

(*b*). It has been shown previously[3] that the loading of a transmission circuit varies with the square of the line voltage. For this reason it was considered convenient to make these studies on a kilovolt-ampere and kilowatt base which is given by the $(\text{kilovolts})^2$ of the circuit under consideration. Thus the data presented are entirely general and useful for any actual quantity of power and any value of voltage, except as the relation between these two quantities affects the per-unit power. The per-unit power transmitted on a $(\text{kilovolts})^2$ base is given by the ratio

$$\frac{\text{kilowatts transmitted}}{(\text{transmission kilovolts})^2}$$

[For example, 150,000 kw on a $(\text{kilovolts})^2$ base of $(220)^2$ gives a per-unit power of 3.1 and on a $(161)^2$ base gives a per-unit power of 5.0.]

(*c*). Based on a line surge impedance of 400 ohms, which is very nearly constant for all high-voltage lines, it has been shown[4] that the surge impedance or unity power-factor loading of such lines is 2.5 times the $(\text{kilovolts})^2$; hence this value of per-unit circuit loading on a $(\text{kilovolts})^2$ base is the unity power-factor loading. Since at this loading the line reactive losses are entirely supplied by the charging kilovolt-amperes of the line itself, it is generally considered a good operating loading. With reactance compensation, part of the line reactive losses are supplied by the series capacitors; hence the loading which will consume the line-charging kilovolt-amperes is increased.

(*d*). A typical hydroelectric system was selected as the basis for this study, since long lines in general involve this type of system, and it is this type that generally has the more severe stability problems. A sketch is included with each curve showing the system arrangement applying for the particular case studied. Refer to the appendix for reactances and constants chosen.

(*e*). The stability analysis in general involved the selection of a generator kilovolt-ampere rating of C times the $(\text{kilovolts})^2$. For each such rating selected, the rating of other terminal apparatus was made to match also by dividing its reactance on rating by the factor C, as discussed in Appendix II. In the steady-state-stability studies, summarized in Figures 1 to 3 inclusive, various values of system rating C were selected and the power limit for each determined. For convenience, the diagonal per cent margin line was included which intersects the power-limit curve at the per cent margin indicated. For the transient-stability studies, Figures 9 to 16 inclusive, various system ratings of C were selected and for each the critical switching time plotted, assuming the sending-end generator fully loaded before the fault.

Discussion of Results

I. Steady-State Stability, Single-Circuit Line—Figure 5

(*a*) *Results With Average Generator Characteristics*

1. *250 Miles.* Zero compensation permits a per-unit generator rating of 2.9 to transmit full load with 20 per cent margin. This value is substantiated by present practices.[5] In the future heavier loadings are technically feasible, and they may be justified by reactance compensation; for instance, 50 per cent compensation allows a rating of 5.1 with 20 per cent margin.

2. *300 Miles.* A per-unit rating of 2.5, generally referred to as the surge-impedance loading of an uncompensated line or the loading where the line supplies its own I^2x losses can be handled at this line length with:

0 compensation with 10 per cent margin
25 per cent compensation with 25 per cent margin

Thus, for this line length and this type of system, it appears that some compensation would be needed for steady-state stability for transmission of economical magnitudes of power depending upon the required margin.

3. *400 Miles.* A per-unit generator rating of only 2 can transmit full load with

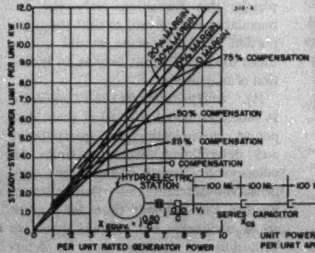


Figure 2 (left). Steady-state stability limit of 300-mile line as a function of rating of hydroelectric generation at sending end

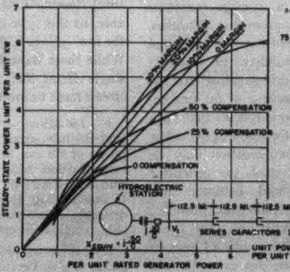


Figure 3. Steady-state stability limit of 450-mile line as a function of rating of hydroelectric generation at sending end

10 per cent margin a distance of 400 miles with no compensation. This indicates that series capacitors are undoubtedly required in lines of this length to transmit justifiable amounts of power.

(*b*) *Amount of Compensation.* There is an optimum value of compensation from a stability standpoint for a given line length. For a 300-mile line this value of compensation is about 75 per cent for the system constants chosen as shown in Figure 7. As the line length increases, this value also increases. Above this optimum value the over-all net impedance is predominantly resistance, so that voltage difference rather than phase angle tends to govern load flow. This is shown clearly in Figure 7 where the power which may be transmitted for compensation greater than optimum is larger for a line having five per cent voltage drop than for a line with flat regulation.

Table I

No. Switching Stations	Power Limits for Four Cycles Clearing—Two Lines (Per Cent Compensation)			
	0	25	50	75
0				
1				2.37
2			3.25	4.4
3		2.6	4.0	6.05
4	2.7			

(*c*) *Effect of Generator Short-Circuit Ratio.* See Figure 8. This factor was investigated for the 300-mile line operating at 20 per cent margin with the following results:

1. A compensation of 25 per cent will permit use of 1.2 short-circuit ratio in place of 2.0 with zero compensation, in transmitting output of a 2.5 per-unit generator.

2. A compensation of 50 per cent will permit use of short-circuit ratio of 1.3 in place of 2.5 with 25 per cent compensation in transmitting output of a 4.0 per-unit generator.

(*d*) *Summary, Steady-State Single-Circuit-Line Results.* It appears that, using a generator of average constants with a line length approaching 300 miles, some series compensation is required for steady-state stability to transmit more than that corresponding to the surge impedance loading of an uncompensated line, and so it follows that with loadings above this for line lengths below 300 miles, some compensation may be required, depending upon the generator characteristics. It should be noted that, while the data for steady-state stability are given for one circuit line, they apply for multiple circuit lines with all lines in on a per-circuit basis; for example, two such units studied may be operated in parallel with a steady-state limit of twice that given.

II. Transient Stability, Single-Circuit Line, Stub-Feeder Faults

(*a*). *Results With Average Generator Characteristics.* The stub-feeder type of fault was considered the most severe worth studying, since a fault on the main line would obviously result in an unavoidable loss which is a well recognized limitation of single-circuit lines. In evaluating these data, it is felt that a total clearing time of six cycles is reasonable, as this type of fault would occur in the low-voltage system at the receiver end. While these data were obtained, using a single-circuit line, they apply on a *per-circuit* basis to multicircuit lines.

Table II

	Read From Figure	At 230 Kv	At 287 Kv
Per-unit kilowatts transmitted		4.72	3.03
Per-unit kilowatts per circuit		2.36	1.52
Steady-state power limit per circuit	2		
0 compensation		2.7 (14.5 per cent margin)	2.1 (38 per cent margin)
25 per cent compensation		3.1 (31 per cent margin)	2.1*
50 per cent compensation		3.4 (46 per cent margin)	2.2*
75 per cent compensation		3.7 (57 per cent margin)	2.4* *Estimated
Fault-clearing time—cycles			
Two switching stations	15		
25 per cent compensation			2.3
50 per cent compensation		0.8	4.3
75 per cent compensation		3.55	5.2
Three switching stations	16		
25 per cent compensation		0 (nearly)	3.7
50 per cent compensation		2.65	5.0
75 per cent compensation		5.3	6.0

1. *150 Miles.* It is found in the study that the full output of a per-unit generator rating of 2.8 can be transmitted with zero compensation, while a rating of 4.5 can be transmitted with 25 per cent compensation. If line loading above approximately 3 is desirable, then series capacitors will reasonably permit such operation in this area.

2. *300 Miles—Figure 10.* It is interesting to note for this case that 50 per cent compensation is required to transmit any reasonable power. For instance, with six cycles clearing, the full output of a maximum generator rating of 3 may be carried through a stub feeder fault with 50 per cent compensation. This loading is not particularly heavy, and so more compensation in a particular instance may be justified. For example, it is technically possible to transmit (eight kilovolts)^2 and 75 per cent compensation, but such loading will be tempered of course by economics.

3. *450 Miles—Figure 11.* With a switching time of six cycles, 66 per cent compensation is required to transmit full load of a per-unit generator rating of 2.5. This indicates that compensation is needed to justify a line of this length. Further, the allowable compensation falls within a rather narrow range, something between 65 per cent and 80 per cent (Figures 7 and 11).

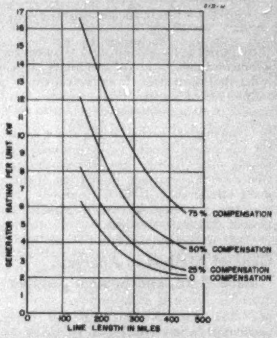


Figure 4. Data from Figures 1, 2, and 3 showing power limit in terms of generator rating with zero margin for various line lengths

Unit power = (kilovolts)^2

The stub-feeder faults as shown in Figure 11 indicate that the transient limit for small switching time is higher than the steady-state limit. These two boundaries define the results obtaining during the first swing and after an infinite number of swings, respectively. Because of the labor involved in determining completely this transition, only the two boundaries were given, so that the upper portion of these curves should not be taken too literally, but only as an indication of magnitude.

(*b*). *Interpretation for Other Conditions.* It should be noted that all of these data permit cross-plotting for interpolating and determining switching times for values of line length and per cent compensation different than those chosen. Further, the switching times for a given set of conditions, but utilizing machines with different inertias, may be deter-

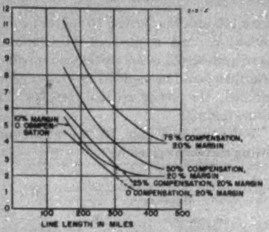


Figure 5. Data from Figures 1, 2, and 3 showing power limit in terms of generator rating with 10 per cent and 20 per cent margin for various line lengths

Unit power = (kilovolts)^2

mined by increasing the time by the factor $\sqrt{H/3}$, H being the new constant and 3 the value used in obtaining the data.

III. Steady-State Stability, Two Parallel Lines—Figure 12

For two-line circuits the worst fault through which the system may ride is a three-phase high-voltage fault at the sending end, resulting in loss of a line section. In view of this it is desirable to investigate the steady-state stability of the system with one line section out.

The multiplicity of the variables involved limited the scope of the investigation in general to a line length of 300 miles. This value was chosen since lengths in this area are being considered for future power transmission, and this, as well as previous studies, indicates that such lengths fall within a region where series capacitors should be given consideration.

The data summarized in Figure 12 show clearly how switching stations may be traded for compensation. It is interesting to note that 25 per cent compensation in the center of the line utilizing one switching station is equivalent to a large number of switching stations for steady-state stability with average generator constants. However, it must be remembered that the number of switching stations is dictated primarily by the transient-stability requirements.

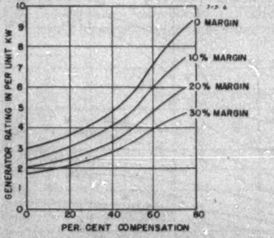
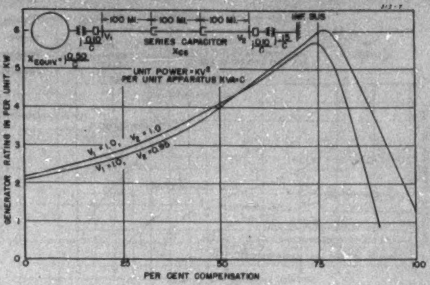


Figure 6. Data from Figure 2 showing effect of margin on required compensation, for a 300-mile line

Unit power = (kilovolts)^2. Steady-state power limit = generator rating (1 + margin)

Figure 7. Effect of line voltage level on steady-state limit

Power limit in terms of generator rating with 20 per cent margin, for 300-mile line



IV. Transient Stability, Two Parallel Lines

In the analyses, data were obtained enabling the comparative effects of switching stations, per cent compensation, and clearing times to be studied. In evaluating the data, a total clearing time of four cycles will be used.

(a) Results With Average Generator Characteristics

1. *150 Miles—Figure 13.* Zero compensation allows a per-unit generator rating of 2.7 with no intermediate switching stations. As can be seen from Figure 13, probably more than the economic value of power can be transmitted with no compensation by the proper choice of switching stations. Economics obviously would determine whether switching stations, special generator characteristics, or compensation should be used to accomplish a given result.

2. *300 Miles.* The data given in Figures 14 through 17 for two parallel lines with 0–4 switching stations for various degrees of compensation are summarized in Table I for a clearing time of four cycles. Here the comparative effects of compensation and switching stations are readily seen. For example, 25 per cent compensation with three switching stations permits nearly the same per-unit generator rating as four switching stations with zero compensation.

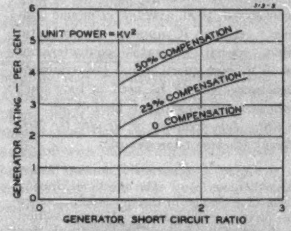


Figure 8. Effect of generator short-circuit ratio on steady-state limit

Power limit in terms of generator rating with 20 per cent margin, for 300-mile line. Assumed $x_{d(equiv)} = 0.7$/short-circuit ratio

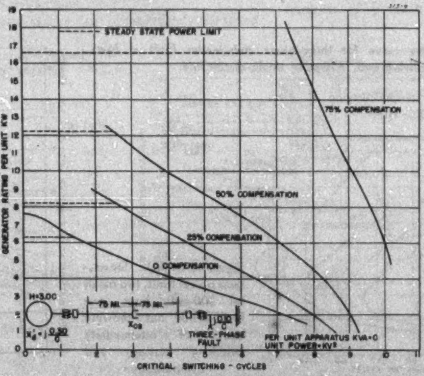


Figure 9. Switching-time curve for three-phase stub-feeder fault at low-voltage receiving end, 150-mile single-circuit line

Hydroelectric generating station carrying full rated load before fault

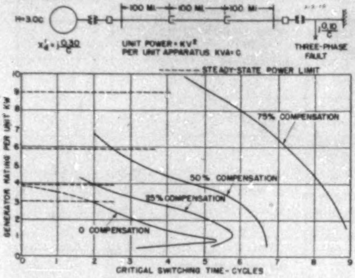


Figure 10. Switching-time curve for three-phase stub-feeder fault at low-voltage receiving end, 300-mile single-circuit line

Hydroelectric generating station carrying full rated load before fault

Similarly, two switching stations with 75 per cent compensation gives more margin than three switching stations with 50 per cent compensation.

Stability cannot be maintained for four cycle clearing time for the full output of any generator rating with the combination of conditions identified by the vacancies in Table I.

These loadings are well under the unity power-factor loadings, which are usually considered good average economic values. Hence, it appears fundamental that special generator characteristics such as high inertias and low transient reactances should be used in long lines whether compensation is used or not.

(*b*). *Interpretation of Results for Different Machine Characteristics*

1. *Effect of Generator Transient Reactance—Figure 18.* The effects on clearing time of a change in the machine reactance for several combinations of compensation and switching stations for a 300-mile line were studied. The results given in Figure 18 indicate that with a clearing time of four cycles, the full output of a per-unit generator rating of 2.5 can be transmitted with a 20 per cent transient reactance, two switching stations, and no compensation, as compared with a 30 per cent transient reactance, one switching station, and 50 per cent compensation.

2. *Effect of Generator Inertia.* Switching times given by these data may again be readily modified to apply to machines having inertia constants different from that used in the study, since they vary as the $\sqrt{H}$.

Refer to Figure 15 to see the effect of changing both H and x_d' in a particular instance.

V. PROTECTIVE EQUIPMENT AND CAPACITOR VOLTAGE RATINGS

In addition to normal load current through series capacitors, short-circuit currents and system swing currents after fault clearing must be considered.

Protective equipment will be required in general for short-circuiting the capacitors during the fault period. After the fault is removed the magnitude of the swing current may be such that it might be necessary to short-circuit the capacitors. Reference to Figure 19, which shows times normal swing currents which were measured during the single circuit—stub-feeder fault study, indicates that for a 150-mile line having 75 per cent compensation and a per-unit kilovolt-ampere loading (C) of 4.0 (which is probably less than could be justified for this type of circuit), the swing current reaches 2.7 times normal.

Referring to Figure 19, the maximum swing current is given by

$$I_S'=\frac{E_1-E_2}{\frac{x_d'+x_t+x_t+x_S}{C}+x_l}=\frac{E_0}{\frac{x_a}{C}+x_l}$$

where

x_a = total apparatus reactance on rating
x_l = net line reactance
C = per-unit apparatus kilovolt-amperes

Since the per-unit normal capacitor current is C, the times normal swing current is

$$I_S=\frac{I_S'}{C}=\frac{E_0}{x_a+x_lC}$$

This shows that, as the kilovolt-amperes transmitted (C) increases, the maximum swing current decreases for a given net line reactance. Also, as the line reactance decreases holding C constant, the swing current increases, as would be expected.

For the system selected, the maximum swing current will not exceed 2.7 times normal because

(*a*). This value is obtained with a low value of C for the shortest line with the most compensation resulting in the lowest x_l.

(*b*). For greater values of C and x_l, the swing current will be less as shown above and by the curves, Figure 19.

These same per-unit values hold for multicircuit lines with all circuits in. For the parallel line case with one line section

Figure 11. Switching-time curve for three-phase stub-feeder fault at low-voltage receiving end, 450-mile single-circuit line

Hydroelectric generating station carrying full rated load before fault

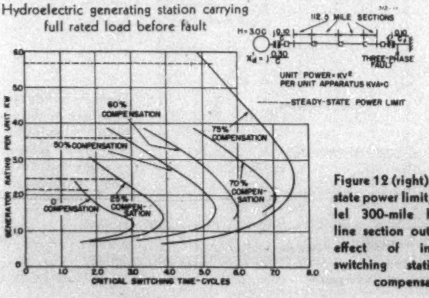
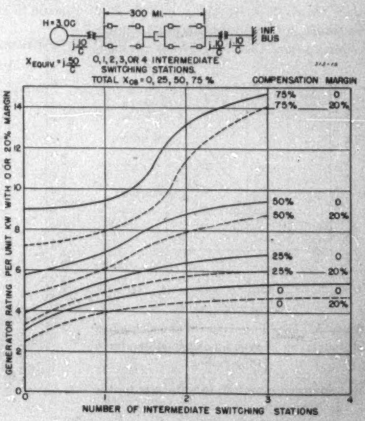


Figure 12 (right). Steady-state power limit, two parallel 300-mile lines, one line section out, showing effect of intermediate switching stations and compensation



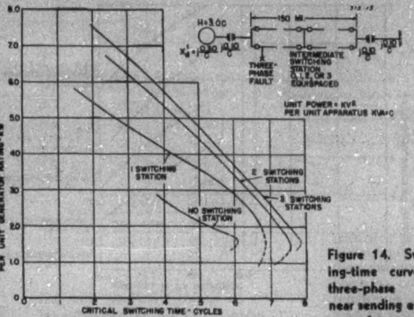


Figure 13. Switching-time curve for three-phase fault near sending end of one of two parallel 150-mile lines, for 0, 1, 2, and 3 intermediate switching stations

Fault results in tripping out of faulted line section. Hydroelectric generating station carrying full rated load before fault

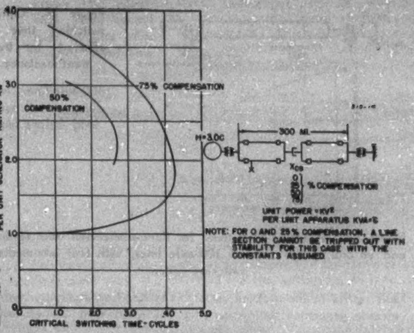


Figure 14. Switching-time curve for three-phase fault near sending end on one of two parallel 300-mile lines, with one intermediate switching station

Fault results in the tripping out of the faulted line section. Hydroelectric generating station carrying full rated load before fault

tripped out as a result of a fault clearing, the swing currents will be less than shown in Figure 19, because [illegible] is increased by virtue of the removed line section. The capacitor unit voltage rating should thus be determined by these current swings if maximum effectiveness is to be realized.

For certain fault locations the currents may be at times of such magnitude as to require the action of protective equipment. After the fault is removed, the capacitors should be placed back in service immediately, if maximum effect in maintaining stability is to be realized.

The effect of short-circuiting the capacitors instantaneously and then restoring them two cycles after the faulted line section is switched out is shown in Figure 15. This calculation involved the use of the equal area criterion as previously outlined.[4] The curves indicate that on this basis the most severe fault requiring the switching out of the capacitor is a little more severe than a three-phase fault on the sending end where the capacitors can be left in, or by-passed and immediately reinserted after the fault is removed with the same effect. It is thus shown that the effect of a two-cycle time delay in reinserting the capacitors is of such low magnitude that it could be neglected for the general study.

Machine Hunting and Abnormal Voltages

It is well known that hydroelectric generators not equipped with amortisseur windings connected to long lines experience hunting during light-load periods. It is also known that series capacitors magnify and encourage such hunting. A good analysis and bibliography of this phenomenon is given in reference 6. In this analysis it is shown that machines equipped with suitable amortisseur windings will not experience hunting when transmitting power over lines up to 300 miles (the extent of the study) in length and fully compensated. In view of these findings, and since a maximum of 75 per cent compensation is useful for stability reasons, it is felt that properly designed damper winding will prevent this difficulty.

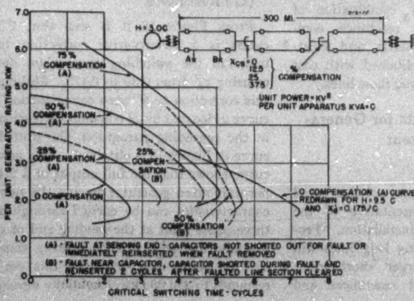
Reference 7 of the paper presents an analysis of possible abnormal voltages

Figure 15. Switching-time curve for three-phase fault near sending end on one of two parallel 300-mile lines, with two intermediate switching stations

Fault results in the tripping out of the faulted line section. Hydroelectric generating station carrying full rated load before fault

A—Fault at sending end—capacitors not short-circuited out for fault or immediately reinserted when fault removed

B—Fault near capacitor—capacitor short-circuited during fault and reinserted two cycles after faulted line section cleared



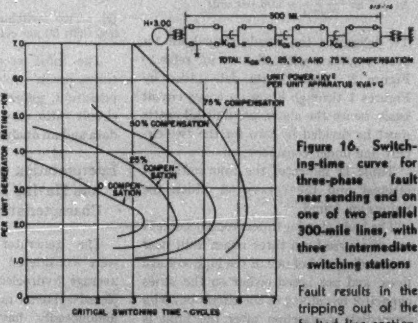


Figure 16. Switching-time curve for three-phase fault near sending end on one of two parallel 300-mile lines, with three intermediate switching stations

Fault results in the tripping out of the faulted line section. Hydroelectric generating station carrying full rated load before fault

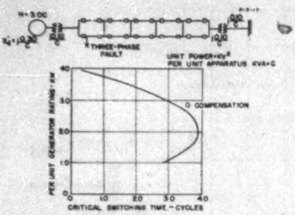


Figure 17. Switching-time curve for three-phase fault near sending end on one of two parallel 300-mile lines, with four intermediate switching stations

Fault results in the tripping out of the faulted line section. Hydroelectric generating station carrying full rated load before fault

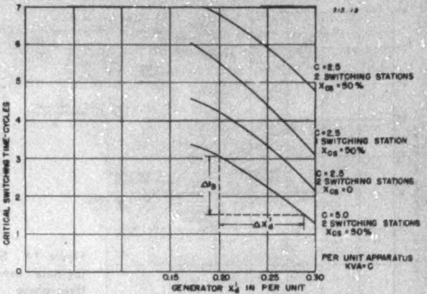


Figure 18. Critical switching time as affected by transient reactance

300-mile line. Machine at full rated load before fault

occurring in long lines in general, and in lines having series capacitors in particular when an unloaded or lightly loaded transformer is left connected on the receiving end. These overvoltages become possibilities when low-side switching is used. Since high-side switching is generally necessary for stability, this should not present a serious problem. Attention is directed to this phenomenon so that it will receive just consideration in a particular instance.

Illustration of the Use of the Curves

Assume that it is desired to transmit the output of a 250,000-kw hydroelectric station a distance of 300 miles, over a two-circuit line, into a typical receiving system such as used in this study.

Consider two possible transmission voltages, 230 kv and 287 kv. The generator kilowatts are therefore

$$\text{At 230 kv } \frac{250{,}000}{(230)^2} = 4.72 \text{ per unit}$$

$$\text{At 287 kv } \frac{250{,}000}{(287)^2} = 3.03 \text{ per unit}$$

To determine the steady-state limit for such a system with all lines in, refer to Figure 2. Steady-state data given by Figures 1 through 8 are on a per circuit basis, hence the above per-unit kilowatts must be divided by two for the two-circuit line under consideration.

Table II indicates the information of interest and the curves on which the values are found.

The fault-clearing times given in Table II are of course for a three-phase fault near the sending end of one of the high-voltage lines, and are based either on the series capacitor remaining in the circuit or on its immediate reinsertion after the fault is removed. The *effect of increasing the* generator inertia may be determined by multiplying these times by $\sqrt{\text{per-unit generator } H/3.0}$. An estimate of the additional clearing time obtainable by the use of a lower generator transient reactance may be obtained from Figure 18, taking the Δt_s for a given $\Delta x_d'$, or conversely, finding the $\Delta x_d'$ required for a needed Δt_s.

It is seen that if four-cycle total clearing time is used, and the lines are designed for 230 kv, transient stability may be maintained, for example with:

(*a*). Two switching stations and 75 per cent compensation, and a per-unit generator H of 3.8 rather than 3.0, (or an x_d' of 0.28 instead of 0.30).

(*b*). Three switching stations, 50 per cent compensation, per unit $H=6.85$ (or an x_d' of 0.23).

(*c*). Three switching stations, 75 per cent compensation.

If the lines are 287 kv, transient stability through a three-phase high-voltage sending-end fault may be maintained with

(*a*). Two switching stations, 25 per cent compensation, per unit $H=9.1$ (or an x_d' of 0.20).

(*b*). Two switching stations and a little less than 50 per cent compensation.

The most economic combination of transmission voltage, reactance compensation, generator inertia, and short-circuit ratio may be obtained with cost data and an analysis along these lines.

Interpretation of Data for Generators Having Different Characteristics

The generator characteristics used were considered representative of the average hydroelectric installation. Projected systems involving long lines will undoubtedly have larger short-circuit ratios, lower transient reactances, and larger inertia constants than the average: hence a word on the interpretation of results for such cases will be given.

Consider a 300-mile two-circuit uncompensated line with two intermediate switching stations, with machine constants as follows:

X_d'	H constant
0.175	9.5—Boulder Dam Units[5]
0.30	3 —Used in this study

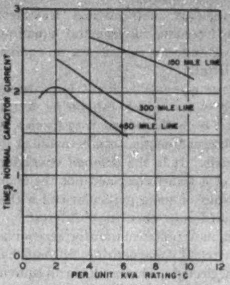
These are the constants of the 82,500-kw generators at Boulder Dam, and they represent a large variation from the average taken for this study.

(*a*) Correction for Inertia (*H*) Constant

The critical switching time for a given set of system conditions varies as the $\sqrt{H}$, and so for an H of 9.3, the critical switching time should be increased by $\sqrt{9.3/3} = 1.78$. Referring to Figure 15, for the case in question, the switching times of the curve for zero compensation should all be multiplied by 1.78 for a given generator rating.

(*b*). Correction for Transient (x_d') Reactance

From Figure 18 it is seen that 2.5 cycles is a conservative correction to make in the switching time when decreasing x_d' from 0.30 to 0.175. Making this correction to a "zero compensation" curve as located by H constant correction in the preceding paragraph, the dotted curve in Figure 18 is obtained. This new curve shows that the full output of a 2.4 per-unit rated generator having the new characteristics can be carried through a three-phase fault at the sending end of a 300-mile line with a total clearing time of four cycles. At 287.5 kv this corresponds to 280,000 kw, essentially agree-



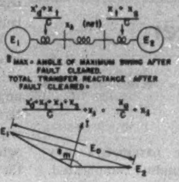

$\frac{x_d'+x_1}{C}$
x_3 (net)
$\frac{x_1+x_2}{C}$
E_1
E_2
δ_{MAX} = ANGLE OF MAXIMUM SWING AFTER FAULT CLEARED.
TOTAL TRANSFER REACTANCE AFTER FAULT CLEARED =
$\frac{x_d'+x_1+x_1+x_2}{C}+x_3=\frac{x_0}{C}+x_4$
E_1
I
E_0
δ_m
E_2

Figure 19. Maximum swing current with stability after clearing stub-feeder fault

Line 75 per cent compensated

ing with published calculated performance data for the 287.5-kv Boulder Dam–Los Angeles lines.[8]

Appendix I. Description of System Studied

A hydroelectric station, whose per-unit-kilovolt-ampere rating, called a factor C (see Appendix II), was varied, supplies power to an infinite bus through:

1. A step-up transformer bank of 0.10 per-unit reactance.
2. A transmission circuit of typical 60-cycle constants.
3. A step-down transformer bank of 0.10 per-unit reactance.
4. A receiving system having a steady-state per-unit reactance of 0.15 and a transient reactance of 0.10.

Constants selected for the generator in per unit on its rating were

$$x_{\text{equiv}}=0.50=\frac{0.70}{\text{short-circuit ratio}}$$

$$x_d'=0.30$$

This corresponds to a short-circuit ratio of 1.4, which is representative of the average machine. The effects of both lower and higher short-circuit ratios were investigated. A generator power-factor rating of 0.95 was used as a nominal value for this type of application. A per-unit generator inertia or H constant of 3.0 was used, which is typical for a machine of normal short-circuit ratio. Since the critical switching times vary directly with $\sqrt{H}$, the effect of other values may be readily evaluated. The line constants selected as typical and nearly constant regardless of voltage for high-voltage 60-cycle lines, were

$r+jx=0.12+j0.80$ ohms per mile

$g+jb=0+j5.2\times10^{-6}$ mhos per mile

The relationship between actual ohms and per-unit ohms on a given kilovolt-ampere base for a particular kilovolt is:

$$\text{per-unit ohms}=\frac{\text{actual ohms}\times\text{kilovolt-ampere base}}{(\text{kilovolts})^2\ 1{,}000}$$

Hence on the kilovolt-ampere base = (kilovolts)^2, used throughout this study

$$\text{per-unit ohms}=\frac{\text{actual ohms}}{1{,}000}$$

and therefore the line constants on the base selected varied only with line lengths. This was another convenience resulting from the use of this base.

Appendix II. A Note on the Factor C

A generator having a per-unit kilovolt-ampere rating of C as used in this study has a kilowatt rating of 0.95 C (kilovolts)^2, because a 0.95 power-factor machine was assumed throughout. For an equivalent reactance x_{equiv} of 0.50 on the kilovolt-ampere rating of C (kilovolts)^2 the reactance on the (kilovolts)^2 base is $0.50/C$. This follows from the relationship that

$$x_{\text{equiv}}\ (\text{on given base})=x_{\text{equiv}}\ (\text{on rating})\times\frac{\text{given base}}{\text{rating}}$$

Hence

$$x_{\text{equiv}}\ (\text{kv})^2\ \text{base}=x_{\text{equiv}}\ (\text{on rating})\ \frac{\text{kv}^2}{C(\text{kv})^2}=\frac{x_{\text{equiv}}\ (\text{on rating})}{C}$$

The same relation holds for the sending end and receiving end transformer and receiving system reactances, as indicated on the sketch accompanying each figure.

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A Useful Equivalent Circuit for a Five-Winding Transformer

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Synopsis: It has been indicated that there is a growing interest in the possibilities of multiwinding transformers with five windings. The lack of a suitable equivalent circuit for this type of transformer has retarded some of these engineering analyses. This paper presents a practical equivalent circuit, together with a development of the necessary formulas. Numerous mathematical manipulations have been omitted in the interest of brevity. Several of these manipulations are the key to the simplicity of the final working formulas. Examples show how the various equivalent-circuit parameters are evaluated. Factors affecting the voltage regulation of a five-winding transformer are brought out with the aid of an example in which two sources of supply are present. An objective of the analysis has been to keep the equivalent circuit suitable for the calculating board. Several precautions in this connection are discussed.

THE demand for electric power in greater quantities and with optimum continuity has increased the transmission engineers' interest in interconnected circuits and the multicircuit transformers associated therewith. Considerable has been published on three- and four-winding transformers.[1] This article enlarges the field to include a much more complicated unit, the five-winding transformer.

It should be emphasized that multicircuit transformers present many problems which cannot be solved by two-winding methods. These complications relate to leakage-impedance phenomena associated with the many fluxes illustrated in Figure 1. Among the more important characteristics affected by these phenomena are

(*a*). Voltage regulation.

(*b*). Load division between circuits.

(*c*). Parallel operation with other transformers.

(*d*). Efficiency.

(*e*). Short-circuit characteristics.

The interlinkage of the various leakage fluxes due to load currents in the different circuits affect the various voltages in complicated and sometimes unexpected ways. It is therefore necessary to have a clear conception of the leakage impedance relationships; otherwise the manipulation of the parameters may prove perplexing and unduly complicated.

Theory

The general theory of a five-winding transformer is no different from that with a lesser number of windings. Such a transformer consists of five separate windings that are mutually coupled by a single magnetic core, illustrated best as in Figure 1. This transformer can be analyzed in the conventional manner common to multicircuit transformers of a lesser number of windings, wherein the basic differential equations of voltage can be expressed as follows:

$$e_1=r_1i_1+L_1\frac{di_1}{dt}+M_{12}\frac{di_2}{dt}+M_{13}\frac{di_3}{dt}+M_{14}\frac{di_4}{dt}+M_{15}\frac{di_5}{dt}$$

$$e_2=r_2i_2+L_2\frac{di_2}{dt}+M_{21}\frac{di_1}{dt}+M_{23}\frac{di_3}{dt}+M_{24}\frac{di_4}{dt}+M_{25}\frac{di_5}{dt}$$

$$e_3=r_3i_3+L_3\frac{di_3}{dt}+M_{31}\frac{di_1}{dt}+M_{32}\frac{di_2}{dt}+M_{34}\frac{di_4}{dt}+M_{35}\frac{di_5}{dt}$$

$$e_4=r_4i_4+L_4\frac{di_4}{dt}+M_{41}\frac{di_1}{dt}+M_{42}\frac{di_2}{dt}+M_{43}\frac{di_3}{dt}+M_{45}\frac{di_5}{dt}$$

$$e_5=r_5i_5+L_5\frac{di_5}{dt}+M_{51}\frac{di_1}{dt}+M_{52}\frac{di_2}{dt}+M_{53}\frac{di_3}{dt}+M_{54}\frac{di_4}{dt}$$

where the subscripts refer to the windings involved, and

e = applied voltage
r = winding resistance
i = winding current
L = coefficient of self-inductance
M = coefficient of mutual inductance

A rigorous mathematical treatment of the foregoing fundamental equations will lead to terms suitable for an equivalent circuit.

A true equivalent circuit for a transformer is one which cannot be distinguished from the actual transformer by any observations that can be made at its terminals. It is the *terminal* characteristics of a transformer and not the characteristics of some particular coil within the transformer that are of interest.

As in most practical transformer equivalent circuits, magnetizing current has been neglected. This results in an equivalent circuit of sufficient accuracy for most all power-circuit problems. The circuit contains components which represent winding resistance, self-inductance, and load or mutual impedance. Any winding can be used as the supply winding; in fact, more than one winding may be connected to a source of energy.

Properties of the Equivalent Circuit

It is a physical and mathematical necessity that a completely equivalent network have as many independent elements (or degrees of freedom) as the original system. The properties of a circuit for the problem at hand are:

1. The circuit must have five points of entry corresponding to the five windings of the transformer.

2. The circuit must have ten independent elements in order that the ten independent measurable impedances of the transformer may be simulated.

3. The circuit should be one that avoids excessive calculation. The impedances should be simple functions of the measurable transformer impedances obtained in commercial transformer production.

4. The circuit should be free from negative-impedance elements. Although this characteristic does not void the solution of any problem, it does complicate studies on a calculating board, as negative resistance cannot be easily provided. Moreover, a circuit with no negative reactance is desirable since it may be used on both the a-c and d-c calculating boards.

Upon designating the five windings of Figure 2, with the numbers 1, 2, 3, 4, and 5, the ten independent impedances which can be measured at the terminals of the transformer are:

$$\begin{array}{cccc} Z_{12} & Z_{13} & Z_{14} & Z_{15} \\ & Z_{23} & Z_{24} & Z_{25} \\ & & Z_{34} & Z_{35} \\ & & & Z_{45} \end{array}$$

Each of these impedances is measured by applying sufficient voltage to one of the windings, whose number appears in the subscript, to circulate a current in a short

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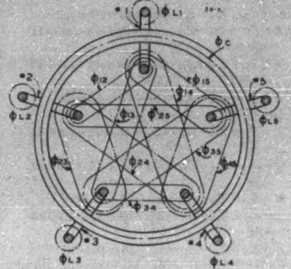

Figure 1. Flux linkages of a five-winding transformer

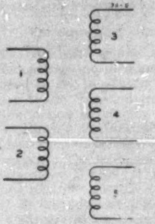

Figure 2. A simple five-winding transformer

circuit on the other winding, whose number appears in the subscript, its magnitude adjusted to the kilovolt-ampere base chosen. All other windings remain open-circuited.

The equivalent circuit used in this derivation of general formulas is that shown in Figure 3. Throughout the derivation of these general formulas, the loads indicated will not be considered.

Derivation of Formulas

Quantitative evaluation of the parameters for the five-winding transformer shown in Figure 3 can be obtained from these fundamental equalities:

$$Z_{12}=Z_1+Z_2+\cfrac{1}{\cfrac{1}{Z_6}+\cfrac{1}{Z_7+Z_8+Z_9+Z_{10}}} \qquad (1)$$

$$Z_{13}=Z_1+Z_3+\cfrac{1}{\cfrac{1}{Z_6+Z_7}+\cfrac{1}{Z_8+Z_9+Z_{10}}} \qquad (2)$$

$$Z_{14}=Z_1+Z_4+\cfrac{1}{\cfrac{1}{Z_6+Z_7+Z_8}+\cfrac{1}{Z_9+Z_{10}}} \qquad (3)$$

$$Z_{15}=Z_1+Z_5+\cfrac{1}{\cfrac{1}{Z_6+Z_7+Z_8+Z_9}+\cfrac{1}{Z_{10}}} \qquad (4)$$

$$Z_{23}=Z_2+Z_3+\cfrac{1}{\cfrac{1}{Z_7}+\cfrac{1}{Z_6+Z_8+Z_9+Z_{10}}} \qquad (5)$$

Table I. Mesh Currents for Example 2

Load On	Mesh Currents Due to Each Load Current				
	I₆	I₇	I₈	I₉	I₁₀
I_1...	47.49+j21.02	16.51−j69.02	16.51−j69.02	16.51−j69.02	16.51−j69.02
I_3...	60.20−j131.90	−119.01+j160.45	−60.20+j131.90	−60.20+j131.90	−60.20+j131.90
I_4...	217.30+j143.90	−202.30−j117.92	−202.30−j117.92	−217.30−j143.90	−217.30−j143.90
I_5...	15.69−j14.91	−2.52+j8.38	−2.52+j8.38	−2.52+j8.38	−15.69+j14.91
Total	340.68+j18.11	−307.32−j18.11	−248.51−j46.66	−263.51−j72.64	−276.68−j66.11

$$Z_{24}=Z_2+Z_4+\cfrac{1}{\cfrac{1}{Z_7+Z_8}+\cfrac{1}{Z_6+Z_9+Z_{10}}} \qquad (6)$$

$$Z_{25}=Z_2+Z_5+\cfrac{1}{\cfrac{1}{Z_7+Z_8+Z_9}+\cfrac{1}{Z_6+Z_{10}}} \qquad (7)$$

$$Z_{34}=Z_3+Z_4+\cfrac{1}{\cfrac{1}{Z_8}+\cfrac{1}{Z_6+Z_7+Z_9+Z_{10}}} \qquad (8)$$

$$Z_{35}=Z_3+Z_5+\cfrac{1}{\cfrac{1}{Z_8+Z_9}+\cfrac{1}{Z_6+Z_7+Z_{10}}} \qquad (9)$$

$$Z_{45}=Z_4+Z_5+\cfrac{1}{\cfrac{1}{Z_9}+\cfrac{1}{Z_6+Z_7+Z_8+Z_{10}}} \qquad (10)$$

These equations can be simplified by successive steps and solved simultaneously resulting in the following ten expressions:

$$Z_6Z_7=(K-2Z_2)\frac{Z_t}{2} \qquad (11)$$

$$Z_6Z_8=L\frac{Z_t}{2} \qquad (12)$$

$$Z_6Z_9=M\frac{Z_t}{2} \qquad (13)$$

$$Z_6Z_{10}=(N-2Z_1)\frac{Z_t}{2} \qquad (14)$$

$$Z_7Z_8=(O-2Z_3)\frac{Z_t}{2} \qquad (15)$$

$$Z_7Z_9=P\frac{Z_t}{2} \qquad (16)$$

$$Z_7Z_{10}=Q\frac{Z_t}{2} \qquad (17)$$

$$Z_8Z_9=(R-2Z_4)\frac{Z_t}{2} \qquad (18)$$

$$Z_8Z_{10}=S\frac{Z_t}{2} \qquad (19)$$

$$Z_9Z_{10}=(T-2Z_5)\frac{Z_t}{2} \qquad (20)$$

in which

$$K=Z_{23}+Z_{12}-Z_{13} \qquad (21)$$

$$L=Z_{13}+Z_{24}-Z_{14}-Z_{23} \qquad (22)$$

$$M=Z_{14}+Z_{25}-Z_{15}-Z_{24} \qquad (23)$$

$$N=Z_{12}+Z_{15}-Z_{25} \qquad (24)$$

$$O=Z_{34}+Z_{23}-Z_{24} \qquad (25)$$

$$P=Z_{24}+Z_{35}-Z_{34}-Z_{25} \qquad (26)$$

$$Q=Z_{13}+Z_{25}-Z_{12}-Z_{35} \qquad (27)$$

$$R=Z_{45}+Z_{34}-Z_{35} \qquad (28)$$

$$S=Z_{14}+Z_{35}-Z_{13}-Z_{45} \qquad (29)$$

$$T=Z_{45}+Z_{15}-Z_{14} \qquad (30)$$

and

$$Z_t=Z_6+Z_7+Z_8+Z_9+Z_{10} \qquad (31)$$

The links of the pentagonal mesh are evaluated by using the following algebraic relations in the second-degree equations 12, 13, 16, 17, and 19:

$Z_7=aZ_6$
$Z_8=bZ_6$
$Z_9=cZ_6$
$Z_{10}=dZ_6$

Equating various of the resulting expressions to the common quantity Z_6, these coefficients are obtained:

$$a=\frac{P}{M}$$

$$b=\frac{SP}{QM}$$

$$c=\frac{SP}{LQ}$$

$$d=\frac{S}{L}$$

Equations 11 to 20 inclusive are solved for various of the network parameters resulting in

$$Z_1=\frac{N}{2}-\frac{1}{Z_t}(Z_6Z_{10}) \qquad (32)$$

$$Z_2=\frac{K}{2}-\frac{1}{Z_t}(Z_6Z_7) \qquad (33)$$

$$Z_3=\frac{O}{2}-\frac{1}{Z_t}(Z_7Z_8) \qquad (34)$$

Table A

Winding	Kilovolt-Ampere Base	Reactance Per Cent
1 to 2	20,000	14
1 to 3	20,000	20
1 to 4	5,000	12
1 to 5	5,000	15
2 to 3	20,000	16
2 to 4	5,000	10
2 to 5	5,000	11
3 to 4	5,000	8
3 to 5	5,000	8
4 to 5	5,000	6

$$Z_4=\frac{R}{2}-\frac{1}{Z_t}(Z_8Z_9) \qquad (35)$$

$$Z_5=\frac{T}{2}-\frac{1}{Z_t}(Z_9Z_{10}) \qquad (36)$$

$$Z_6=\frac{MQL}{2SP}+\frac{L}{2}\left(\frac{Q}{S}+1\right)+\frac{M}{2}\left(\frac{Q}{P}+1\right) \qquad (37)$$

$$Z_7=\frac{P}{M}Z_6 \qquad (38)$$

$$Z_8=\frac{SP}{QM}Z_6 \qquad (39)$$

$$Z_9=\frac{SP}{QL}Z_6 \qquad (40)$$

$$Z_{10}=\frac{S}{L}Z_6 \qquad (41)$$

$$Z_t=Z_6+Z_7+Z_8+Z_9+Z_{10} \qquad (31)$$

in which the constants are those previously defined.

Comments on Negative Impedance

No practical use can be made of the negative resistances or reactances of transformers as negative impedance by themselves. These negative impedances can be considered as mathematical fictions. They are virtual values. They reproduce faithfully the *terminal* characteristics of the transformer, but they cannot necessarily be applied to internal coils.

Negative resistance may appear in high-efficiency transformers in which the stray impedance losses become appreciable when compared with the low ohmic copper losses. This condition quite often occurs in three-winding transformers and is even more apparent in a five-winding transformer.

The total leakage reactance between a pair of windings cannot be negative, but there is no reason why the mutual effect between circuits for load currents may not be negative, depending on how the leakage field of one interacts with that of the other. In the circuits considered, the individual leakage impedances assigned to a circuit constitute mutual load impedance for the other circuits, and as such may be either positive or negative.

The total copper losses, including stray impedance losses, are correctly represented by the equivalent circuit, whatever the signs of its branches may be; but the simple equivalent circuit provides no means of distributing the losses within the transformer windings. Some of these losses are not in the windings at all, but in the clamps, core end plates, and tank, and therefore the conductor heating cannot be based on the losses of the equivalent circuit. However, the total impedance losses causing the heating of the oil are given by the equivalent circuit.

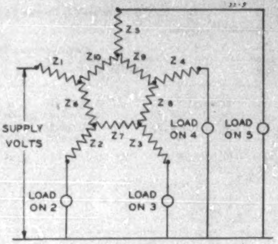

Figure 3. Five-winding transformer equivalent circuit

Examples

The foregoing theory can be applied to any five-winding transformer in a manner similar to that encountered in any electrical network. The only requirement is that certain data relating the actual circuits must be available either from measurements or from design calculations in order that the equivalent circuit may be set up. With the aid of the equations developed herein, each unknown can be evaluated singly. It is evident that all equations, depend upon determining the mesh impedances in the order Z_6, Z_7, Z_8, Z_9, and Z_{10}.

The application of these principles to two types of problems will be outlined in detail. Regardless of the degree of simplicity or complexity, the same precise rules must be followed. The importance of observance of the sign, positive or negative, accompanying each numerical value cannot be emphasized too strongly.

The first example demonstrates the use of this method of solution when only the reactance component of impedance is important. A second example treats resistance and reactance with equal importance, as is common when complex quantities represent the impedance units. Only a single-phase solution need be made for any balanced three-phase bank; however, if unlike transformers are involved, a solution for each phase must be made.

Example 1. Reactance Components Only. A five-winding transformer has the reactance between windings related to the kilovolt-ampere base indicated for each as shown in Table A. When all reactances are translated to a common kilovolt-ampere base, 20,000 in this case, the reactance values become those indicated in Table B. Then

$$K=Z_{23}+Z_{12}-Z_{13}=16+14-20=10$$
$$L=Z_{13}+Z_{24}-Z_{14}-Z_{23}=20+40-48-16=-4$$
$$M=Z_{14}+Z_{25}-Z_{15}-Z_{24}=48+44-60-40=-8$$
$$N=Z_{12}+Z_{15}-Z_{25}=14+60-44=30$$
$$O=Z_{34}+Z_{23}-Z_{24}=32+16-40=8$$
$$P=Z_{24}+Z_{35}-Z_{34}-Z_{25}=40+32-32-44=-4$$
$$Q=Z_{13}+Z_{25}-Z_{12}-Z_{35}=20+44-14-32=18$$
$$R=Z_{45}+Z_{34}-Z_{35}=24+32-32=24$$
$$S=Z_{14}+Z_{35}-Z_{13}-Z_{45}=48+32-20-24=36$$
$$T=Z_{45}+Z_{15}-Z_{14}=24+60-48=36$$

The mesh links are

$$Z_6=\frac{MQL}{2SP}+\frac{L}{2}\left(\frac{Q}{S}+1\right)+\frac{M}{2}\left(\frac{Q}{P}+1\right)$$
$$=\frac{(-8)\times18\times(-4)}{2\times36\times(-4)}+\frac{(-4)}{2}\left(\frac{18}{36}+1\right)+\frac{(-8)}{2}\left(\frac{18}{-4}+1\right)=9$$

$$Z_7=\frac{P}{M}\ Z_6=\frac{(-4)}{(-8)}\times9=4.5$$

$$Z_8=\frac{SP}{QM}\ Z_6=\frac{36\times(-4)}{18\times(-8)}\times9=9$$

$$Z_9=\frac{SP}{QL}\ Z_6=\frac{36\times(-4)}{18\times(-4)}\times9=18$$

$$Z_{10}=\frac{S}{L}\ Z_6=\frac{36}{(-4)}\times9=-81$$

$$Z_t=Z_6+Z_7+Z_8+Z_9+Z_{10}$$
$$=9+4.5+9+18-81=-40.5$$

and the branches are

$$Z_1=\frac{N}{2}-\frac{1}{Z_t}(Z_6Z_{10})$$
$$=\frac{30}{2}-\frac{1}{(-40.5)}(9\times-81)=-3$$

$$Z_2=\frac{K}{2}-\frac{1}{Z_t}(Z_6Z_7)$$
$$=\frac{10}{2}-\frac{1}{(-40.5)}(9\times4.5)=6$$

$$Z_3=\frac{O}{2}-\frac{1}{Z_t}(Z_7Z_8)$$
$$=\frac{8}{2}-\frac{1}{(-40.5)}(4.5\times9)=5$$

$$Z_4=\frac{R}{2}-\frac{1}{Z_t}(Z_8Z_9)$$
$$=\frac{24}{2}-\frac{1}{(-40.5)}(9\times18)=16$$

$$Z_5=\frac{T}{2}-\frac{1}{Z_t}(Z_9Z_{10})$$
$$=\frac{36}{2}-\frac{1}{(-40.5)}(18\times-81)=-18$$

Example 2. Determination of Current and Voltage Relations for a Five-Winding Transformer Under Load Conditions. A single-phase five-winding transformer has the characteristics shown in Table C and

Table B

Winding	Reactance Per Cent
1 to 2	14
1 to 3	20
1 to 4	48
1 to 5	60
2 to 3	16
2 to 4	40
2 to 5	44
3 to 4	32
3 to 5	32
4 to 5	24

impedance data given in Table D. The circuit diagram of this transformer is shown in Figure 2.

The following information about this transformer installation is required:

1. The equivalent circuit.
2. The load on winding 3 and its power factor when power is supplied from a transmission line to winding 2, maximum capacity 10,000 kva, and from a local generating station to winding 3. The load is on windings 1 and 5, and a synchronous condenser is on winding 4 for power-factor correction. The loads and their power factor are shown in Table E. In order to operate the transmission line connected to winding 2 at its optimum value, an overload of 1,500 kva is necessary on winding 4 from the synchronous condenser.
3. The voltage at each transformer terminal if it is desirable to maintain rated line voltage on winding 3, assuming load ratio control equipment is available to distribute the loads as given in 2.

Solution of 1. The equivalent circuit. The impedance when referred to a common base of kilovolt-amperes of 30,000 is:

$$Z_{12}=\left(\frac{50\times100}{10,000}+j20\right)3=1.5+j60$$

$$Z_{13}=\left(\frac{150\times100}{30,000}+j20\right)=0.5+j20$$

$$Z_{14}=\left(\frac{30\times100}{7,500}+j10\right)4=1.6+j40$$

$$Z_{15}=\left(\frac{15\times100}{5,000}+j15\right)6=1.8+j90$$

$$Z_{23}=\left(\frac{40\times100}{10,000}+j10\right)3=1.2+j30$$

$$Z_{24}=\left(\frac{52.5\times100}{7,500}+j5\right)4=2.8+j20$$

$$Z_{25}=\left(\frac{20\times100}{5,000}+j15\right)6=2.4+j90$$

Table C

Winding	Kilovolt-Amperes	Circuit Voltage
1	30,000	230,000 Wye
2	10,000	115,000 Wye
3	30,000	22,000 delta
4	7,500	13,800 delta
5	5,000	34,500 delta

$$Z_{34}=\left(\frac{45\times100}{7,500}+j10\right)4=2.4+j40$$

$$Z_{35}=\left(\frac{30\times100}{5,000}+j10\right)6=3.6+j60$$

$$Z_{45}=\left(\frac{25\times100}{5,000}+j5\right)6=3.0+j30$$

from which

$$K=Z_{23}+Z_{12}-Z_{13}=(1.2+j30)+(1.5+j60)-(0.5+j20)=2.2+j70$$

$$L=Z_{13}+Z_{24}-Z_{14}-Z_{23}=(0.5+j20)+(2.8+j20)-(1.6+j40)-(1.2+j30)=0.5-j30$$

$$M=Z_{14}+Z_{25}-Z_{15}-Z_{24}=(1.6+j40)+(2.4+j90)-(1.8+j90)-(2.8+j20)=-0.6+j20$$

$$N=Z_{12}+Z_{15}-Z_{25}=(1.5+j60)+(1.8+j90)-(2.4+j90)=0.9+j60$$

$$O=Z_{34}+Z_{23}-Z_{24}=(2.4+j40)+(1.2+j30)-(2.8+j20)=0.8+j50$$

$$P=Z_{24}+Z_{35}-Z_{34}-Z_{25}=(2.8+j20)+(3.6+j60)-(2.4+j40)-(2.4+j90)=1.6-j50$$

$$Q=Z_{13}+Z_{25}-Z_{12}-Z_{35}=(0.5+j20)+(2.4+j90)-(1.5+j60)-(3.6+j60)=-2.2-j10$$

$$R=Z_{45}+Z_{34}-Z_{35}=(3.0+j30)+(2.4+j40)-(3.6+j60)=1.8+j10$$

$$S=Z_{14}+Z_{35}-Z_{13}-Z_{45}=(1.6+j40)+(3.6+j60)-(0.5+j20)-(3.0+j30)=1.7+j50$$

$$T=Z_{45}+Z_{15}-Z_{14}=(3.0+j30)+(1.8+j90)-(1.6+j40)=3.2+j80$$

Substituting these values into the equations for the mesh impedances, 37 to 41, and 31, these result:

$$Z_6=\frac{MQL}{2SP}+\frac{L}{2}\left(\frac{Q}{S}+1\right)+\frac{M}{2}\left(\frac{Q}{P}+1\right)=\frac{(-0.6+j20)(-2.2-j10)(0.5-j30)}{2(1.7+j50)(1.6-j50)}+\frac{(0.5-j30)}{2}\left(\frac{-2.2-j10}{1.7+j50}+1\right)+\frac{(-0.6+j20)}{2}\left(\frac{-2.2-j10}{1.6-j50}+1\right)=0.697-j1.19$$

$$Z_7=\frac{P}{M}\ Z_6=\frac{1.6-j50}{-0.6+j20}(0.697-j1.19)=-1.749+j2.97$$

$$Z_8=\frac{SP}{QM}\ Z_6=\frac{(1.6-j50)(1.7+j50)}{(-2.2-j10)(-0.6+j20)}\times(0.697-j1.19)=11.05-j12.75$$

$$Z_9=\frac{SP}{QL}\ Z_6=\frac{(1.7+j50)(1.6-j50)}{(-2.2-j10)(0.5-j30)}\times(0.697-j1.19)=-7.48+j8.40$$

$$Z_{10}=\frac{S}{L}\ Z_6=\frac{1.7+j50}{0.5-j30}(0.697-j1.19)=-1.06+j2.04$$

$$Z_t=Z_6+Z_7+Z_8+Z_9+Z_{10}=(0.697-j1.19)+(-1.749+j2.97)+(11.05-j12.75)+(7.48+j8.40)+(-1.06+j2.04)=1.46-j0.53.$$

It follows that the branch impedances from equations 32 to 36 inclusive are:

$$Z_1=\frac{N}{2}-\frac{1}{Z_t}(Z_6Z_{10})=\frac{0.9+j60}{2}-\frac{(0.697-j1.19)(-1.06+j2.04)}{(1.46-j0.53)}=0.03+j28.03$$

$$Z_2=\frac{K}{2}-\frac{1}{Z_t}(Z_6Z_7)=\frac{2.2+j70}{2}-\frac{(0.697-j1.19)(-1.749+j2.97)}{(1.46-j0.53)}=0.61+j31.98$$

$$Z_3=\frac{O}{2}-\frac{1}{Z_t}(Z_7Z_8)=\frac{0.8+j50}{2}-\frac{(-1.749+j2.97)(11.05-j12.75)}{(1.46-j0.53)}=1.28-j12.45$$

$$Z_4=\frac{R}{2}-\frac{1}{Z_t}(Z_8Z_9)=\frac{1.8+j10}{2}-\frac{(11.05-j12.75)(-7.48+j8.40)}{(1.46-j0.53)}=27.43-j117.70$$

$$Z_5=\frac{T}{2}-\frac{1}{Z_t}(Z_9Z_{10})=\frac{3.2+j80}{2}-\frac{(-7.48+j8.40)(-1.06+j2.04)}{(1.48-j0.53)}=1.87+j56.65$$

Solution of 2. The Power Supplied to Winding 3 and Its Power Factor When the Fluctuating Load Is Absorbed by the Generating Station on Winding 3. In large power transformers the effect of losses and reactance on input energy and power factor as compared with the output and its power factor is quite small, sufficiently so, to justify its omission in the interest of simplification.

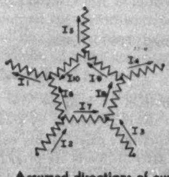

Figure 4. Assumed directions of currents for example 2

The second source of power, that of winding 3, is regarded as a negative load.

Kilovolt-ampere load on winding 1
$=24,000\ (0.8-j0.6)=19,200-j14,400$
Kilovolt-ampere load on winding 2
$=-10,000\ (1.0+j0)=-10,000+j0$
Kilovolt-ampere load on winding 4
$=9,000\ (0.5+j0.866)=4,500+j7,794$
Kilovolt-ampere load on winding 5
$=4,500\ (0.9-j0.435)=\underline{3,950-j1,958}$
Kilovolt-amperes supplied by winding 3
$=17,650-j8,564$
$=19,625$ kva at 89.9 per cent power factor

Table D

Winding	Kilovolt-Ampere Base	Copper Loss Kilowatts	Reactance Per Cent
1 to 2	10,000	50	20
1 to 3	30,000	150	20
1 to 4	7,500	30	10
1 to 5	5,000	15	15
2 to 3	10,000	40	10
2 to 4	7,500	52.5	5
2 to 5	5,000	20	15
3 to 4	7,500	45	10
3 to 5	5,000	30	10
4 to 5	5,000	25	5

Solution of 3. The Voltage at Each Transformer Line Terminal When It Is Desired to hold 22,000 Volts Across Winding 3 With the Local Generator. Since all values are expressed in terms of per cent with the exception of the loads, it is advantageous to convert all loads to a per cent basis using 30,000 kva as the reference. The per cent load on the windings is:

On winding 1 $(19{,}200-j14{,}400)\dfrac{100}{30{,}000}=64-j48=I_1$

On winding 2 $(10{,}000+j0)\dfrac{100}{30{,}000}=33.33+j0=I_2$

On winding 3 $(17{,}650-j8{,}564)\dfrac{100}{30{,}000}=58.81-j28.55=I_3$

On winding 4 $(4{,}500+j7{,}794)\dfrac{100}{30{,}000}=15.00+j25.98=I_4$

On winding 5 $(3{,}950-j1{,}958)\dfrac{100}{30{,}000}=13.17-j6.53=I_5$

The division of load by the various mesh circuits is arrived at by solving for the currents that exist, taking each load singly. After all loads are properly proportioned according to the rules of parallel circuits, the resultant current in each mesh link is found by a simultaneous superposition. Winding 2 is used as the source in all load-division computations. The direction of currents has been assumed as in Figure 4 in which the current in link 7 due to load on winding 1 is:

$$I_7=I_8=I_9-I_{10}=\frac{Z_6}{Z_t}I_1$$
$$=\frac{0.697-j1.19}{1.46-j0.53}(64-j48)=16.51-j69.02$$

Also

$$I_6=I_1-I_7=(64-j48)-(16.51-j69.02)$$
$$=47.49+j21.02$$

Table E

Winding	Kilovolt-Amperes	Per Cent
1	24,000	80 lagging
4	9,000	50 leading
5	4,500	90 lagging

Current in link 6 due to load on winding 3 is:

$$I_6=-I_{10}=-I_9=-I_8=$$
$$\frac{Z_7}{Z_t}I_3=\frac{-1.749+j2.97}{1.46-j0.53}\times$$
$$(-58.81+j28.55)=60.20-j131.90$$

Also

$$I_7=-I_3+I_6=(-58.81+j28.55)+$$
$$(-60.20+j131.90)=-119.01+j160.45$$

Current in link 6 due to load on winding 4 is:

$$I_6=-I_{10}=-I_9=\frac{Z_7+Z_8}{Z_t}I_4$$
$$=\frac{(-1.749+j2.97)+(11.05-j12.75)}{1.46-j0.53}\times$$
$$(15.0+j25.98)=217.30+j143.90$$

Also

$$I_7=I_8=I_4-I_6=(15.0+j25.98)-(217.30+$$
$$j143.90)=-202.30-j117.92$$

Current in link 7 due to load on winding 5 is:

$$I_7=I_8=I_9=\frac{Z_6+Z_{10}}{Z_t}I_5$$
$$=\frac{(0.697-j1.19)+(-1.06+j2.04)}{1.46-j0.53}\times$$
$$(13.17-j6.53)=-2.52+j8.38$$

Also

$$I_6=-I_{10}=I_5-I_7=(13.17-j6.53)-$$
$$(-2.52+j8.38)=15.69-j14.91$$

The superposition of all currents is most conveniently handled in a table similar to Table I.

The per cent voltage at the terminals of any winding is determined by subtracting the per cent voltage drop through the circuit from the 100 per cent value assigned to the winding used as a reference.

The per cent voltage drop in the branches and links of the mesh is:

$$I_1Z_1=(64-j48)(0.03+j28.03)\frac{1}{100}$$
$$=13.47+j17.93$$

$$I_2Z_2=(33.33+j0)(0.61+j31.98)\frac{1}{100}$$
$$=0.20+j10.66$$

$$I_3Z_3=(58.81-j28.55)(1.28-j12.45)\frac{1}{100}$$
$$=2.80-j7.69$$

$$I_4Z_4=(15.0+j25.98)(27.43-j117.70)\frac{1}{100}$$
$$=34.71-j10.52$$

$$I_5Z_5=(13.17-j6.53)(1.87+j56.65)\frac{1}{100}$$
$$=3.95+j7.34$$

$$I_6Z_6=(340.68+j18.11)(0.697-j1.19)\frac{1}{100}$$
$$=2.59-j3.93$$

$$I_7Z_7=(-307.32-j18.11)(-1.749+j2.97)\frac{1}{100}$$
$$=5.91-j8.81$$

$$I_8Z_8=(-248.51-j46.66)(11.05-j12.75)\frac{1}{100}$$
$$=-33.42+j26.54$$

$$I_9Z_9=(-263.51-j72.64)(-7.48+j8.40)\frac{1}{100}$$
$$=25.81-j16.70$$

$$I_{10}Z_{10}=(-276.68-j66.11)(-1.06+j2.04)\frac{1}{100}$$
$$=4.28-j4.94$$

The voltage on the generator bus is held at 22,000 volts, which is our 100 per cent base and the point from which all voltages are calculated.

$$\text{Per cent } V_1=100-(I_3Z_3-I_7Z_7+I_6Z_6+I_1Z_1)$$
$$=100-[(-2.80-j7.69)-$$
$$(5.91-j8.81)+(2.59-j3.93)+$$
$$(13.47+j17.93)]=92.65-j15.03$$

$$\text{and } V_1=230{,}000\sqrt{\overline{92.65}^2+\overline{15.03}^2}$$
$$=216{,}000 \text{ volts}$$

$$\text{Per cent } V_2=100-(I_3Z_3-I_7Z_7-I_2Z_2)=$$
$$100-[(-2.80-j7.69)-(5.91-j8.81)-$$
$$(0.20+j10.66)]=108.51-j9.54$$

$$\text{and } V_2=115{,}000\sqrt{\overline{108.51}^2+\overline{9.54}^2}$$
$$=125{,}400 \text{ volts}$$

$$\text{Per cent } V_4=100-(I_3Z_3+I_8Z_8+I_4Z_4)$$
$$=100-[(-2.80-j7.69)+(-33.42+$$
$$j26.54)+(34.71-j10.52)]=101.52-j8.33$$

$$\text{and } V_4=13{,}800\sqrt{\overline{101.52}^2+\overline{8.33}^2}$$
$$=14{,}070 \text{ volts}$$

$$\text{Per cent } V_5=100-(I_3Z_3+I_8Z_8+I_9Z_9+$$
$$I_5Z_5)=100-[(-2.80-j7.69)+(-33.42$$
$$+j26.54)+(25.81-j16.70)+(3.95+$$
$$j7.34)]=109.47-j9.49$$

$$\text{and } V_5=34{,}500\sqrt{\overline{109.47}^2+\overline{9.49}^2}$$
$$=37{,}930 \text{ volts}$$

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Paired-Phase Bus Bars for Large Polyphase Currents

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THE design of efficient conductor systems for large alternating currents has always been a problem, and the problem is especially difficult with large low-voltage polyphase currents, such as are found in feeder distribution systems for industrial plants. Special conductor shapes and arrangements have been proposed in the past and have proved satisfactory for some applications, but all have had their limitations. A new method of arranging bus bars, named paired phasing, has now been developed and has been found to be superior to conventional arrangements for transmitting polyphase currents in the range of 1,000 amperes and above, at 600 volts and less.

The paired-phase arrangement is described in this paper in terms of enclosed bus-bar systems, as most of the experience in the use of the arrangement has been in that connection, but emphasis has been placed on the general characteristics of the arrangement rather than on this specific application. Therefore, the comparisons with conventional arrangements, the basic electrical and mathematical theory, and the construction and performance data will readily suggest the adaptability of paired phasing to other applications where efficient utilization of conductor materials and convenient structure are desired.

Only the three-phase version of the paired-phase arrangement is considered in this paper. It is shown in cross section in Figure 1. There are six conductors arranged in three separate and distinct pairs. The pairs are *a-b*, *b-c*, and *c-a*; that is, the two conductors in each close-spaced pair are connected to different phases. Each of the three phase currents is thus carried by two conductors in parallel. Wide flat bars placed face to face proved to have the most satisfactory conductor shape.

Summary

With the paired-phase arrangement, many of the difficulties associated with the transmission of large alternating currents are minimized. Nonuniform current density, including "skin effect" and "proximity effect," is greatly reduced. This results in low a-c energy losses which combines with effective heat dissipation to produce low and uniform temperature rise. Both resistance and reactance voltage drops are very small, and voltage drop is almost perfectly balanced. Furthermore, the dimensions that produce all these desirable electrical and thermal characteristics lend themselves to convenient structural designs wherein the problems of joining adjacent bus-bar lengths, making tap-offs, support against mechanical and short-circuit forces, and insulation are greatly simplified.

There are several specific features which particularly adapt the paired-phase arrangement to prefabricated enclosed bus-bar systems. Enclosure, of course, impedes heat dissipation, but the low heat generation minimizes this effect. Metallic or magnetic enclosures cause no additional losses and no increase in reactance. Structural designs are possible which facilitate erection in the field and the adaptation of standardized constructions to special installation conditions.

The paired-phase arrangement has several unique properties that distinguish it from previously known arrangements. Its low voltage drop and low losses depend largely upon close intrapair spacing, dimension *s* in Figure 1. The interpair spacing *d*, or different relative pair locations, as illustrated by the three variations of Figure 1, have no appreciable effect upon voltage drop and, therefore, may be varied to obtain low temperature rise and convenient structure. The balanced voltage drop of the paired-phase arrangement does not depend upon geometrical symmetry.

A peculiar inherent characteristic of the arrangement is that the currents in each close-spaced pair of bars are nearly equal and opposite, or somewhat similar to the currents in a single-phase transmission system, and this current division appears to be responsible for paired-phase performance. This single-phase approach also forms the basis of a simple method for calculating paired-phase performance.

Paired-Phase and Conventional Arrangements Tested

The best way to appraise the performance of the paired-phase arrangements is by comparison with commonly used conventional arrangements, shown in Figure 2. In contrast to the paired-phase arrangement, the laminated arrangement has closely spaced bars connected to the same phase, while the interlaced arrangement has the bars evenly spaced and not grouped into distinct pairs.

A large amount of information has been published concerning open laminated bus bars where phases are well separated and no enclosures are present.[1,2] Most of this information is completely inapplicable to conventional enclosed bus-bar assemblies, because additional proximity-effect losses caused by interphase spacings of two or three inches, induced losses in metallic enclosures, and greatly restricted heat dissipation make performance much worse than commonly supposed. Some temperature-rise data for small-size enclosed bus bars and some reactance and resistance data for large-size bus bars in aluminum enclosures are available,[3] but to fully compare and contrast the performance of the paired-phase arrangement new data will be presented upon all of the arrangements shown in Figures 1 and 2.

Designs having two one-fourth- by four-inch round-edge copper bars per phase have been selected for presentation; this size represents medium rather than large current-carrying capacity and emphasizes the need for careful conductor design even in this size. The basic paired-phase theory applies equally well to all conductor sizes commonly used. In general, the relative performance of larger sizes of laminated bus bars is much poorer, while the relative performance of larger sizes of paired-phase and interlaced arrangements is roughly the same.

In order to simplify comparisons, a rectangular 14-gauge steel housing with a cross-section perimeter of 53 inches was used in all tests; the relative breadth and width were varied slightly to accommodate the various arrangements. A housing of this size represents standard practice for the wider-spaced arrangements, but is larger than would ordinarily

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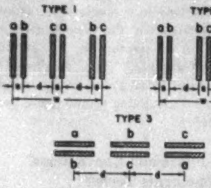


Figure 1. Three variations of the paired-phase bus-bar arrangement

s = intrapair spacing
d = interpair spacing
w = width

be used with the very compact arrangements having a small over-all width, *w*. Performance of commercial compact arrangements with a smaller housing would, therefore, usually be slightly inferior to that indicated.

The dimension *s* was kept constant at five-eighths inch during most paired-phase and laminated tests. Comparison of laminated performance with *s* equal one-half inch, the more conventional dimension, indicated a difference of small magnitude. An intrapair spacing of five-eighths inch represents a more than adequate spacing for practical paired-phase arrangements. The effect of variation of this dimension will, however, be discussed as the test results are analyzed.

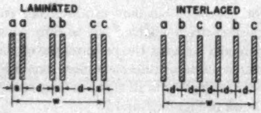


Figure 2. Conventional bus-bar arrangements

All tests were made upon a 40-foot run, comprising four ten-foot sections, in order to minimize end effects. In each case, all bus bars were connected together at one end and low-voltage balanced three-phase 60-cycle current fed in the other end. Voltage measurements were made line to line at the feed-in end of the run to eliminate induced voltages in the potential leads. Power was measured by a special low-voltage wattmeter.

The point-to-point variation in current density in each bus bar was studied by insulated exploring wires cemented to the surfaces of the bars in the manner employed by a number of experimenters.[4–6] The exploring wire measurements also provided a means of analyzing the location of energy losses in greater detail than is possible with a wattmeter, which is suitable only for over-all averages. Each bar was equipped with 16 number 28 wires, eight feet long, spaced at one-half inch intervals around the bar perimeter. The voltage induced in each wire, which is a measure of the current density adjacent to the wire, was measured by a rectangular co-ordinate a-c potentiometer. The total current in each bar was also measured with a magnetic potentiometer or Rogowski coil.[7]

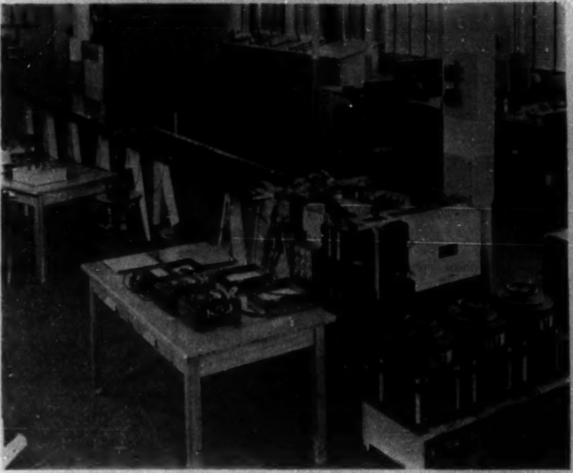


Figure 3. Typical test setup

Temperatures were measured by thermocouples. In all tests both the bus bars and housing were painted with a nonmetallic paint to assure maximum and consistent heat radiation. All temperature rises given would be approximately 25 to 35 per cent higher if the bus bars were not painted and 50 to 70 per cent higher if neither housing nor bus bars were painted.

A portion of the research laboratory with a typical test setup is shown in Figure 3. One housing section has been removed so that the interior construction may be seen.

Voltage Drop, Temperature Rise, and Energy Losses

Relative voltage drops and temperature rises are given in Figure 4. Since the voltage drop is unbalanced to some extent in all arrangements, both the maximum line-to-line impedance drop and the minimum line-to-line impedance drop are plotted, with the area between shaded, so that the relative amount of unbalance may be seen. These values are plotted against the over-all width of the bus-bar assembly, *w*, for all of the vertical-bar arrangements, but it is not intended that comparisons be made only for equal over-all widths. This dimension has no direct significance for the paired-phase type-3 arrangement, but impedance drops and temperature rise for this arrangement are plotted to the same scales so that appraisals may be made. For all arrangements the maximum temperature rise decreases for increased spacings. In the case of the laminated and interlaced arrangements, increased spacing causes both increased voltage drop and increased voltage-drop unbalance; in the paired-phase arrangements increased interpair spacing causes substantially no change in voltage drop but provides better voltage-drop balance.

Impedance drops are separated into average reactance and resistance components in Figures 5 and 6, respectively. The reactance curves in particular show the independence of paired-phase characteristics upon relative pair position. The resistance curves show that both the paired-phase and interlaced arrangements have low average a-c resistance. This factor will be considered in more detail later in connection with an analysis of energy losses. The effect of intrapair spacing upon paired-phase reactance may be seen in Figure 7. This curve is actually for the type-3 arrangement but applies approximately to any practical paired-phase arrangement where interpair distance is great relative to intrapair distance. It shows that lower reactance if

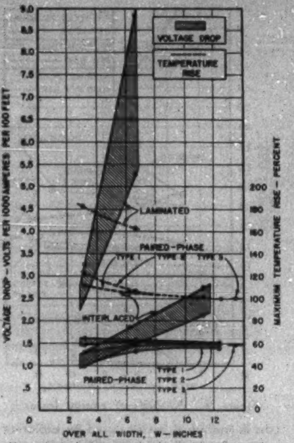


Figure 4. Effect of arrangement and spacing upon line-to-line impedance voltage drop and upon maximum temperature rise

Temperature rises are in per cent of that of the best arrangements. Paired-phase and laminated, $s = {}^3/_8$ inch. Type 3 paired-phase, $d = 6$ inches. Bar size $^1/_4$ by 4 inches

desired may be achieved by closer intrapair spacing.

Since the paired-phase arrangements with larger interpair spacings (type-1 or 2 arrangement with an over-all width of about six inches or more, or a type-3 arrangement) have superior characteristics from all considerations, there appears to be no practical reason other than space limitations for using smaller interpair spacings. Measurements made with these smaller spacings have been included, however, because of theoretical interest.

The inferior performance of the laminated arrangement needs no further comment, except that one may point out that there may be special instances, such as short connection straps where its use in preference to a more efficient arrangement may be justified by structural limitations.

In comparing the paired-phase and the interlaced arrangements, the practicability of obtaining various bus-bar spacings must be considered. Very close intrapair spacing is practical with paired-phasing, because bars may be offset into the vacant area surrounding each pair of bars at points where connections between adjacent bus-bar lengths or tap-off connections are to be made. Thus a clear space of one-fourth or three-eighths inch between paired-phase bars means a full

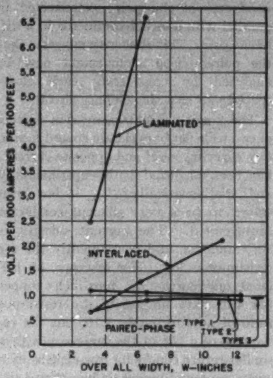


Figure 5. Effect of arrangement and spacing upon line-to-line reactance voltage drop

spacing of that amount throughout the entire bus-bar run. The vacant area surrounding each pair also simplifies the problem of support and insulation. In the interlaced arrangement, because joint and tap-off connections must be made in the confined space between adjacent bars, clearances are necessarily decreased at these points, and insulation and installation problems are complicated. In interlaced arrangements a compromise must be made therefore between electrical properties, temperature rise, and structural complications; that compromise is not necessary with paired-phasing. These facts must be kept in mind when considering the data presented in this paper. For instance, an interlaced bus-bar system with an over-all width, *w*, of ten inches would have $1^3/_4$ inches between adjacent bars whereas a type-1 or type-2 paired-phase system of the same over-all width would have approximately $3^3/_4$ inches between pairs or a type-3 arrangement could be used without changing the electrical characteristics.

The relative current density for typical bus-bar arrangements is shown in Figure 8 in which the quantity plotted is the square of the magnitude of the current density. The measurements from the two faces of each bar have been averaged. This method of plotting gives an indication of relative energy losses, which are proportional to the areas under the curves. The more uniform current distribution within each bar and between bars of the paired-phase arrangements is evident. The small interpair effect is indicated by the slight difference in distribution between the two types of paired-phase arrangements.

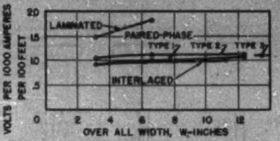


Figure 6. Effect of arrangement and spacing upon line-to-line resistance voltage drop

Copper temperature 75 degrees centigrade

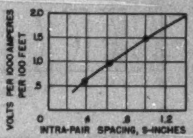


Figure 7. Effect of intrapair spacing, *s*, upon line-to-line reactance voltage drop of type-3 paired-phase arrangement

The nonuniform current distribution in both the laminated and the interlaced arrangements is caused by the greater reactance of the outside bars in these arrangements.

The analysis of energy losses in Figure 9 clarifies the performance of the paired-phase arrangement. The most important energy loss is the loss in that individual bar having the greatest energy loss, as this will determine the maximum "hot-spot" temperature rise, and in this respect paired phasing excels. Slightly lower average losses may be secured by using smaller interpair spacings, approaching the interlaced arrangement, but the greater resulting unbalance makes the individual bar losses higher rather than lower. Paired-phase housing losses, as shown in the lower portion of Figure 9, are negligible, and relative pair position has no influence upon these losses. Laminated-arrangement housing losses increase with wide spacing to such an extent that the average total loss increases even though individual bar loss decreases because of lessened proximity effect.

Equal and Opposite Currents Explain Paired-Phase Performance

Paired-phase performance may be understood by examining the distribution of current within the bus bars and the consequent magnetic-field effects in more detail. The magnitude and phase angle of the current density are shown in Figure 10, while the net currents in individual bars are shown in Figure 11. Only one paired-phase arrangement is shown, but

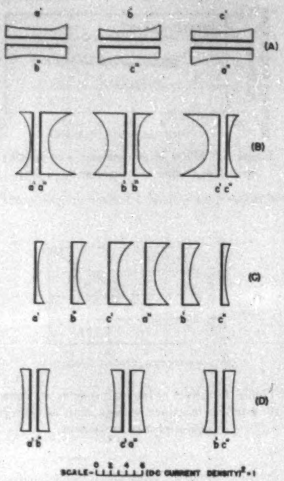


Figure 8. Current density squared in typical arrangements

A. Paired-phase type 3
$d=6$ inches $s={}^5/_8$ inch
B. Laminated
$d=2{}^5/_8$ inches $s={}^5/_8$ inch
$w=6{}^5/_8$ inches
C. Interlaced
$d=2{}^1/_4$ inches $w=11{}^1/_4$ inches
D. Paired-phase type 1
$d=5{}^1/_4$ inches $s={}^5/_8$ inch
$w=12{}^3/_8$ inches

the current distribution is similar in all paired-phase arrangements. There is a marked contrast in this respect between paired-phase and conventional arrangements. It is notable that the phase angle of the current density in the paired-phase arrangements is more uniform within each bar, and the net bar currents are more symmetrically disposed than in the other types of conductor arrangements.

The essence of paired-phase performance is that the currents in each close-spaced pair of bars are essentially equal and opposite. That is, each of the three phase currents (a, b, and c) divides into two component bar currents (a' and a'', b' and b'', c' and c'') which are nearly equal in magnitude and which differ in phase angle by nearly 60 degrees, so that the currents in each close-spaced pair of bars (such as a' and b'', or b' and c'', or c' and a'') differ in phase angle by nearly 180 degrees. The currents in each close-spaced pair are thus essentially similar to the currents in an isolated single-phase transmission system, even though each conductor of the pair is connected in parallel with another conductor in a different pair. This similarity suggests that the paired-phase arrangement may be analyzed by considering each close-spaced pair as a single-phase circuit.

The single-phase transmission system of two wide flat bars closely spaced face to face carrying equal and opposite currents has been analyzed both experimentally and mathematically.[9–12] It has been shown to be a very efficient conductor arrangement. The current density is very uniform, and consequently the a-c resistance is low. For instance, an isolated one-fourth- by four-inch copper bar will have an a-c to d-c resistance ratio of approximately 1.16, but the resistance of a bar of the same size with a similar return conductor five-eighths-inch away, center to center, is less than 1.02. The reactance of such a close-spaced pair is also very small. This means not only low-reactance voltage drop but small external magnetic field. In other words, the magnetic fields of the two equal and opposite currents cancel or neutralize each other. This explains the small interpair proximity effect and the negligible effect of magnetic or metallic surroundings upon either reactance or energy losses in the paired-phase arrangement.

The single-phase pair of closely spaced flat bars has many advantages over other low-loss single-phase arrangements such as concentric tubes. These advantages include the full exposure of both bars for heat dissipation, joining, support, insulation, and tap-offs. This single-phase arrangement has not been used generally in the past, but the recent advent of suitable commercial designs has led to the adoption of this arrangement for the service of large single-phase loads, with very satisfactory results.[13,14]

It is certainly unexpected and seems almost paradoxical that a most efficient method of transmitting large low-voltage polyphase currents should be one wherein the power is, in effect, transmitted single phase; but a comparison of paired phasing with conventional methods, considering all electrical, thermal, and structural factors, indicates that this is indeed true.

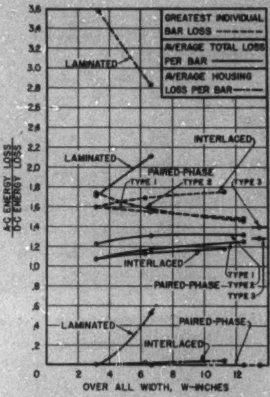


Figure 9. Analysis of energy losses

Loss in individual bar measured by exploring wires. Average total loss measured by wattmeter. Housing loss is difference between average total loss and average bar loss measured by exploring wires (not shown)

The beneficial results of the approximately equal and opposite currents in each close-spaced pair are further emphasized by the consideration of a condition where such a current division does not exist. The average ratio of a-c to d-c loss of the three-phase paired-phase arrangements (with larger interpair spacings) approximates 1.30 (see Figure 9), even though the current density within each bar is very uniform, because each bar carries nearly $1/\sqrt{3}$ rather than one half of the phase current. Losses are proportional to the square of the current: $(1/\sqrt{3})^2/(1/2)^2=1.33$. Conventional theory would suggest, therefore, that better performance would be obtained if the current in each bar could be made one half of the phase current, or, in other words, if the phase angle between the two currents in each close-spaced pair could be made 120 degrees instead of nearly 180 degrees, and the phase angle between the two currents of each phase, zero degrees instead of nearly 60 degrees. Such a condition may be produced by making a transposition at the mid-point of a bus-bar run, as shown in Figure 12, wherein each bar is paired with a different phase in each half of the run; for example, the a' bar is paired with a b-phase bar (b'') in the first half of the run and with a c-phase bar (c'') in the second half. The resultant performance is not, however, as might be expected:

Temperature rise	increased 31 per cent
Voltage drop	badly unbalanced and increased 217 per cent
Average energy loss	increased 54 per cent
Housing energy loss	becomes appreciable

In short, such a condition is undesirable, and all the desirable properties of the paired-phase arrangement are lost. This can be explained by the fact that there are no longer equal and opposite currents to cause a cancellation of magnetic effects in each close-spaced pair, and consequently interpair influence become

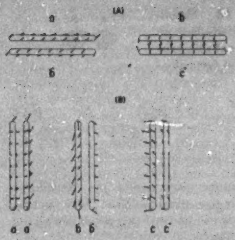


Figure 10. Details of current density

A. Paired-phase type 3 (same dimensions as Figure 8A)

B. Laminated (same dimensions as Figure 8B) Each vector shows magnitude and phase angle of current density at origin of vector

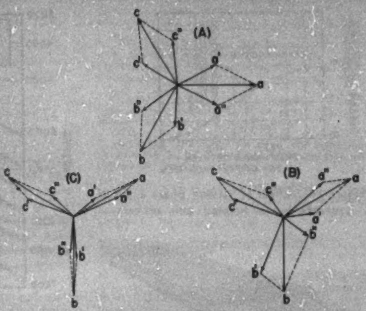


Figure 11 (right). Bar currents and phase currents

A. Paired-phase type 3 (same dimensions as Figures 8A and 10A)

B. Laminated (same dimensions as Figures 8B and 10B)

C. Interlaced (same dimensions as Figure 8C)

a', a", b', b", c', c" indicate bar currents *a, b, c* indicate phase currents

appreciable. This large interpair proximity effect is shown by the distorted current-density measurements in Figure 13.

Varying the phase angle between the currents in an isolated pair of nonenclosed bars further corroborates the effect of transposition. Test results shown in Figure 14 indicate that even under this simplified condition losses are least when the currents differ in phase angle by 180 degrees. Losses when the currents differ in phase angle by 120 degrees (which represents the condition with transposition) are intermediate between the paired-phase condition and the zero-degree phase-angle condition, corresponding to the laminated arrangement.

Calculation of Paired-Phase Bus Bars

The electrical characteristics of the paired-phase arrangement, including voltage drop and current distribution, are readily figured by electrical theory. A mathematical analysis of a simplified paired-phase arrangement is given in the appendix. These calculations indicate that the division of current between conductors in the paired-phase arrangement is not dependent upon any particular conductor shape but largely upon the grouping of the phases into pairs. Of course the conductor shape influences the distribution of current within each conductor. The mathematical analysis confirms experimental results in determining that the currents in each close-spaced pair become more nearly similar to single-phase as the interpair distance is increased and in fact approach the single-phase condition as a limit as the distance becomes infinite. This suggests and tests confirm that entirely satisfactory paired-phase performance may be secured when a steel housing completely surrounds each pair of bars.

The usual method of voltage-drop calculation based upon the assumption of equal and inphase currents in all bars connected to each phase will not give correct results when applied to paired-phase arrangements. Voltage drop can be calculated accurately, however, by application of formula 12 or 15 in the appendix. It has been found that for practical dimensions and purposes voltage-drop characteristics may be figured on the assumption of exactly equal and opposite currents in each pair of bars. Both the equivalent single-phase method of formula 15 and the more exact formula 12 give the correct resistance as well as reactance components of voltage drop. Sample calculations are given in the appendix.

Temperature-rise performance is not so readily calculated. Although the current and, hence, losses in each bar can be determined within a few per cent, heat transfer is so complicated by insulation, by proximity of other conductors, and by the enclosure, that tests using three-phase current are necessary for accurate information. Sufficient empirical data of course make possible the prediction of change in performance produced by minor variation in design.

Commercial Design of Paired-Phase Bus Bars

One practical commercial design embodying the paired-phase principle is shown in Figure 15. The flatwise arrangement was chosen because of the advantageous structural features, among which is the simple overlapping type of joining bus bars, insulation, and housing. This arrangement provides complete access to all bus-bar surfaces for cleaning and inspection before joining and complete accessibility of all bolts for maintenance By mounting the bus bars upon flat compression-type porcelain insulators, which in turn are supported on the housing, maximum mechanical and short-circuit strength are obtained. Also the dielectric strength between closely adjacent bus bars is readily improved by the use of a single flat sheet of insulation to supplement the one-fourth-inch minimum air space.

Tests indicate that this insulation sheet increases the temperature rise by less than two per cent. Other measure.

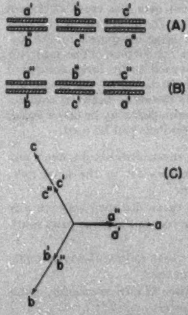


Figure 12 (left). Transposition to change current division

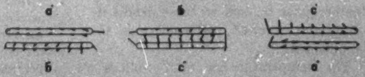
A. Arrangement in one half of bus-bar run

B. Arrangement in other half of bus-bar run. Each bar in A and B insulated except at beginning and end of run, where two bars of each phase are connected together

C. Resulting current division (compare with Figure 11A)

Figure 13. Nonuniform current density produced by transposition

(Compare with Figure 10A)



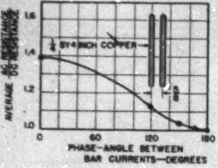


Figure 14. Effect of varying phase angle of currents in an isolated pair of bars

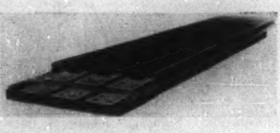


Figure 15. "*Lo-X*" bus duct utilizing paired-phase principle

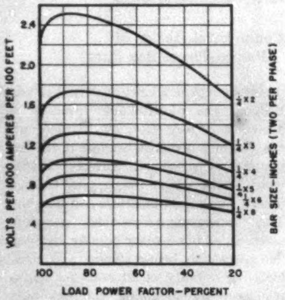


Figure 16. Line-to-line voltage drop of commercial paired-phase bus duct

Unbalance of voltage drop is less than width of curves. Resistance taken at copper temperature of 75 degrees centigrade $s = {}^1/_2$ inch, d = bar width plus 2 inches

ments indicate that with all sizes of bus bars, the hot-spot temperature rise when the entire assembly is mounted edgewise is the same as when it is mounted flatwise; therefore, space limitations will determine the best position for any particular application. The shallow flat shape of the housing proves convenient in installation where close clearances are encountered.

Basic paired-phase principles as just outlined have been utilized in the design of numerous special fittings, such as elbows and tees, transformer taps, load tap-offs, and switchboard connections. Experience has shown that careful attention must be paid to these details if satisfactory over-all performance is to be obtained.

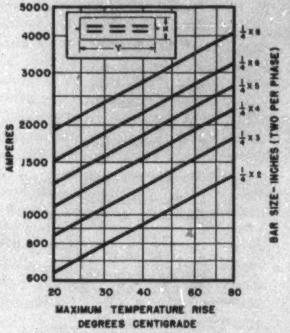


Figure 17. Temperature rise of commercial paired-phase enclosed bus duct indoors in still air

Totally closed steel housing

Bar Size	X	Y
$^1/_4$ by 2	$4^1/_2$	by 14
$^1/_4$ by 3	$4^1/_2$	by 17
$^1/_4$ by 4	$4^1/_2$	by 20
$^1/_4$ by 5	$4^1/_2$	by 23
$^1/_4$ by 6	$4^1/_2$	by 26
$^1/_4$ by 8	$4^1/_2$	by 32

The temperature-rise and voltage-drop characteristics of a complete line of enclosed bus-bar system utilizing the paired-phase principle are given in Figures 16 and 17. Values are all based on actual tests of complete assemblies, including insulation and housing. These curves indicate the excellent performance that can be obtained with the paired-phase arrangement.

Appendix. Mathematical Analysis of Current-Distribution and Voltage Drop in Paired-Phase Bus Bars

Consider Figure 18. The conductors are of arbitrary shape. The current density is assumed uniform over the cross section of each conductor. The length of the circuit is assumed to be large compared to the distances between conductors. The interpair distance is assumed to be large compared to the intrapair distance, so that as an approximation all distances between conductors not in the same pair may be taken equal. The following symbols will be used:

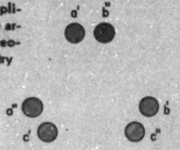
D_d = geometric mean distance between any two conductors not in the same pair, centimeters

D_s = geometric mean distance between any two conductors in the same pair, centimeters

D_r = geometric mean radius of any conductor, centimeters

r = d-c resistance of any conductor, ohms per centimeter

Figure 18. Simplified paired-phase arrangement with geometrical symmetry



E = voltage drop, volts per centimeter, line-to-neutral. (Note that voltage drops in body of this paper are all line to line)

I = current

a', b', c', a'', b'', c'' subscripts refer to conductor quantities. a, b, c subscripts refer to phase quantities.

$\omega = 2\pi$ times frequency in cycles per second

All logarithms are to base ϵ.

Since

$$I_a' + I_a'' + I_b' + I_b'' + I_c' + I_c'' = 0 \quad (1)$$

the voltage drop in phase a may be written: (see reference 19, page 39, equation 55)

$$E_a' = E_a = I_a' r + 2 \times 10^{-9} j\omega [I_a' \log(1/D_r) + I_b'' \log(1/D_s) + (I_a'' + I_b' + I_c' + I_c'') \times \log(1/D_d)] \quad (2)$$

$$E_a'' = E_a = I_a'' r + 2 \times 10^{-9} j\omega [I_a'' \log(1/D_r) + I_c' \log(1/D_s) + (I_a' + I_b' + I_b'' + I_c'') \times \log(1/D_d)] \quad (3)$$

From symmetry

$$E_b = (-{}^1/_2 - j\sqrt{3}/2) E_a \quad (4)$$

$$E_c = (-{}^1/_2 + j\sqrt{3}/2) E_a \quad (5)$$

$$I_b' = (-{}^1/_2 - j\sqrt{3}/2) I_a' \quad (6)$$

$$I_c' = (-{}^1/_2 + j\sqrt{3}/2) I_a' \quad (7)$$

$$I_b'' = (-{}^1/_2 - j\sqrt{3}/2) I_a'' \quad (8)$$

$$I_c'' = (-{}^1/_2 + j\sqrt{3}/2) I_a'' \quad (9)$$

The solution of the above equations gives

$$I_a' = \left[\frac{1}{2} - \frac{\sqrt{3}}{2} \times \frac{10^{-9}\omega \log(D_d/D_s)}{r + 10^{-9} j\omega \log(D_d^2/D_r^2 D_s)}\right] I_a \quad (10)$$

$$I_a'' = \left[\frac{1}{2} + \frac{\sqrt{3}}{2} \times \frac{10^{-9}\omega \log(D_d/D_s)}{r + 10^{-9} j\omega \log(D_d^2/D_r^2 D_s)}\right] I_a \quad (11)$$

$$E_a/I_a = \frac{r}{2} + 10^{-9}\omega \left[\frac{1}{2} j \log \frac{D_d D_s}{D_r^2} + \left(\sqrt{3} \log \frac{D_d}{D_s}\right)\left(\frac{\sqrt{3}}{2} \times \frac{10^{-9}\omega \log(D_d/D_s)}{r + 10^{-9} j\omega \log(D_d^2/D_r^2 D_s)}\right)\right] \text{ ohms per centimeter} \quad (12)$$

An interesting result is obtained by allowing D_d to become indefinitely large:

$$\text{Lim}_{D_d \to \infty} I_a' = (1/\sqrt{3})(\epsilon^{j90°}) I_a \quad (13)$$

$$\text{Lim}_{D_d \to \infty} I_a'' = (1/\sqrt{3})(\epsilon^{-j90°}) I_a \quad (14)$$

$$\lim_{D_d \to \infty} E_a/I_a = (4/3)(1/2)[r+2\times 10^{-9} j\omega \log (D_s/D_r)] \text{ ohms per centimeter} \quad (15)$$

Equations 13, 14, 15, and similar equations for the other phases represent a condition of equal and opposite or single-phase current in each close-spaced pair of bars. The term $[r+2\times 10^{-9} j\omega \log (D_s/D_r)]$ in equation 15 is the impedance of one bar of a close-spaced-pair when carrying single-phase current.

Numerical substitution of practical values in equations 10, 11, and 12 shows that large variations in D_d have little effect upon I_a', I_a'', and E_a/I_a. Therefore, it seems reasonable that these equations may be used even when geometrical symmetry, as assumed in deriving these equations, does not exist.

Numerical Example

Consider a type-3 paired-phase arrangement:

Bar size $^1/_4$ by 4 inches
$r = 9.90\times 10^{-6}$ ohms per foot $= 324\times 10^{-9}$ ohms per centimeter
$s = {}^5/_8$ inch
$d = 6$ inches

From the formula and curves of reference 17

$D_r = 0.2235\,(4+0.25) = 0.950$ inch $= 2.41$ centimeters
$D_s = 1.372$ inches $= 3.49$ centimeters

GMD of two bars in adjacent pairs (6 inches center to center) = 5.75 inches
GMD of two bars in outside pairs (12 inches center to center) = 11.88 inches

No readily usable values for *GMD* of the diagonally located bars are available; therefore, consider diagonal *GMDs* the same as above. As a working approximation the average may be taken as

$$D_d = \sqrt[12]{(5.75)^8(11.88)^4} = 7.33 \text{ inches} = 18.62 \text{ centimeters}$$

From equation 10

$$I_a' = 0.525\epsilon^{j28.4^\circ} I_a$$

From equation 11

$$I_a'' = 0.591\epsilon^{-j24.4^\circ} I_a$$

From equation 12

$E_a/I_a = (6.17+j5.71)10^{-6}$ ohms per foot (line to neutral)

From equation 15

$E_a/I_a = (6.58+j5.65)10^{-6}$ ohms per foot (line to neutral)

By measurement (average of three phases)

$$I_a' = 0.544\epsilon^{j37.3^\circ} I_a$$
$$I_a'' = 0.574\epsilon^{-j23.8^\circ} I_a$$

$E_a/I_a = (6.35+j5.61)10^{-6}$ ohms per foot (line to neutral)

The differences between measured and calculated values are in the directions that would be expected in view of the various assumptions made. The phenomena of Figure 14, neglected in the calculations, makes the phase angle between I_a' and I_a'' more nearly 60 degrees than calculated from equations 10 and 11. The resistance calculated from equation 12 is lower than measured, because slight nonuniform current density in individual bars is neglected. That calculated from equation 15 is higher, because the magnitude of the bar currents is less than the 0.577 I_a assumed by this method. A small correction could be made for nonuniform current density by applying published formulas for skin effect in a single-phase pair of flat bars.[11] Reactances agree within experimental error.

Calculation of impedance by the conventional assumption that $I_a' = I_a''$, vectorially (see reference 17, equations 15 to 20) gives

$E_a/I_a = (4.95+j8.27)10^{-6}$ ohms per foot (line to neutral)
$E_b/I_b = E_c/I_c = (4.95+j16.55)10^{-6}$ ohms per foot (line to neutral)
Average $= (4.95+j13.80)10^{-6}$ ohms per foot (line to neutral)
Measured with $I_a' = I_a''$ (produced by use of a transposition, Figure 12):
Average $= (9.87+j19.02)10^{-6}$ ohms per foot (line to neutral)

The conventional method of calculation, therefore, does not give correct results for ordinary paired-phase arrangements, because current distribution is not as assumed. On the other hand, when bar currents are as assumed, the current density within each bar is not uniform, and, in addition, the steel housing causes extra losses and higher reactance.

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An Electromechanical Calculator for Directional-Antenna Patterns

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Synopsis: An electromechanical calculator has been designed for the rapid solution of directional-antenna patterns. After the desired antenna parameters have been set up, the machine automatically draws the field-intensity curve to any desired scale on a sheet of polar co-ordinate paper and indicates the rms value for drawing a circle of the equivalent nondirectional pattern.

The fundamental design of the calculator does not limit the number of antennas that can be used in the directional array. As the number of antennas is increased, the greater the degree of control is possible to mould the radiation pattern into the desired shape.

Not only is the horizontal directional field-intensity pattern, such as used in broadcast practice, readily available, but the field-intensity contours at any elevation angle can be drawn with equal ease. The vertical radiation characteristics of the several antennas can be selected at will. This makes it possible to explore the solid contour surface of the whole hemisphere in a relatively short time and also determine the total power radiated from the directional-antenna system. The machine is also suitable for calculating the solid contour surface of the radiation pattern from a parallel horizontal antenna array.

TO anyone who has had the task of designing a directional-antenna array, the need of some short cut to reduce the tedium of laborious calculations has been very apparent. To meet this need a number of schemes has been brought forth, ranging from early graphical methods to actual measurements on models, from purely mechanical to purely electrical devices. These devices have varying degrees of usefulness, and all meet to a large degree the requirements of lessening the time required to explore the number of varying parameters necessary to meet some special case.

Extended tables systematizing patterns of two tower arrays have been published.[1-4] These diagrams are very helpful to show trends, even though the designer may in the final analysis have to calculate very precisely the pattern after deciding upon a suitable range of spacings and phasings. The authors felt that a systematization of three tower arrays would be of very real value to the industry. Therefore, in taking up the problem of developing an antenna-pattern calculator, they had in mind not only its immediate use as at first outlined, but also the ultimate accurate and quick development of a great number of patterns. After considering carefully the merits of various methods, it was decided that an electromechanical device could be made to combine the accuracy and simplicity of the mechanical method with the speed and flexibility of the electrical method.

The usual problem in broadcast practice is to mould the radiation pattern into the desired shape to cover the service areas and give the required protection to other radio stations. As a matter of economics, it is desirable to do the job with the minimum number of antennas. With severe requirements, the number of antennas must be increased until the radiation pattern can be moulded into the desired shape. This electromechanical calculator was designed primarily to handle this type of problem. It is not necessary to assume images on the basis of a perfect earth. The only requirement is that the vertical radiation characteristic of the several antennas be known when it is desired to determine the solid contour surface over the whole hemisphere.

It is also possible to solve for the solid contour surface of horizontal radiators located above the surface of the earth. In this case, the horizontal radiation characteristic of the individual antennas in free space must be known. The images in this case must be set up on the machine just as the actual antennas are set up. If the horizontal antennas are perpendicular to and bisected by a vertical plane, then the contours are run at constant angles to this perpendicular plane, just as the various elevation contours are run in the case of vertical antennas. In other words, the contours are run as if the antennas were vertical, and then the contour surface is rotated 90 degrees to locate properly the antennas above the surface of the earth. Only that part of the contour surface is useful that appears above the surface of the earth. For such an array the contour surface will be symmetrical to the vertical plane passing through the center of the antennas.

A more general case is to have horizontal parallel antennas without requiring their centers to bisect a vertical plane. In this case those horizontal antennas and their images, that are not bisected by a vertical plane, must have their phase adjusted by for each contour to correct for the space phase difference. These adjustments of phase are made on the assumption that the observation point is located at infinity, which is the same assumption that is ordinarily used for the antenna spacing. For this more general case the pattern will be unsymmetrical about any vertical plane, and the contour surface will have to be run for both ends of the array.

Design Consideration

In consideration of the problem of systematizing directional-antenna patterns, it is desirable to have considerable flexibility, accuracy, and speed in the machine. Since the time involved to do this job by hand is prohibitive, one can afford to spend considerable time and effort in perfecting a machine to the point that it will give, with sufficient accuracy, this valuable engineering information. The design and development of such a machine, incidentally, involves the solution of a number of rather interesting problems.

The purely mechanical calculator[5,6] has simplicity and accuracy depending upon the preciseness with which the individual mechanical parts are made. However, in its usual forms, it lacks considerable flexibility and speed for calculating all the desired factors involved in a directional pattern. The usual design of these calculators will give the horizontal pattern for three antennas, and, if the antennas are identical, the vertical patterns can be determined without much difficulty. Its operation depends upon one vector being fixed and used as the reference with the vectors of the other two antennas revolving about its ends in the proper fashion. The handling of more than three antennas becomes mechanically involved, because the additional vectors must revolve about the ends of the free-ended vectors. Recording the patterns accurately and automatically with such a device is exceedingly difficult, because the distance between the free-ended vectors must be

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The authors express appreciation to all the operating engineers on the WHK–WCLE staff who have contributed in making the electromechanical calculator a success.

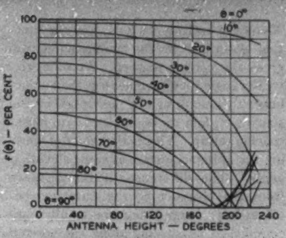


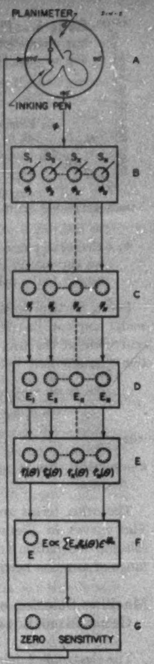
Figure 1. Vertical radiation characteristic as a function of electrical height

measured and recorded accurately at all points. If readings are taken at periodic intervals, these values must again be used in a separate operation to determine the rms value of the pattern. This is a very important quantity if the pattern is to be used for a definite power into the system. Even then there is the question of the power used in the high angle radiation. It usually takes longer to determine the rms value than it does to determine the pattern itself. A more satisfactory method is to use an equation involving Bessel functions to determine the rms value.[7] Appendix I gives the general form of this equation with an illustrative example.

The purely electrical form of calculator may place the directional-antenna pattern on an oscilloscope screen. This scheme is rapid, being visual, can be altered instantaneously by changing the various antenna parameters at will, and is not limited to the number of antennas in the system. The rms value can be read from a meter directly. The limitation of this scheme is primarily lack of accuracy, depending of course upon the electrical design.

The authors have put into operation at the transmitting plant of WHK-WCLE an electromechanical directional-antenna pattern calculator which will automatically draw the horizontal pattern of a four-element array with the possibility of adding as many more elements as desired. This machine will also automatically draw the field-intensity contours at various elevation angles. These data readily yield the vertical pattern in any direction from the antenna system. In addition to this, the machine measures the rms value of the pattern as it is drawn. Hence, the volume of the solid radiation pattern is readily determined by a simple computation. From this information the actual power radiated from the antenna system can easily be computed.

The electromechanical calculator accomplishes its purpose by starting with a simple mechanical system, converting to an electrical system for flexibility, and finally converting back to a mechanical system to draw the curve. The mechanical portion of the calculator consists of a turntable to hold the polar co-ordinate chart paper. This turntable is driven by a variable-speed motor which in turn drives the several mechanical cosine generators. These generators in effect rotate the second, third, and fourth antennas around the first or reference antenna. The mechanical cosine generators are rack-geared to phase-shifting transformers. At this point the mechanical vector is converted into an equivalent electrical vector. All of the electrical vectors can then be added in series and the resultant represents the field intensity in the given direction in space as indicated by the orientation of the turntable carrying the polar co-ordinate chart paper and the elevation angle. An automatic voltagraph converts this electrical magnitude into an equivalent mechanical magnitude to drive the radial inking pen and planimeter. The inking pen draws the pattern while the planimeter measures the rms of the pattern. Usually, after the directional pattern is finished, the rms circle is drawn on the polar co-ordinate chart as additional valuable information.



PLANIMETER
INKING PEN
ϕ
A
B
C
D
E
F
G
S_1 S_2 S_3 S_n
E_1 E_2 E_3 E_n
$f_1(\theta)$ $f_2(\theta)$ $f_3(\theta)$ $f_n(\theta)$
$E \propto \sum E_k f_k(\theta) \epsilon^{j\beta_k}$
E
ZERO
SENSITIVITY

Figure 2. Symbolic representation of the electromechanical antenna-pattern calculator

A—Motor-driven turntable carrying polar co-ordinate chart paper. The orientation of the observer is represented by ϕ

B—Mechanically driven cosine wave generators with adjustments for antenna orientations ϕ_k, spacing S_k and elevation angle θ

$S_k \cos(\phi_k-\phi)\cos\theta$

C—Mechanically driven phase-shifting transformers with adjustments for antenna current phase Ψ_k

$\beta_k=S_k \cos(\phi_k-\phi)\cos\theta\,\Psi_k$

D—Electrical magnitude adjustment of horizontal field intensity $E_k \cdot E_k \epsilon^{j\beta_k}$

E—Electrical magnitude to give vertical radiation characteristic $f_k(\theta)$

$E_k f_k(\theta)\epsilon^{j\beta_k}$

F—Summation of electrical vectors and total electrical magnitude control of field intensity E

G—Voltagraph converts electric field intensity E to an equivalent mechanical magnitude. It is provided with zero and sensitivity adjustments

The output of the voltagraph drives the inking pen to draw the resulting pattern on the polar co-ordinate chart paper. Also, a planimeter is driven to measure the area of the pattern

Generalized Equation

The electromechanical calculator in reality is an equation solver. In this case the equation is of a particular variety, and the machine must be adapted to handle the various parameters in the equation. The method of operation of the machine can best be explained after considering the general nature of the equation to be solved.

For a directional antenna array of n antennas, the generalized equation is

$$\boldsymbol{E}=\sum_{k=1}^{k=n} E_k f_k(\theta)\epsilon^{j\beta_k} \tag{1}$$

where

$\boldsymbol{E}$ = the total effective field-intensity vector for the antenna array with respect to the voltage reference axis

k = the kth antenna in the system

n = the total number of antennas in the complete directional-antenna array

E_k = the horizontal magnitude of the field intensity produced by the kth antenna

$f_k(\theta)$ = vertical radiation characteristic of the kth antenna (unity along the horizon)

θ = elevation angle of the observation point P measured up from the horizon in degrees

$\epsilon^{j\beta_k}$ = unity vector determining the direction of the voltage vector of the kth antenna

ϵ = 2.718 . . . the base of natural logarithms

$j=\sqrt{-1}$ imaginary j operator

$$\beta_k=S_k \cos(\phi_k-\phi)\cos\theta+\Psi_k \tag{2}$$

= phase relation of the voltage (or current) in the kth antenna with respect to the voltage reference axis

S_k = electrical length of the spacing of the kth antenna from the space reference point

ϕ_k = true horizontal azimuth, orientation of the kth antenna, with respect to the space reference axis

ϕ = true horizontal azimuth angle of the direction to the observation point P (measured clockwise from true north)

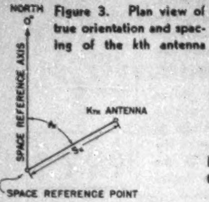


Figure 3. Plan view of true orientation and spacing of the kth antenna

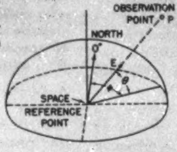


Figure 4. Vector representation of the total field intensity in space

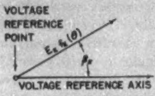


Figure 5. Vector representation of the field intensity of the kth antenna

Ψ_k = electrical phase angle of the voltage (or current) in the kth antenna with respect to the voltage reference axis

For a vertical antenna having a sinusoidal current distribution with a current node at the top, the vertical radiation characteristic takes on the form

$$
{}_k(\theta)=\frac{\cos(h\sin\theta)-\cos h}{(1-\cos h)\cos\theta} \qquad (3)
$$

where

h = electrical height of the antenna in degrees.

The other terms are as defined above. The curves in Figure 1 represent the solution of equation 3 over the most useful range of antenna heights.

Machine Solution of Generalized Equation

The electromechanical antenna-pattern calculator solves the generalized equation as shown symbolically in Figure 2. The position of the observer, the placement of the antennas, and the phase of antenna currents determine the direction of the various antenna vector voltages. These adjustments are all mechanical. The magnitude of these vectors are controlled electrically and are added for the total effective field intensity. This field intensity is then converted into an equivalent mechanical magnitude to drive the inking pen and planimeter.

The direction of the inking pen from the zero-degree reference orientation of the turntable represents the orientation ϕ of the observer from the antenna system. The turntable which is driven by a variable-speed motor is geared through clutches to all the cosine wave generators. The orientation of the kth antenna, for example, from true north is accomplished on the calculator by locking the turntable so that the inking pen is true north. The kth antenna cosine-wave-generator clutch is then released and turned to give the desired orientation ϕ_k in degrees as indicated on the orientation dial. The clutch is then locked into position. The spacing S_k of the kth antenna measured from the space reference point is an amplitude adjustment, measured in electrical degrees on the spacing arm, of the cosine wave generator. These two adjustments locate the kth antenna as illustrated in Figure 3. The adjustments thus far give the function $S_k\cos(\phi_k-\phi)$ for the horizontal pattern.

The cosine wave generator drives a phase-shifting transformer through a rack and gear. This mechanism is so proportioned that if the observer moves around the antenna, by rotating the turntable, the phase of the current in the kth antenna will shift the same number of electrical degrees that the number of electrical degrees from the observer to the antenna changes. The phase of this current is zero in the kth antenna when the voltage vector lies along the voltage reference axis. For this condition the distance from the observer to the space reference point is equal to the distance from the observer to the kth antenna. The phase Ψ_k is adjusted by a clutch arrangement on the shaft of the phase-shifting transformer. Any electrical phasing measured in degrees can be set mechanically on the phase dial associated with this clutch. With this adjustment added, the function becomes $S_k\cos(\phi_k-\phi)+\Psi_k$ for the horizontal pattern.

All of the mechanically adjusted parameters are represented in equation 2. Along the horizon $\cos\theta=1$, and the function described in the above paragraph gives the correct answer. At some elevation angle $\cos\theta$ becomes less than unity; hence this factor can be multiplied by the spacing S_k and an equivalent new closer spacing set up. This is the effect of viewing the antenna system at some elevation angle. The difference in the distance from the observer to the antennas becomes less; hence, the same result is accomplished by staying along the horizon and placing the antennas closer together. Directly over the antenna system all antennas are the same distance; hence, the product $S_k\cos\theta$ must go to zero. The function for any direction is $S_k\cos(\phi_k-\phi)\cos\theta+\Psi_k$.

The direction of the observation point P in terms of the true orientation ϕ and elevation angle θ is illustrated in Figure 4. The magnitude of the total effective field intensity E is represented along the direction of the line from the space reference point toward the observation point P for convenience in drawing the polar charts. The field intensities at a given elevation angle can be duplicated with an equivalent horizontal pattern, using closer antenna spacing and modified field intensities.

The field intensity of the kth antenna as given in equation 1 is vectorially represented in Figure 5. The phase angle β_k is controlled by the mechanical adjustments which in turn controls the phase of the output voltage of the phase-shifting transformer. The magnitude of the output voltage E_k is controlled by a potentiometer. See Figure 6. For field-intensity magnitudes at elevation angles, this value must be controlled in accordance with the vertical radiation characteristic $f_k(\theta)$ of the antenna. For convenience in running contours at the various elevation angles, vertical-radiation-

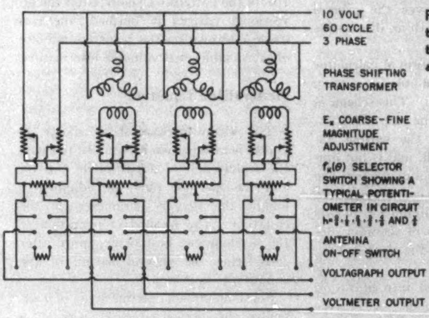

10 VOLT
60 CYCLE
3 PHASE
PHASE SHIFTING TRANSFORMER
E_k COARSE-FINE MAGNITUDE ADJUSTMENT
$f_k(\theta)$ SELECTOR SWITCH SHOWING A TYPICAL POTENTIOMETER IN CIRCUIT
ANTENNA ON-OFF SWITCH
VOLTAGRAPH OUTPUT
VOLTMETER OUTPUT

Figure 6 (left). Electric circuit for magnitude control and addition of antenna voltages

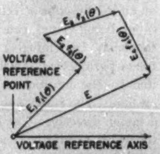


Figure 7. Summation of four antenna field-intensity voltage vectors

characteristic potentiometers are used for antennas having heights in multiples of an eight-wave length plus one position which is adjustable to any other desired vertical pattern. These vertical-pattern potentiometers speed up the work and lessen the chance of making errors. The electric circuit involving these parameters is illustrated in Figure 6.

The total effective field-intensity vector $\boldsymbol{E}$ is the summation of all the vector voltages from the various antennas. In this calculator these voltages are added in series. The resulting summation of the vector voltages for a four-element directional-antenna system is illustrated in Figure 7. Only the angles of these vectors vary as the orientation from the antenna system changes. Both the angle and magnitude changes as the elevation angle θ is varied.

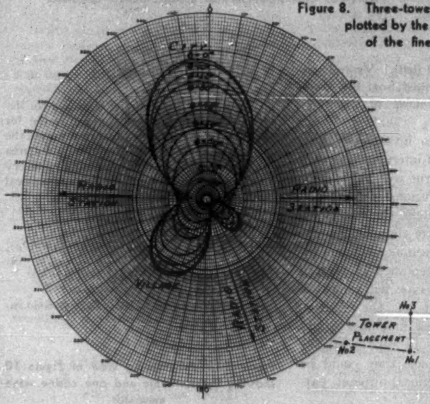


Figure 8. Three-tower solid-contour surface, plotted by the machine, on one sheet of the finely engraved polar coordinate chart paper

Directional - antenna parameters

$\phi_2=276$ degrees
$S_2=176$ degrees
$\Psi_2=296$ degrees
$E_2=115$
$E_1=168$
$\phi_3=0$ degrees
$S_3=100$ degrees
$\Psi_3=218$ degrees
$E_3=115$
$E_{rms}=190$
$\phi_4=$ —
$S_4=$ —
$\Psi_4=$ —
$E_4=$ —
$E_{max}=378$
$h_1=h_2=h_3=\frac{\lambda}{4}$

The magnitude E of the total effective field-intensity vector $\boldsymbol{E}$ is now converted into an equivalent mechanical magnitude in order that the radial drawing pen can be driven in such a way as to produce the field-intensity pattern on the polar coordinate chart paper. A rapid self-balancing precision potentiometer circuit was developed to accomplish this result. Adjustments are provided to make zero voltage correspond to the center of the polar co-ordinate chart paper. A sensitivity adjustment makes it possible to select any reasonable scale for a given output voltage magnitude E. Usually in systematizing patterns, the magnitude of the maximum lobe of the pattern is adjusted to fall on a given circle. This usually requires an adjustment of the sensitivity for each pattern.

Enough power is supplied from the balancing mechanism to drive, in addition to the pen, a planimeter which rolls and slides on the chart paper and measures the area of the chart. By means of a table the operator can immediately determine the rms value of the pattern and draw this circle on the chart with the drawing pen controlled by a single antenna which is nondirectional.

Speed

The machine is driven by a variable-speed motor. For simple patterns the speed can be increased somewhat and the machine still maintain the required accuracy. For ordinary systematization work it takes from three to five minutes to complete a pattern. This involves setting the parameters and drawing the pattern and the rms circle. The solid contour surface can be investigated and recorded on the chart paper in less than an hour. This usually involves a total of nine contours placed three to a chart.

Accuracy

The machine is operated from a 220-volt three-phase 60-cycle source at the present time. Line-voltage fluctuations are the greatest source of error. The machine depends upon these voltages remaining constant while the pattern is being run. Originally it was planned to provide an independent source of power free from fluctuations, but this had to be abandoned for the duration of the war.

A number of different makes of phase-shifting transformers was tested, and the best, which are being used in the present machine, showed a maximum error of three per cent. This appeared to be satisfactory for the systematization work. If desired on special jobs, checks can always be made, with a vacuum-tube voltmeter, of all the voltages in the system, to increase the magnitude accuracy to that of the voltmeter. The error in phase angle checks within one degree.

In the systematization work, the antenna orientations ϕ_k are advanced approximately one degree to compensate for the lag in the drawing pen. This lag is a function of the speed of the turntable; hence, by slowing the turntable down it can be practically eliminated.

The precision potentiometer can be adjusted so that the mechanical magnitude tracks with the electrical magnitude and the balance point is always within one per cent of the full-scale value.

The polar planimeter used to measure the area of the charts varies somewhat in its readings but usually can be relied upon to within one per cent.

Chart Paper

Two varieties of special chart paper have been prepared. One is a simple ten-inch circle with crosslines printed on a 12-by-12-inch sheet and is used primarily for the systematization work. Three such charts with patterns were tacked to cover lid of the calculator illustrated in Figure 10. The patterns are all for three tower antennas. The numbers at the bottom completely specify parameters.

The finely engraved chart paper was also prepared on 12-by-12-inch sheets but can be trimmed down to 11 by 12 inches to fold into a standard notebook of $8^1/_2$ by 11 inches. The engravings on this chart consist of a complete circle ten inches in diameter, divided with radials to single degrees and numbered every ten degrees clockwise from the top. The ordinates are divided into ten parts to the inch with heavy markings every half inch. Figure 8 illustrates this chart paper with the horizontal and eight elevation angle contours crowded on one sheet. The legend "Directional-Antenna Parameters" in the upper right-hand corner specifies all the conditions imposed upon the system. The legend in the lower right-hand corner specified the firm name, date, manufacturer and pattern number. This chart paper is now available to the public[8] with or without the blocks in the corners in a light or heavy weight of paper. The chart paper is printed with a brown ink for ease in making photostats or blueprints.

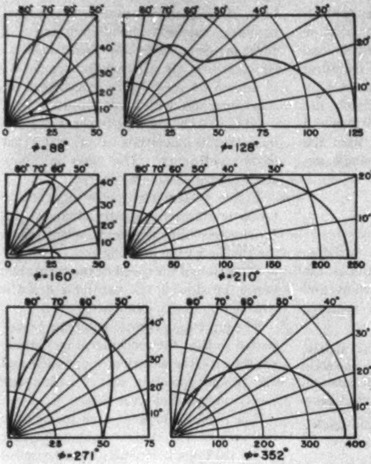


Figure 9 (left). Vertical patterns plotted from the elevation contours of Figure 8

Figure 10. Electromechanical antenna-pattern calculator

Vertical Patterns

Vertical-plane radiation patterns as shown in Figure 9 are readily obtained from the elevation contours in Figure 8. The vertical patterns are important when considering sky-wave interference problems between radio stations.

Illustrations

The complete machine is shown in Figure 10. The magnitude controls are on the left-hand panel, followed by the turntable and cosine wave generator panels. A supply of chart paper is held in a pocket under the table, convenient to the operator. The master-control power switch is on the left-hand front leg. The voltagraph-control amplifiers and power transformers are located on the shelf under the table. The whole unit is on wheels and is provided with a dust cover lid.

Figure 11 is a close-up view showing details of the turntable and one cosine wave generator.

Conclusions

After three months of continuous operation, the machine has proved to be a speedy means of securing directional-antenna patterns. The systematization work which is progressing steadily will be available at a later date.

The machine is extremely valuable when one is searching for a directional-antenna pattern to meet certain specific requirements. Many patterns can be drawn quickly to investigate thoroughly the regions of interest. The systematization of patterns will aid materially in finding these regions of interest.

With care of operation, the results are believed more accurate than can be expected from most directional-antenna installations. The acceptance of such a machine by the profession will remove a considerable amount of the routine work. A few of these machines would perhaps be adequate to supply all the needed patterns; hence, they could be used to offer a directional-antenna pattern service.

Appendix I

The general form of the equation for determining the root-mean-square value for the directional-antenna pattern involves the product of the field intensity of each antenna multiplied by the field intensity of every antenna in the system. In mathematical form this can be written

$$E_{rms}=\sqrt{\sum_{p=1}^{p=n}\sum_{q=1}^{q=n}E_pE_q\cos(\Psi_p-\Psi_q)J_0(S_{pq})} \tag{4}$$

where

E_{rms} = root-mean-square effective field intensity

Figure 11. A close-up view of Figure 10 showing the turntable and one cosine wave generator

p = the pth antenna in the system
q = the qth antenna in the system
n = the total number of antennas in the complete directional antenna array
E_p = horizontal magnitude of the field intensity produced by the pth antenna
E_q = horizontal magnitude of the field intensity produced by the qth antenna
Ψ_p = electrical phase angle of the voltage (or current) in the pth antenna with respect to the voltage reference axis
Ψ_q = electrical phase angle of the voltage (or current) in the qth antenna with respect to the voltage reference axis
$J_0(S_{pq})$ = Bessel function of the first kind and zero order of the spacing S_{pq}, in radians, between the pth and qth antennas

In this equation when $p=q$, the terms are simply the square of the antenna field intensity, because the cosine and Bessel function terms each reduce to unity. For example in a three-tower array the application of equation 4 results in

$$E_{rms}=\sqrt{\sum_{p=1}^{p=3}\sum_{q=1}^{q=3}E_pE_q\cos(\Psi_p-\Psi_q)J_0(S_{pq})}$$

$$=\sqrt{\begin{array}{l}E_1^2+E_2^2+E_3^2\\+2E_1E_2\cos(\Psi_1-\Psi_2)J_0(S_{12})\\+2E_1E_3\cos(\Psi_1-\Psi_3)J_0(S_{13})\\+2E_2E_3\cos(\Psi_2-\Psi_3)J_0(S_{23})\end{array}} \qquad (5)$$

If the parameters for the three-tower directional-antenna array illustrated in Figure 8 are substituted in equation 5 the horizontal rms field intensity becomes

$$E_{rms}=\sqrt{\begin{array}{l}168^2+115^2+115^2\\+2(168)(115)\cos(0\text{–}296)\times\\\qquad\qquad J_0(3.07)\\+2(168)(115)\cos(0\text{–}218)\times\\\qquad\qquad J_0(1.745)\\+2(115)(115)\cos(218\text{–}296)\times\\\qquad\qquad J_0(3.37)\end{array}}$$

$$=\sqrt{\begin{array}{l}168^2+115^2+115^2\\+2(168)(115)(0.4384)(-0.2829)\\+2(168)(115)(-0.7880)(0.3719)\\+2(115)(115)(0.2079)(-0.3587)\end{array}}$$

$$=191$$

This shows an error of approximately one half of one per cent in the value 190 measured by the planimeter on the machine.

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Mobile Capacitor Units for Emergency Loading of Transformers in Open Delta

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Synopsis: Loading of closed delta banks of substation transformers to a point approaching safe thermal limit results in severe overloading of two transformers as well as decided unbalance in voltage and current when operated in open delta as a result of the failure of one transformer. By the use of mobile capacitor units with capacitors arranged in two unequal groups, the load on two transformers in open delta is reduced, regulation improved, voltage and current are balanced within operating limits, and with the aid of fans, the top oil temperatures—and consequently, the copper temperatures—are kept within safe limits. This permits proper loading of transformers in closed delta, a relatively short interruption to service or reduction in load upon failure of one transformer in a bank, and a reduction in number of transformers required for emergency use.

To maintain satisfactory service on a power system, mobile equipment such as railway-car-mounted substations[1] and highway trailer portable transformers have been found very effective. These portable transformers were designed to be used in case of the failure of more than one transformer in a bank. To maintain service in event of failure of a single transformer in a delta-delta bank, transformers of sufficient capacity were used to permit open delta operation without materially damaging the insulation from overheating during the repair of the damaged unit. This method should no longer be used, for it is now imperative that substation transformers be loaded to a point approaching the safe thermal limit. With transformers so fully loaded, the failure of one transformer in a bank will necessitate an interruption to service, or reduction in load for sufficient time to permit moving and installing additional transformer capacity, since operation in open delta would load the two transformers to 173 per cent of the closed delta loading. Operating at this overload would result in severe overheating of the two transformers and a very pronounced unbalance in voltage and current. Figure 1 shows calculated regulation of a typical bank of transformers in open delta with the two transformers operating at 173 per cent of rating. In addition to time and effort required to move and install emergency transformer capacity, this method requires a considerable stock of transformers of assorted sizes, voltages, and impedances on a system having a number of primary voltages. For example, the primary voltage of substation transformers on the system with which the authors are associated includes: 100 kv, 44 kv, 33 kv, 22 kv, 13.2 kv, 6.6 kv, and 2,400 volts.

An alternate method consists of open delta operation with the use of mobile capacitors and fans. As the power factor of load on substation transformers of the system on which these capacitors are used is ordinarily between 70 per cent and 80 per cent, the regulation of transformers operating at approximately 173 per cent of normal load in open delta is large and badly unbalanced. This excessive regulation and unbalance is corrected by connecting capacitors across the phase lagging the open delta and across the open delta. Figure 2A shows the calculated regulation of a bank of two 750-kva, 35-degree, 44,000–2,400-volt transformers both with and without capacitors and

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with a maximum load of 3,125 kva, the power factor being 74 per cent. Figure 2B shows transformer currents in each case. Capacitance of 1,080 kva was used across the phase lagging the open delta and 540 kva across the open delta. In making these calculations it was assumed for all loads that the load currents were equal in each phase and 120 degrees apart. The voltages on the source side of the transformers were assumed to be equal for each phase and 120 degrees apart. The power factor of 74 per cent was taken with respect to the balanced primary voltages. The per cent voltages shown on Figure 2A are the calculated values on the load side of the transformer bank. The curves of voltage are therefore somewhat distorted from what they would be in an actual application, because the primary and secondary voltages both would be somewhat unbalanced. In this application, however, when using capacitors, the voltage on both the primary and secondary windings of the transformer bank will be balanced at a load of 2,500 kva. This is shown for the load voltages on the curves of Figure 2A.

The loading of transformers in open delta with capacitance properly applied may be illustrated by the following example:

> Assume a safe loading of three transformers to be 300 kva, the power factor, 75 per cent, and the kilowatts, 225. The load on each transformer, 100 kva, 75 kw.
>
> In open delta, the loading of each transformer will be 173 kva, less a small decrease in output due to regulation.
>
> If capacitance should be added to bring the power factor of each transformer to 100 per cent, the loading of each would be 112.5 kva, plus the capacitor losses, which are small.
>
> Should capacitance be added to bring the power factor of the transformer which is connected across the phase lagging the open delta to 90 per cent, the load on each transformer will average approximately 125 kva, plus capacitor losses. This is still a satisfactory condition, as the regulation will be good, and transformers can be cooled with fans.

Method of Connecting Capacitance

Tests were made to determine the most effective application of capacitance to the phases of an open delta bank with loads of various power factors. A capacitance ratio of two parts across the phase lagging the open delta to one part across the open delta gave satisfactory results. This connection is shown schematically in Figures 4, 5, and 6, and is used in all mobile units. Thirty-six 15-kva capacitors rated 600 volts with 2,400-volt insulation to case were installed in each mobile unit. The groups of 12 and 24 capacitors were arranged for connection in parallel for 600-volt operation and four series-parallel for 2,400-volt operation. A mobile unit of this size was determined upon as a single unit is applicable to banks with loads as small as 300 kva and to banks with loads as large as approximately 1,500 kva, there being some 400 banks within these limits on the system on which these mobile capacitors are used. As can be seen in Figure 3, a unit of this capacity is comparatively small and can be handled easily.

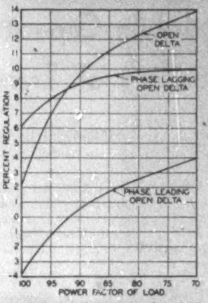


Figure 1. Calculated phase regulation of open-delta bank with transformers loaded to 173 per cent of rating

Transformer rating:
300 kva, 35 degrees centigrade rise
Voltage, 40,000/575

Regulation:
100 per cent power factor—0.94 per cent
90 per cent power factor—3.70 per cent
80 per cent power factor—4.65 per cent

Figure 3. Mobile capacitor unit attached to towing vehicle

Tests and Operating Results

Data were obtained from capacitors in use as well as in tests in which one transformer was disconnected to give open delta connection. Figure 4 shows test on bank of 300 kva, 35-degree rise, 44,000/575-volt transformers with a peak load of over 1,300 kva and with complete readings taken with loads between 1,100 and 1,200 kva. Temperature curves show transformer temperatures while operating in closed delta, self-cooled, and in open delta, fan-cooled, with capacitors. One set of readings was taken with 25 per cent of the capacitors disconnected, but with the same ratio of 2 to 1 applied to the phase lagging the open delta and across the open delta. This test, as well as data in Figures 5 and 6, illustrates the wide range permissible in amount of capacitance used. Figure 5 shows test of bank

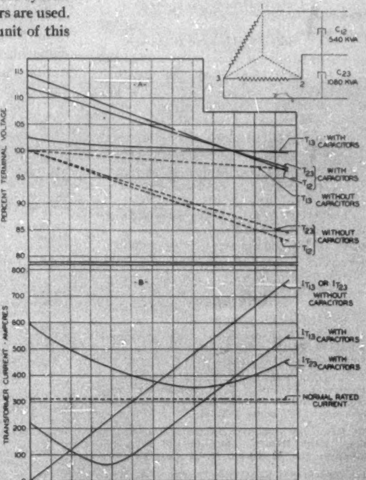


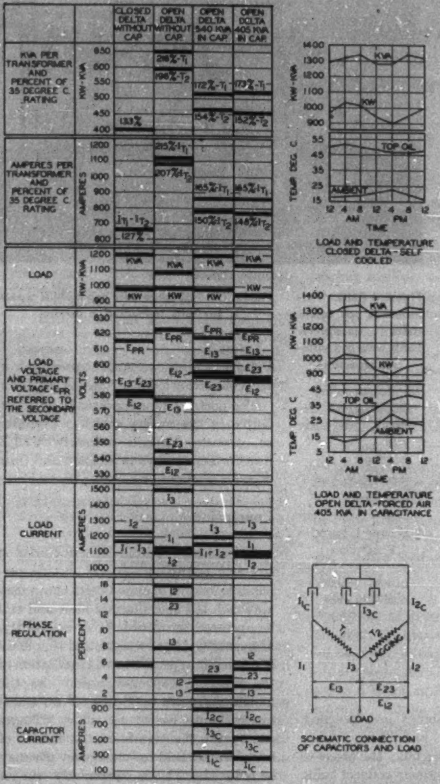
Figure 2. Calculated performance of transformers in open delta with and without capacitors

Transformer rating:
750 kva, 35 degrees centigrade rise
Voltage: 44,000—2,400

Regulation:
100 per cent power factor—0.94 per cent
90 per cent power factor—3.60 per cent
80 per cent power factor—4.75 per cent
74 per cent power factor—5.00 per cent

Power factor of load:
74 per cent

Per cent impedance:
0.9+j6.5 on 750 kva



LOAD AND TEMPERATURE CLOSED DELTA - SELF COOLED

LOAD AND TEMPERATURE OPEN DELTA - FORCED AIR 405 KVA IN CAPACITANCE

SCHEMATIC CONNECTION OF CAPACITORS AND LOAD

Figure 4. Performance of transformer bank in closed delta and in open delta with and without capacitors

Transformer rating:
300 kva, 35 degrees centigrade rise
Voltage: 40,000/575

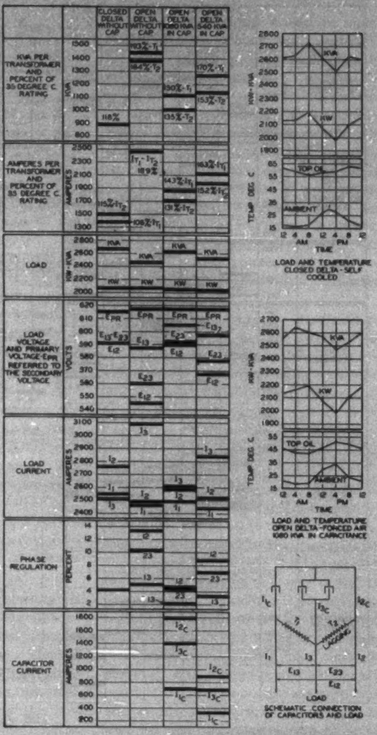
Regulation:
100 per cent power factor—0.94 per cent
90 per cent power factor—3.70 per cent
80 per cent power factor—4.65 per cent

of 750 kva, 35 degrees centigrade, 44,000/575-volt transformers. In this case, capacitance of 1,080 kva was used for one set of readings and 540 kva for another, in each case the ratio of 2 to 1 across the lagging phase and open delta being retained. Figure 6 shows results with one 250-kva transformer of a bank of three out of service, a load of over 600 kw, with a normal power factor of approximately 80 per cent being carried by the remaining two transformers with a 540-kva capacitor unit. As in Figures 4 and 5, additional readings were taken with various amounts of capacitance.

The preceding figures illustrate the flexibility of the mobile capacitor units, for they are applicable to all open delta banks, regardless of primary voltage or impedance providing the secondary voltage is common to one of the two voltages for which these units are arranged. Provision is made to permit a portion of the 540 kva in each mobile unit to be disconnected quickly, still retaining the 2 to 1 ratio, if less capacitance is required for smaller loads, and, as shown in Figure 7, two or more units may be operated in parallel to take care of larger loads.

Description of Mobile Unit

As the total weight of the unit is only 4,600 pounds, it can be handled easily by car or light truck. Electric brakes are provided with brake control on a flexible cable, so each capacitor unit can be towed by any one of a number of vehicles.



LOAD AND TEMPERATURE CLOSED DELTA - SELF COOLED

LOAD AND TEMPERATURE OPEN DELTA - FORCED AIR 1080 KVA IN CAPACITANCE

SCHEMATIC CONNECTION OF CAPACITORS AND LOAD

Figure 5. Performance of transformer bank in closed delta and in open delta with and without capacitors

Transformer rating:
750 kva, 35 degrees centigrade rise
Voltage: 44,000/600

Regulation:
100 per cent power factor—0.81 per cent
90 per cent power factor—2.73 per cent
80 per cent power factor—3.39 per cent

The enclosing housing is provided with doors on each side, one side providing access to a 50,000-kva automatic oil circuit breaker which controls the capacitor circuit. The door on the opposite side opens to a terminal board by means of which the capacitors may be grouped for either 600- or 2,400-volt operation. One multitap current transformer on each of the two groups of capacitors provides current indication and tripping energy.

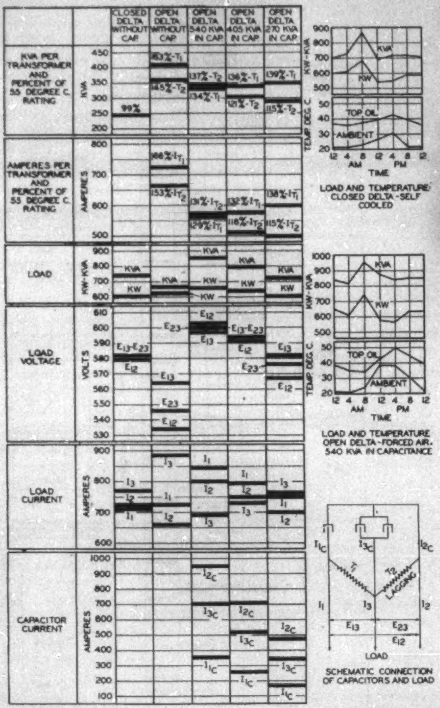


Figure 6 (left). Performance of open-delta transformer bank with and without capacitors

Transformer rating:
250 kva, 55 degrees centigrade rise
Voltage:
13,200/575

Regulation:
100 per cent power factor—1.2 per cent
80 per cent power factor—4.05 per cent

Ventilation of the capacitors and breaker is provided by openings in the bottom and at each side of the enclosing housing. Natural circulation of air was found insufficient to maintain capacitor temperatures within desired limits so a 12-inch exhaust fan was installed in one side of each mobile unit. This provided ample circulation for properly cooling the equipment.

The entrance bushings are stenciled, "Common," "High," and "Low," referring to the transformer bus; the phase to which the two transformers connect being the "Common," the phase having the higher voltage as referred to the "Common" connecting to the "High" terminal, and the remaining phase to the "Low" terminal. With this marking correct connection of the portable unit to the transformer bus can be made quickly. Voltage readings are not required for correct connection if phase rotation is known

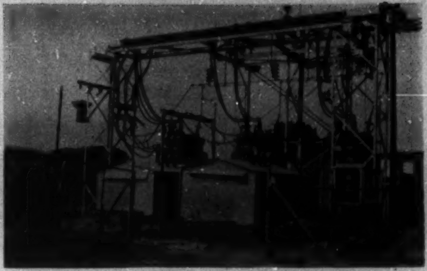


Figure 7. Test setup with two mobile capacitor units connected in parallel

The portable fans can usually be transported in the towing vehicle. Two fans per transformer will ordinarily cool a transformer 500 kva or smaller, and three or more are used on a larger transformer.

Conclusion

By locating portable capacitor units at strategic points on a power system, permissible loading of closed delta banks may approach the safe thermal limit and still be operated in open delta in emergency. The mobility, light weight, and simplicity of connection, permit rapid installation of the portable capacitor units. As the mobile units are applicable to all transformer banks having secondary voltages common to the capacitors, the number of transformers held for emergency use may be reduced. This is particularly true on systems having a multiplicity of primary voltages.

Reference

1. A 100,000-Volt Portable Substation, Charles I. Burkholder and Nicholas Stahl. AIEE Transactions, volume 34, 1915, pages 279–92.

Equivalent Ambient Temperatures for Loading Transformers

W. C. SEALEY
MEMBER AIEE

Synopsis: Safe overloads for transformers can be determined by a modification of the American Standards Association rule for overloading to permit the use of an equivalent ambient temperature, which will result in the same loss of life as for continous operation at 30 degrees centigrade ambient and full load.

Based on the rule that aging of insulation doubles for each eight degrees centigrade increase in temperature, methods are developed for calculating the equivalent ambient temperature based on United States Government Weather-Bureau records. The equivalent ambient temperature for each month is calculated as the sum of the greatest recorded monthly mean temperature plus a daily correction factor based on the greatest recorded daily range, plus a monthly correction factor based on the greatest recorded monthly range. Monthly equivalent ambient temperatures so obtained can be used in combination with load curves to determine the permissible safe load for a transformer for a given locality.

Usefulness of Equivalent Ambient

TRANSFORMERS are given a name-plate rating in kilovolt-amperes which is the load they will carry under specified conditions. They may be safely overloaded beyond their name-plate rating under certain conditions, such as an ambient temperature below 30 degrees centigrade. In most locations ambient temperatures below 30 degrees centigrade exist for at least a portion of the time.

One method of conserving material is to utilize it to the best advantage so that greater output can be secured from a given quantity. Following these principles, transformers now in service can be used to carry loads greater than their name-plate rating, and new transformers with smaller name-plate ratings than the kilovolt-amperes to be carried can be secured for new applications.

Principles Governing Equivalent Ambient

Under normal operating conditions a transformer ages because of the effect of temperature in deteriorating the insulation. The rate at which the life of the insulation is consumed increases as the operating temperature is increased. An increase in ambient temperature of the cooling medium results in a corresponding increase in the operating temperature of the transformer and corresponding increased rate of loss of life. Similarly, a decrease in the ambient temperature results in a decrease in the rate of loss of life.

Under actual operating conditions, the ambient temperature varies continuously. According to the American Standards Association Guides for Operation "oil-immersed self-cooled transformers may be overloaded continuously one per cent of rated kilovolt-amperes for each degree centigrade that the daily temperature of the cooling medium is below 30 degrees centigrade."

Accordingly, if the transformer is operating at constant load throughout the year, the maximum load will be based on the hottest day of the year. The use of an equivalent ambient temperature permits fuller utilization of transformer capacity, taking advantage of the fact that operation at low temperatures at lower rate of loss of life tends to compensate for operation for a time at high temperatures with faster rate of loss of life of insulation.

The equivalent ambient temperature is defined as the temperature which, if maintained continuously during the period of time under consideration, would result in the same aging of insulation as that occurring under the actual ambient temperatures. The equivalent ambient temperature may be used with the following modification of the American Standards Association rule for overloading transformers: "For low ambient temperatures the continuous kilovolt-ampere loading may be increased one per cent for each degree centigrade that the equivalent ambient temperature is below 30 degrees centigrade for self-cooled transformers."

The calculations for determining the equivalent ambient temperature are based on the laws governing the aging of transformer insulation. If the rate of aging were proportional to the temperature, the equivalent temperature would be equal to the average temperature. Actually, however, aging proceeds more rapidly at higher temperatures. The rate of aging doubles for each increase in temperature of five to ten degrees centigrade. Figure 1 is a curve which shows the rate of loss of life as a function of operating temperature, assuming the rate of aging doubles for each increase in temperature of eight degrees centigrade. Since the rate of aging shown in Figure 1 is relative, the ordinates (degrees centigrade) can represent actual degrees centigrade or degrees centigrade above any assumed base temperature.

Where the ambient temperature changes slowly, it has the effect of increasing the operating temperature of the transformer a corresponding amount. For example, if the ambient temperature is 40 degrees centigrade instead of 30 degrees centigrade, the operating temperature of the transformer will be ten degrees higher.

The method of calculating the equivalent ambient temperature is illustrated by Figure 2. Curve *A* is the recorded ambient temperature plotted versus time. Using curve *A* as a base and the relation between temperature and rate of loss of life in Figure 1, curve *B* is plotted, representing the rate of loss of life in per cent of the rate of loss of life at ten degrees centigrade corresponding to points on curve *A*. The degrees centigrade of each point of curve *A* above the assumed base temperature of 10 degrees centigrade are used to find the relative rate of aging for that point from Figure 1, for plotting on curve *B* of Figure 2. The area under curve *B* represents the total loss of life, and the average value of the ordinates of curve *B* is the average rate of loss of life for the period considered. The equivalent ambient temperature is the sum of the base temperature of ten degrees centigrade plus the temperature from Figure 1 corresponding to the average loss of life in Figure 2, since, if maintained constantly, this temperature would result in the same total loss of life as results from curve *A*.

Integration in exactly this manner could be used for determination of the equivalent temperature.

However, when using available data, the problem is considerably simplified if the year is considered on a monthly basis and the characteristics of a typical day for each month are determined. The monthly equivalent ambient temperatures are obtained by combining the daily equivalent ambient temperatures for the days of the month. In turn, the

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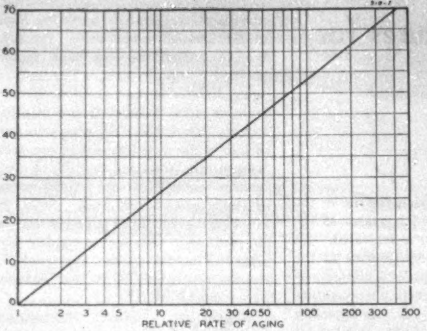
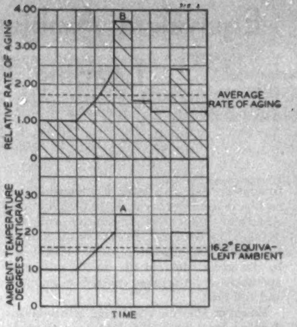


Figure 1 (left). Curve showing how rate of aging varies with the temperature according to the eight degrees centigrade rule

Figure 2 (right). Curves illustrating aging calculations



annual equivalent ambient temperature is obtained by combining the monthly equivalent ambient temperatures for the months of the year.

General Curves

Using the principle outlined in connection with Figures 1 and 2, it is possible to calculate the equivalent ambient for temperature variations with time for any shape of wave. A convenient manner of expressing the data so obtained is as a correction to be added to the average value of the wave or as a different correction to be subtracted from the maximum value of the wave. Curves showing the corrections to be added to the average value to obtain the equivalent temperature of a sine wave and of a wave in which the temperature varies as a straight line function of the time are shown in Figures 3 and 4. Figure 5 shows similar corrections to be subtracted from the maximum value of square top waves. These corrections in each case were obtained by calculating the equivalent temperature in the same manner as the equivalent temperature of Figure 2 was obtained.

Daily Temperatures

The records of the weather bureau for the location considered can be used to determine average daily temperatures and the variation during the day of the hourly temperature.

Transformers do not respond immediately to changes in ambient temperature because of the thermal capacity of their copper, iron, and oil. The effect of the thermal capacity of a transformer is twofold. It tends to smooth out and average the effect of rapid changes in temperature. In addition, it decreases the effect of the change so that a change of ten degrees in ambient during a 24-hour period, for example, will result in only an eight-degree change in the hot-spot temperature of the transformer. The mathematical relations governing this effect are derived in Appendix I where it is shown that the variation in temperature of a typical transformer caused by sine-wave variation in the ambient temperature during one day is 80 per cent of the ambient-temperature variation. The effect of the higher harmonics is smoothed out and decreased more than the temperature variation because of the fundamental frequency of one cycle per day.

Figure 6 shows typical daily variation in temperatures taken from reports of the United States Weather Bureau. This curve is of the general form of a sine wave with superimposed higher frequency harmonics of smaller magnitude.

The daily range in temperature may vary from a few degrees to as much as 50 degrees Fahrenheit or more. An examination of weather-bureau records of the range in temperatures indicates that the greater ranges may occur in connection with the highest temperature of the month or with the lowest or intermediate temperatures. Consequently, the assumption is made that every day has the same daily range in temperature. It will be noted that curve *A* of Figures 3 and 4 gives practically the same results as taking 80 per cent of the range and applying it to curve *B*. Consequently, for correcting for daily range the correction from curve *A* corresponding to the daily range may be added to the average temperature. The value of daily range to be chosen depends upon the conservatism desired. In order to insure conservative values of ambient temperature, the use of the greatest recorded daily range in temperature is recommended.

(The American Standards Association rule takes into account daily variations, and strictly speaking no correction would be made for them. However, in the interest of greater exactness, it seems logical to make this correction in order to obtain a constant base for the life of insulation instead of a varying base depending upon the locality.)

Monthly Temperatures

If the mean daily temperatures are averaged over a period of years, the average daily temperatures will vary with time in accordance with a sine wave. Figure 7 shows values for Milwaukee over a period of 55 years. The average maximum, the average mean, and the average minimum daily temperatures vary as sine waves with superimposed higher harmonics of smaller magnitude.

On the average the temperature during an individual month will follow a portion of a sine wave. Since there are 12 months

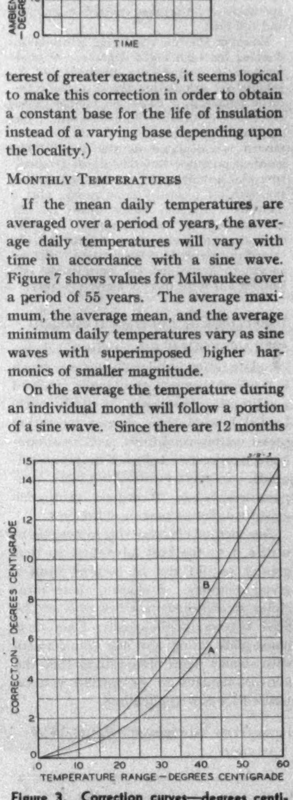


Figure 3. Correction curves—degrees centigrade

Curve *A*—Correction to be added to average value of straight-line variation of temperature to obtain equivalent temperature

Curve *B*—Correction to be added to average value of sine wave of temperature to obtain equivalent temperature

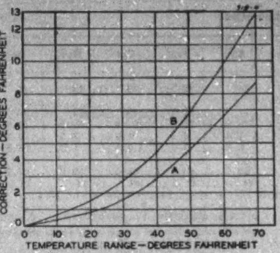


Figure 4. Correction curves—degrees Fahrenheit

Curve *A*—Correction to be added to average value of straight-line variation of temperature to obtain equivalent temperature

Curve *B*—Correction to be added to average value of sine wave of temperature to obtain equivalent temperature

in a year, and the period of yearly variation of temperature is 12 months, each month will extend over $^1/_{12}$ of 360 degrees, or 30 degrees. Since the curvature of each part of a sine wave is different, the actual shape of the ideal monthly variation will be different for each period of the year. If a straight line is drawn through the maximum value for the month and the average value for the month, a very close approximation will be obtained for periods of the order of one month. The portion of the curve which has the greatest effect on the aging is the portion above the average value, and it will be apparent by inspection of the straight lines *A*, *B*, and *C* of Figure 8 that very little error is introduced by assuming a straight-line variation of temperature with time during a month, if the straight line passes through the maximum value of temperature and the mean value of temperature located at the halfway point of the month. Figure 9 shows typical values of the variation of daily temperature during the month. The mean temperatures plotted are arranged in their order of magnitude with the hottest day plotted at the left and progressing to the coldest day at the right. These curves are in accordance with the ideal variation in that a straight line drawn through the maximum daily mean temperature for the month and the monthly mean temperature results in a very good approximation of the monthly equivalent. Consequently, curve *A* of Figures 3 and 4 can be used to obtain the correction for variation of daily mean temperatures during the month. The range in temperatures to be used depends upon the conservatism desired. In order to insure conservative values of ambient temperature, the use of the greatest monthly range of daily mean temperatures ever recorded for that month is recommended. This figure is not always readily available. In such cases one of the other approximations given in Appendix III can be used.

Figure 10 shows a nontypical variation of temperatures during a month in which the temperatures vary considerably from a straight line. However, even here the straight line drawn through the maximum and the mean temperatures represents an equivalent temperature that is not greatly in error, and such a wave shape is certain to have an average value that is lower than the greatest average value ever recorded for the month, because of the effect of the low values in reducing the average. This is illustrated by the figure where the actual mean was 20 degrees Fahrenheit for the month and greatest mean recorded for that month is 34 degrees Fahrenheit.

Reports of the weather bureau show the mean temperature for each month over a large number of years. By selecting the greatest mean temperature which has been recorded for that month, a value of average temperature is obtained which is not likely to be exceeded in the future years or in any case by more than a small amount. Using these values, the equivalent monthly ambient is equal to the greatest mean temperature ever recorded for the month, plus the daily correction corresponding to the greatest daily range ever recorded for the month from curve *A* of Figures 3 or 4 plus the monthly correction corresponding to the greatest monthly range in daily mean temperature ever recorded for the month from curve *A* of Figures 3 or 4.

After equivalent temperatures for each of the 12 months of the year have been calculated, the equivalent temperature for any period of time involving two or more months can be determined readily by use of Figure 5. By successively combining months, the equivalent temperature for the complete year may be obtained with accuracy, without making assumptions as to the variation of the average monthly temperatures throughout the year.

Source of Data

The most convenient source of temperature data to be used in calculating the equivalent ambient is the annual and monthly meteorological summaries of the location concerned which can be obtained in each case from the local weather bureau. A list is given in Appendix II of the cities which have United States Weather Bureaus which issue meteorological summaries. In addition, weather bureaus can furnish certain specific data on request. An example of such data is the greatest daily range in temperature which has ever occurred during a given month since an early date.

Recommended Rules for Calculating Equivalent Ambient

1. The daily correction is read from curve *A*, Figure 3 or 4, corresponding to the value of the greatest recorded daily range in temperatures for the month.

2. The monthly correction is read from curve *A*, Figure 3 or 4, corresponding to the greatest recorded range of daily mean temperatures for the month. (See appendix for approximations.)

3. The equivalent monthly temperature equals the maximum mean temperature for the month considered, for any year plus the

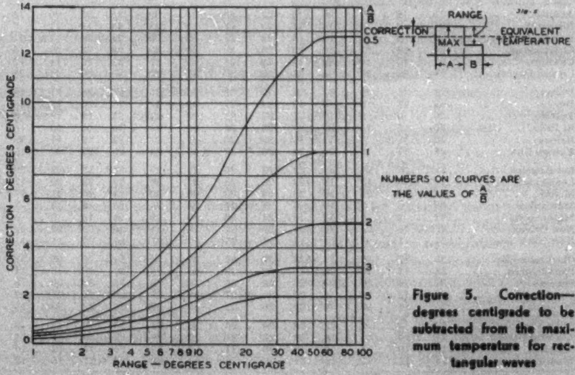


Figure 5. Correction—degrees centigrade to be subtracted from the maximum temperature for rectangular waves

Table I. Typical Calculation of Equivalent Ambient Temperatures

<table>
<tr><th>Month</th><th>Jan</th><th>Feb</th><th>Mar</th><th>Apr</th><th>May</th><th>June</th><th>July</th><th>Aug</th><th>Sept</th><th>Oct</th><th>Nov</th><th>Dec</th></tr>
<tr><td>Greatest daily range</td><td>36</td><td>44</td><td>46</td><td>40</td><td>39</td><td>34</td><td>29</td><td>30</td><td>42</td><td>33</td><td>43</td><td>43</td></tr>
<tr><td>Greatest monthly range in daily temperature</td><td>49</td><td>51</td><td>50</td><td>44</td><td>39</td><td>34</td><td>30</td><td>27</td><td>34</td><td>40</td><td>51</td><td>46</td></tr>
<tr><td>Greatest monthly mean</td><td>46.2</td><td>42.2</td><td>52.5</td><td>59.2</td><td>67.9</td><td>78</td><td>80.6</td><td>79.2</td><td>75.4</td><td>62.8</td><td>50.9</td><td>44.6</td></tr>
<tr><td>Daily correction</td><td>2.3</td><td>3.5</td><td>3.8</td><td>2.8</td><td>2.7</td><td>2.0</td><td>1.5</td><td>1.6</td><td>3.1</td><td>1.9</td><td>3.3</td><td>3.3</td></tr>
<tr><td>Monthly correction</td><td>4.4</td><td>4.8</td><td>4.6</td><td>3.5</td><td>2.7</td><td>2.0</td><td>1.6</td><td>1.3</td><td>2.0</td><td>2.8</td><td>4.8</td><td>3.8</td></tr>
<tr><td>Equivalent ambient (degrees Fahrenheit)</td><td>52.9</td><td>50.5</td><td>60.9</td><td>65.5</td><td>73.3</td><td>82.0</td><td>83.7</td><td>82.1</td><td>80.5</td><td>67.5</td><td>59.0</td><td>51.7</td></tr>
<tr><td rowspan="6">Equivalent ambient (degrees centigrade)</td><td>12</td><td>10</td><td>16</td><td>19</td><td>23</td><td>28</td><td>29</td><td>28</td><td>27</td><td>20</td><td>15</td><td>11</td></tr>
<tr><td colspan="2">11.1</td><td colspan="2">17.6</td><td colspan="2">25.8</td><td colspan="2">28.5</td><td colspan="2">24.1</td><td colspan="2">13.2</td></tr>
<tr><td colspan="4">14.9</td><td colspan="4">27.2</td><td colspan="4">20</td></tr>
<tr><td colspan="8">22.7</td><td colspan="4">20</td></tr>
<tr><td colspan="12">21.9</td></tr>
<tr><td colspan="2">11.1</td><td colspan="3">19.6</td><td colspan="3">28.3</td><td colspan="4">20</td></tr>
<tr><td>C</td><td colspan="5">16.8</td><td colspan="3">28.3</td><td colspan="4">20</td></tr>
<tr><td>Permissible per cent overload <i>C</i></td><td colspan="5">13.2</td><td colspan="3">1.7</td><td colspan="4">10</td></tr>
<tr><td>Equivalent ambient—<i>D</i></td><td colspan="5">15.9</td><td colspan="3">28.3</td><td colspan="2">24.1</td><td colspan="2">15.9</td></tr>
<tr><td>Permissible per cent overload <i>D</i></td><td colspan="5">14.1</td><td colspan="3">1.7</td><td colspan="2">5.9</td><td colspan="2">14.1</td></tr>
</table>

daily correction from rule 1 plus the monthly correction from rule 2.

4. The equivalent temperatures for periods of two or more months may be obtained by combining equivalent monthly temperatures using the curves of Figure 5 and subtracting the correction of Figure 5 from the higher equivalent ambient monthly temperature.

APPLICATION OF RULES FOR CALCULATION OF EQUIVALENT AMBIENT

Table I shows how these rules may be applied to calculating equivalent ambient temperature by their application to the Philadelphia district. The data were obtained from the local weather bureau. The first three lines of Table I contain data obtained from the weather bureau in Philadelphia. The fourth and fifth lines are the daily and monthly corrections from Figure 4 corresponding to the temperature ranges in lines 1 and 2, respectively. The equivalent ambient (degrees Fahrenheit) is the sum of the preceding three lines. The equivalent ambient in degrees Fahrenheit is converted into degrees centigrade. In the following lines, the months are combined into longer periods, using the curves of Figure 5. The period considered is represented by the arrows. The equivalent yearly ambient is 21.9 degrees centigrade. According to the modification of the American Standards Association rule, a transformer can carry 30−21.9 or 8.1 per cent additional load continuously in this locality; that is, a load of 108 per cent of the name-plate rating. This is shown by curve *C* of Figure 11. Curves *A* and *B* are determined similarly.

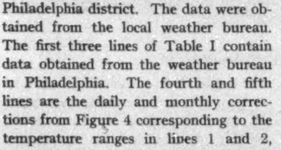


Figure 6. Typical variation of ambient temperature during 24-hour period

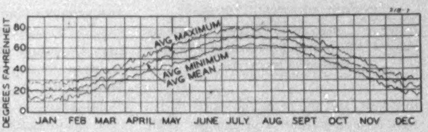


Figure 7. Typical variation of average daily temperatures over a period of 55 years

APPLICATION OF EQUIVALENT AMBIENTS TO LOAD CURVES

Any yearly load curve can be plotted in per cent of the maximum load as shown by Figure 12, curve *B*. By plotting the permissible load for the same periods of time as the load blocks on the same scale, an indication will be obtained as to which curve best fits the load scale. In some cases a recombination of curves will allow the load curve to be approached more closely. It should be noted that only one of the safe load curves may be used. That is, any curve has the same loss of life as any other curve, but it is not permissible to use part of one curve for a portion of the year and part of another curve for another portion of the year. It is, of course, permissible to divide the permissible load curves into blocks of uneven periods of time, as is shown by curve *C* of Figure 12. This is accomplished by the use of the curves of Figure 5 in connection with the equivalent monthly ambient temperatures.

Example. Assume the maximum kilovolt-amperes of load curve *B* of Figure 12 is 8,000 at 80 per cent daily

Table II. Equivalent Ambient Temperatures (Degrees Centigrade) for Various Localities

	Annual	Jan	Feb	Mar	Apr	May	June	July	Aug	Sept	Oct	Nov	Dec
Atlanta	25	16	17	20	23	27	30	30	30	31	25	20	18
Bismarck	21	3	8	14	17	23	27	32	28	25	19	11	7
Boston	20	8	7	11	15	22	27	28	26	24	20	13	11
Chattanooga	24	16	17	20	22	26	30	30	30	28	25	20	15
Chicago	21	10	10	13	18	23	28	28	27	25	19	14	11
Corpus Christi	28	22	22	25	27	28	30	31	31	30	29	25	24
Dallas	28	25	20	23	26	28	32	33	33	31	26	21	18
Denver	23	12	13	15	19	23	27	30	31	26	20	15	14
Detroit	20	6	8	11	17	23	27	28	27	25	18	13	8
El Paso	26	15	15	20	24	28	32	32	31	30	25	20	16
Helena	20	10	8	11	16	20	25	29	27	22	17	11	10
Kansas City	25	11	13	20	21	26	32	33	33	28	22	18	13
Los Angeles	24	20	20	22	22	23	25	27	27	27	25	22	20
Louisville	24	16	13	18	22	26	28	31	30	29	22	20	17
Miami	28	27	25	26	28	28	29	30	30	29	28	27	27
Milwaukee	20	6	6	10	16	21	27	28	26	23	18	13	7
Minneapolis	21	2	3	12	18	24	28	30	29	24	18	9	6
New Orleans	27	23	23	24	25	28	31	31	31	30	29	23	22
New York	21	11	9	14	18	23	28	29	27	26	20	14	11
Oklahoma City	25	17	19	21	23	26	32	32	33	31	26	20	13
Philadelphia	22	12	10	16	19	23	28	29	28	27	20	15	11
Phoenix	31	18	20	23	28	32	36	39	38	35	30	22	18
Pittsburgh	23	12	12	15	20	25	27	30	29	28	21	17	12
St. Louis	25	14	13	20	22	26	32	33	32	27	22	20	15
Salt Lake City	22	6	9	13	18	24	29	32	31	26	19	11	8
San Francisco	19	14	15	18	19	20	21	20	20	22	20	18	14
Seattle	18	10	11	16	17	20	21	23	22	20	17	12	10
Washington, D. C.	23	12	12	18	21	26	28	31	30	28	21	16	12

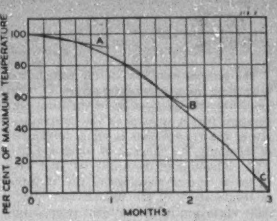


Figure 8. Comparison of assumed straight lines with ideal sine waves for variation during a monthly period throughout the year

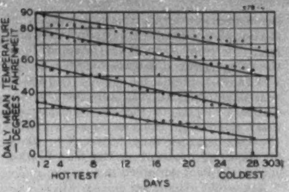


Figure 9. Typical variation of daily mean temperatures during a month arranged in the order of magnitude for four different months

The straight lines are drawn through the maximum and the mean temperature points

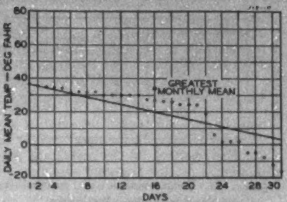


Figure 10. Nontypical variation of daily mean temperature during a month

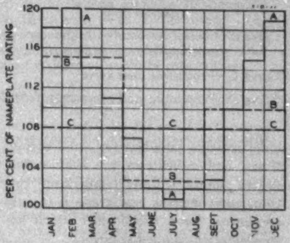


Figure 11. Safe overload curves for Philadelphia

Curve *A*—Monthly curve
Curve *B*—Curve in four month blocks
Curve *C*—Constant load curve

load factor. The location is Philadelphia. The correction for load factor is six per cent according to the interim report on transformer overloading and its effect is considered separately. Curves *A* and *C* are calculated as shown on Table I. If the unit is loaded by curve *A*, it will be noted that curves *A* and *B* are closest from January to April. Consequently, the transformer kilovolt-amperes required will be

$$\frac{8{,}000}{1.08}=7{,}400 \text{ kva}$$

If curve *C* is used, the transformer kilovolt-amperes required is 8,000/1.13 = 7,070 kva. An examination of Table I shows that the equivalent ambient for November and December is lower than for the critical period, January to May. Consequently, greater overloads can be carried during the critical period if November and December are added to the block of the critical period to obtain curve *D* of Figure 12. If curve *D* is used, the transformer kilovolt-amperes required is

$$\frac{8{,}000}{1.14}=7{,}020 \text{ kva}$$

The ordinates of curve B' are equal to the ordinates of curve *B* multiplied by 8,000/7,020. A still further correction of six per cent can be made for the load factor so that the transformer kilovolt-amperes required =

$$\frac{7{,}020}{1.06}=6{,}620 \text{ kva}$$

That is, 6,620 transformer kilovolt-amperes is required to supply a maximum load of 8,000 kva, which varies during the year in accordance with load curve *B* with an 80 per cent daily load factor.

Equivalent Ambient Temperatures for Various Localities

Table II shows calculated values of monthly and annual equivalent ambient temperatures for various localities in the United States. These values can be used for calculating permissible overloads for transformers.

By reference to this table, it will be noted that there is much greater variation in winter monthly ambient temperatures among localities than there is variation in the annual ambient temperatures or summer ambient temperatures. For example, during January the variation is from two degrees centigrade for Minneapolis to 25 degrees centigrade for Dallas, compared to an annual variation of from 20 degrees centigrade for Boston to 31 degrees centigrade for Phoenix. During July the variation is only from 28 degrees centigrade for Boston to 39 degrees centigrade for Phoenix and 33 degrees centigrade for Dallas. By the eight-degree rule, the higher temperatures which occur have a much greater effect on the value of the equivalent ambient than the lower temperatures. The variation among localities of the highest temperatures is less than the variation among localities of the lowest temperatures. Consequently, the greatest variation among localities in safe loads will be during the winter months, not during the summer nor on an annual basis.

These facts, in combination with the example in Table I, illustrate the value of using monthly equivalent ambient temperatures in combination with a yearly load curve for obtaining maximum output from a given transformer.

Accuracy of Method

The greatest values ever recorded have been used for monthly mean temperatures, monthly range in temperatures, and daily range in temperatures. Typically shaped curves for the manner of variation of temperature with time have been used. The eight-degree rule has been assumed for the variation of the life of insulation. The life base used is that of a transformer operated continuously at full load and 30 degrees centigrade ambient.

For most years the actual values of every one of the factors of mean temperature and ranges will be less than the values assumed. In addition, if one factor is low, it will tend to compensate for higher values of other factors. Consequently, the loss of life in nearly all years will be considerably less than the assumed value. The fact that low loss of life in one year will offset a high loss of life in another year provides an additional factor of safety. Such conservative relations are desirable, since, if the equivalent ambient is to be safe for any year, it must be safe for the worst year which is liable to occur.

It is, of course, possible to have an unusual combination of mean temperature, temperature ranges, and shape of variation curves such as to produce a higher loss of life in a month than the base value, but such an occurrence is extremely unlikely. However, should such a combination occur, its effect will be to exceed the base by only a small amount and very infrequently, and therefore it can be considered in the same class as an emergency overload.

The eight-degree rule has been found to be approximately correct by several

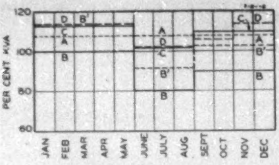


Figure 12. Determination of permissible overload for a given load curve

Curve *A*—Permissible constant load curve

Curve *B*—Actual load curve in per cent of maximum load

Curve *B'*—Actual load curve in per cent of transformer kilovolt-amperes

Curve *C*—Permissible load curve

Curve *D*—Permissible load curve

independent investigators at various times, and indications are that a five-degree rule or a lower-temperature rule than eight degrees centigrade is unduly pessimistic for a transformer which has its insulation in good condition.

Conclusions

1. The use of equivalent ambient temperatures permits the load capacity of transformers to be utilized more fully, so that greater loads may be carried on present transformers, or so that smaller transformers may be secured for a given load.

2. The equivalent ambient temperature can be calculated from the records of the local weather bureau, as the monthly mean temperature plus a correction for daily range in temperature plus a correction for a monthly range in temperature.

3. Values of equivalent ambient temperatures which permit overloads to be carried with demonstrable safety can be obtained.

4. The rules given in the text permit the determination of the equivalent ambient temperatures and corresponding overloads with a limited amount of calculation.

5. The monthly equivalent ambient temperatures for each month of the year and combinations of them are more generally useful than the annual equivalent ambient for determining permissible overloads on transformers.

Appendix I. Effect of the Thermal Capacity of a Transformer in Modifying Daily Temperature Variation

In the portion of the heat circuit shown in Figure 13, heat is assumed to be flowing from point *A* to point *B* through the heat-resistive material (shown shaded) with a resistance *R* where *R* equals the temperature difference required to cause one watt of power to flow through the thermal circuit under steady conditions. Let the thermal capacity be *K*, where *K* is the watt-hours per degree centigrade thermal capacity of the body considered. Let W equal the watts heat flow from *A* to *B*, t = time and T_B = the temperature difference between *A* and *C*. In this circuit the energy flowing in time dt is equal to Wdt, and the heat absorbed is equal to the thermal capacity times the change in temperature or KdT_x. (T_x = temperature drop across *K*.) Since all the heat is absorbed, the heat flowing equals the heat absorbed or

$$Wdt = KdT_x$$

Solving for T_x,

$$T_x = \frac{1}{K}\int Wdt$$

The temperature drop across the resistance *R* is equal to *RW*. Therefore, the total temperature drop in the circuit is equal to

$$T = RW + \frac{1}{K}\int Wdt$$

The equation of an electric circuit containing resistance and capacitance only is given as follows:

$$e = rI + \frac{1}{C}\int Idt$$

By inspection, it is evident that *T* in the equation corresponds to *e*, *R* corresponds to *r*, W corresponds to *I*, and *K* corresponds to *C*. It is evident that thermal capacity bears the same relation to the heat circuit that the electrical capacity has in the electric circuit.

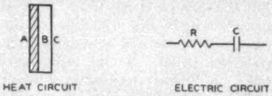


Figure 13. Heat circuit and analogous electric circuit

The reactance to heat flow = *X*, and, by analogy to the electric circuit, is equal to $1/2\pi fK$, where f = the frequency. For a typical transformer with a 45 degrees centigrade oil rise for continuous operation, a thermal time constant of three hours, and a watts loss of *W*, the resistance to heat flow in the circuit, $R = 45/W$.

Normally, the ambient temperature of a transformer varies according to a daily cycle with a high point during the day and a minimum during the night, according to a sine law with sometimes superimposed higher-frequency variations (Figure 4). Consequently, f can be assumed to be $= 1/24$ cycles per hour.

The thermal time constant is the time required to reach the ultimate temperature, if all heat were absorbed by the thermal capacity of the transformer. Consequently, for a typical transformer when the thermal time constant is 3, the value of

$$K = \frac{W \times 3}{45} = \frac{W}{15}$$

$$X = \frac{1}{2\pi fK} = \frac{1}{2\pi \times \frac{1}{24} \times \frac{W}{15}} = \frac{180}{\pi W}$$

$$\frac{X}{R} = \frac{180}{\pi W} \times \frac{W}{45} = 1.27$$

The temperature rise of the transformer is equal to the thermal drop across *X*. Consequently, the proportion of the total temperature which appears as the temperature rise of the transformer is equal to the sine of the angle, the tangent of which $= X/R = 1.27$. The sine of the angle $= 0.79$. That is, for a typical transformer the variation in temperature of the transformer due to daily variation in the ambient temperature is 80 per cent of the ambient variation.

Appendix II. Locations of Government Weather Bureaus from Which Meteorological Summaries May Be Obtained

ALABAMA
- Birmingham
- Mobile
- Montgomery

ARIZONA
- Flagstaff
- Phoenix
- Yuma

ARKANSAS
- Fort Smith
- Little Rock

CALIFORNIA
- Eureka
- Fresno
- Los Angeles
- Redding
- Sacramento
- San Diego
- San Francisco

COLORADO
- Denver
- Grand Junction
- Pueblo

CONNECTICUT
- Hartford

DELAWARE
- Delaware Breakwater

DISTRICT OF COLUMBIA
- Washington

FLORIDA
- Apalachicola
- Jacksonville
- Key West
- Miami
- Tampa

GEORGIA
- Atlanta
- Augusta
- Macon
- Savannah

IDAHO
- Boise
- Pocatello

ILLINOIS
- Chicago
- Peoria
- Springfield

INDIANA
- Evansville
- Fort Wayne
- Indianapolis

IOWA
- Charles City
- Davenport
- Des Moines
- Dubuque
- Sioux City

KANSAS
- Concordia
- Dodge City
- Kansas City
- Topeka
- Wichita

KENTUCKY
- Louisville
- Pikeville

LOUISIANA
- New Orleans
- Shreveport

MAINE
- Eastport
- Portland

MASSACHUSETTS
- Boston

MICHIGAN
- Alpena
- Detroit
- Escanaba
- Grand Rapids
- Houghton
- Marquette

MINNESOTA
- Duluth
- Minneapolis

MISSISSIPPI
- Meridan
- Vicksburg

MISSOURI
- Columbia
- Kansas City
- St. Joseph
- St. Louis
- Springfield

MONTANA
- Billings
- Havre
- Helena
- Kalispell
- Miles City

NEBRASKA
- Lincoln
- North Platte
- Omaha
- Valentine

NEVADA
- Reno
- Tonopah
- Winnemucca

NEW HAMPSHIRE
- Concord

NEW JERSEY
- Atlantic City

NEW MEXICO
Albuquerque
Roswell

NEW YORK
Albany
Buffalo
Canton
New York
Rochester
Syracuse

NORTH CAROLINA
Asheville
Charlotte
Hatteras
Raleigh

NORTH DAKOTA
Bismarck
Devils Lake
Fargo
Williston

OHIO
Cincinnati
Cleveland
Columbus
Toledo

OKLAHOMA
Oklahoma City

OREGON
Portland
Roseburg

PENNSYLVANIA
Erie
Harrisburg
Philadelphia
Pittsburgh

SOUTH CAROLINA
Columbia
Charleston

SOUTH DAKOTA
Huron
Rapid City

TENNESSEE
Chattanooga
Knoxville
Nashville

TEXAS
Abilene
Amarillo
Austin
Brownsville
Corpus Christi
Del Rio
Dallas
El Paso
Fort Worth
Galveston
Houston
San Antonio

UTAH
Salt Lake City

VERMONT
Burlington

VIRGINIA
Lynchburg
Norfolk
Richmond

WASHINGTON
Seattle
Spokane
Walla Walla

WEST VIRGINIA
Columbia
Charleston

WISCONSIN
Green Bay
La Crosse
Madison
Milwaukee

WYOMING
Cheyenne
Lander
Sheridan
Yellowstone Park

Appendix III

In many cases local weather bureaus have compiled from their records values of the greatest range in daily mean temperature during each month in any one year. When this figure is not available, some other approximation of the monthly range may be used in its place. One convenient base for such an estimate is the greatest daily range ever recorded during the month. An examination of temperature records will show that the greatest range in monthly mean temperature is approximately the same as the greatest recorded daily range for the month. The correlation is not accidental. If the phenomena concerned are investigated, it will be apparent that large daily changes are due to changes in the wind direction from a cooler region to a warmer region or vice versa combined with the effect of sunshine and precipitation, both factors being reduced in effect by the thermal capacity of the earth. Monthly variations are due to the same causes, and a correlation between the variation of daily mean temperature during the month and the maximum variation of temperatures during the day would be expected. For calculation several alternatives are possible: 1. To assume that the monthly range of daily mean temperature is equal to the greatest daily range for that month. 2. More conservatively, the monthly range may be assumed to be equal to the greatest daily range ever recorded for that locality. Still another approximation for the monthly range may be based on the greatest daily mean temperature ever recorded in that month and the greatest monthly mean temperature ever recorded for that month. Twice the difference of these two values may be used as the value of the monthly variation.

It is obvious that the value of the equivalent ambient temperature obtained will be different, depending upon which one of these approximations is used. However, the difference will not be great, and the overloads obtained will be reasonably safe. The more conservative the figure used for the monthly variation, the less frequent will be overloads combined with ambient temperatures which must be classed as emergency conditions. The life of the transformer will be prolonged if emergency conditions occur with less frequency. The safest practice is, of course, to use the most conservative of the methods in cases where data for different methods are available.

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Wood-Pole 230-Kv Transmission Lines

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MEMBER AIEE

STEEL has been comparatively cheap in the United States and, until recently, available in quantity. Consequently the use of steel structures for supporting the higher-voltage transmission lines has been favored by transmission engineers. However, since conservation of critical materials has become necessary, it is essential that designs incorporating wood construction be considered for such applications. A contribution to efforts in this direction is provided by a review of the design used for single-circuit wood-pole 230-kv transmission lines recently constructed to supply power to an important industrial area.

Early in 1942 plans for this project, including steel-tower designs, had been completed, and materials were about to be ordered when restrictions regarding the use of steel became effective. Subsequently, the project was redesigned for wood-pole construction, and the entire project consisting of 158 miles of single-circuit 230-kv line was completed and placed in operation $5^{1}/_{2}$ months later.

The decision to use wood construction precipitated a hasty review of previous experience with such designs. It was found that wood-pole construction had been used in Sweden for 230-kv operation with satisfactory results. However, detailed information regarding these lines was not available. These lines are the longest wood-pole lines built in the United States for 230-kv operation and the only ones for which wood has been used for all structures and all types of structures.

Design Premises

Conventional designs for transmission lines operating at lower voltages were available, and many had been proved satisfactory over long periods of operation. The general form of the two-pole *H*-frame structure appeared acceptable, though it was found that the problem of design was increased greatly because of increased insulation and conductor-clearance requirements, greater mechanical loads caused by the increased conductor diameters, and correspondingly increased ice and transverse wind loads. It was intended that the lines should be designed to operate with a high degree of reliability and that high factors of safety should be employed in consideration of the importance of the connected load and the complete lack of previous experience with wood-pole construction for 230-kv operation. To attain high electrical reliability, advantage was taken of the insulating value of the wood supporting members in the paths between conductors and ground. Suspension insulator strings of 15 $5^{3}/_{4}$-inch units were provided, and these were supported through large metallic surfaces in order to distribute leakage currents. Experimental data regarding the minimum insulation required for 230-kv operation to prevent digestion and burning of crossarm material were not available. Operating records of lower-voltage wood-pole lines with proportionately less porcelain insulation indicate that the number of insulator units selected for these lines will assure satisfactory performance.

The impulse flashover voltage ($1^{1}/_{2}$x 40-microsecond negative wave) of a 15-unit insulator string is 1,345 kv, which is the flashover voltage of a rod gap of 76 inches. The minimum clearance of the conductor from the pole ground wire was maintained at 86 inches. This value of minimum clearance permitted a maximum conductor displacement of 37.5 degrees from vertical. Minimum clearance to crossarms and other wooden members was 76 inches. For determinations of maximum conductor displacement, a wind pressure of eight pounds per square foot on bare conductor was assumed.

Crossarm Design

The length of crossarm required to maintain clearance desired under these conditions was 50 feet, which length provided a conductor separation of 24 feet. Each crossarm consisted of 3- by 12-inch timbers, two 28-foot lengths and two 22-foot lengths, spliced at the center by means of a block 10 feet long and 12 by $7^{1}/_{4}$ inches in cross section. A lapped joint was designed using bolts and split-ring timber connectors between surfaces in contact. Blocks of similar dimensions except four feet in length, were installed between the 3- by 12-inch members at the ends of the crossarm, also by means of bolts and timber connectors. These blocks served to increase greatly the strength of the arm in the longitudinal direction.

Conductors

Hollow copper conductors of 500,000 circular mils cross section, 1.1 inches in diameter, were installed. The conductor loading assumed was one-half inch radial ice with an eight-pound wind at zero degrees Fahrenheit. The maximum tension under this loading condition is 9,000 pounds or 41.5 per cent of the ultimate strength. Two overhead ground wires, each consisting of three number 5 extra-high-strength copper-covered steel strands, were installed and located so as to provide 30-degree angles of protection for each conductor. The maximum tension of overhead ground conductors is 4,300 pounds or 36 per cent of the ultimate strength.

Structure Design

To meet National Electric Safety Code requirements for grade-*B* construction and to provide sufficient strength for spans of reasonable length, class-2 poles were specified and installed. Poles 65 feet in length were required in order to provide level ground spans of 520 feet and the desired final clearances of conductors to ground of 32 feet at 60 degrees Fahrenheit. The pole-market situation early in 1942, however, was such that 65-foot poles were not available in the necessary quantities, though relatively greater quantities of 60-foot class-2 western-red-cedar, southern-pine, and Douglas-fir poles were available for early shipment. Poles 60 feet in length were purchased and were made to serve the purposes of 65-foot poles by installing latticed steel-channel extensions on the tops of each of the poles of the two-pole structures.

The top of the steel-pole extension and the end of the crossarm were connected through a tension member, and the tops of the extensions were in turn tied together by means of one-half-inch Siemens-Martin strand. Formed channel bayonets were bolted to the steel-pole extensions to support the overhead ground wires.

The structure was strengthened by means of a *K*-brace assembly consisting of 3- by 10-inch timbers and 3- by 6-inch timbers, and an *X*-brace consisting of two

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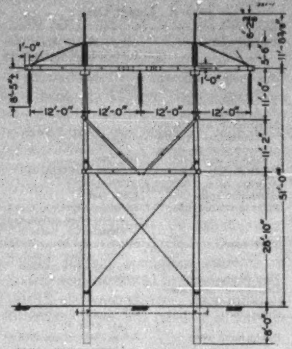

Figure 1. Type-*A* tangent structure

one-half-inch Siemens-Martin strands in the lower section. Each joint was carefully designed and assembled with bolts and timber connectors in an effort to attain complete rigidity of the structure in the direction of transverse wind loads.

Structure Types

Two circuits were built on a single right of way 250 feet in width, and center lines of structures of the two circuits were spaced 100 feet. Guying was so arranged that all guy stub poles and anchors were within the limits of the right of way.

The type-*A* tangent structure, shown in Figure 1, was installed at all normal tangent positions in the line and for small angle positions, guyed and unguyed, depending upon the angles of the line. The maximum angle with which this type of structure could be used was approximately three degrees. The point of minimum strength in the type-*A* structure is at the upper point of connection of the *K*-brace to the pole. For this reason, additional strength could not be obtained by guying from this point or from points lower on the structures. Therefore, guying at slight angles was accomplished by connecting to the end of the crossarm and extending the guy horizontally to a stub pole, then to the anchor connection at the usual angle. A wood strain insulator was installed in the horizontal guy between the crossarm and the stub pole in order to maintain the insulation level of the line. To provide transverse support for the overhead ground wires, a second guy was connected to the overhead ground-wire attachment on the bayonet. The two bayonets were connected horizontally by means of one-half-inch Siemens-Martin strand. Since pole ground wires were not installed on poles of guyed structures of any type, the overhead ground wires were connected to earth at such structure locations by means of the guy strands and anchor rods. Guys for type-*A* structures were installed on one side for small angles in the line and, similarly, on both sides for structures supporting long spans.

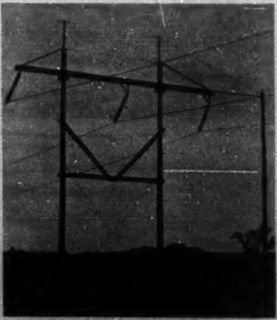

Figure 2. Type-*B* angle structure

The type-*B* structure, shown in Figure 2, is a variant of the type *A* in that the crossarm was offset two feet, and angle brackets were added in order to provide desired conductor clearances at angles in the line up to approximately eight degrees. Type-*A* structures were guyed also in the same manner as shown in this illustration. In relatively level sections of the right of way type-*A* and type-*B* structures were assembled on the ground and erected by means of special equipment shown in Figure 6. In rugged and hilly sections the use of this equipment was not feasible or economical. In these sections arms, braces, and all fittings were installed by means of hoisting equipment after poles had been installed.

The type-*C* structure, shown in Figure 3, is a braced and guyed angle suspension structure and was installed at positions where the line angle was between approximately 8 degrees and 25 degrees. The type-*C* structure is conventional, though enlarged for 230-kv operation, except that 12,000-pound wood strain insulators were installed as tension members to transmit overturning stresses from the tops of the unguyed poles to the guys.

Dead-end structures, referred to as type *D*, and shown in Figure 4, were installed for long spans, at line terminals, and at locations where the line angle exceeded approximately 25 degrees. At angle positions guys of sufficient strength to withstand maximum tensions of conductors and overhead ground wires were installed on both sides of the structures and in line with conductors and overhead ground wires, except that the overhead ground wire inside the angle at such positions was installed in suspension and held in place by one guy to the top of the guyed pole at the opposite side of the structure. This was done to avoid the use of stub poles and overhead guys. At tangent locations conductors were guyed in the same manner. In order to maintain clearances between conductors and overhead ground-wire guys at tangent locations, guys were installed in vertical planes displaced 45 degrees from the vertical planes of the conductors, and pole tops were connected together by means of one-half-inch Siemens-Martin strand in order to balance guying stresses.

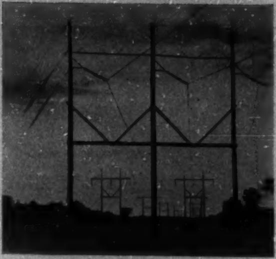

Figure 3. Type-*C* angle structure

Two sets of transposition structures were installed in each circuit to provide one complete barrel to balance the electrical characteristics of the three phases. Type-*F* structures were installed for this purpose, and are as shown in Figure 5.

At two locations it was necessary to cross high-voltage tower transmission lines of the power company operating in the area in which these lines were built. Crossing spans were supported in each case by type-*A* structures using 100-,

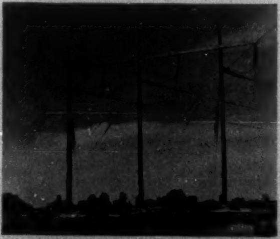

Figure 4. Type-*D* dead-end structure

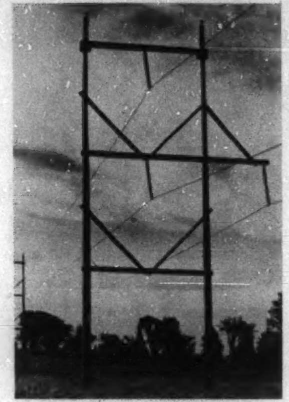

Figure 5. Type-*F* transposition structure

105-, and 110-foot pine poles. These structures were supported in all directions by means of sets of guys attached at the level of the horizontal braces and at points midway between these attachments and ground. Two spans away in each direction from the crossing spans, dead-end structures of normal height were installed, conductor heights having been graded down by the installation of poles of moderate lengths for the intermediate structures.

Guyed structures were installed as required by the rugged terrain and may be round at frequent intervals. Storm guyed and stabilizing structures as such were not used. The longitudinal strength of the assembled arm used in the type-*A* structure is such as to equal the unguyed strength of the supporting poles. The effect of one broken conductor is to deflect torsionally the whole structure, which deflection is resisted and limited by the remaining unbroken conductors and overhead ground wires. The overhead ground-wire bayonets bend under this condition, and the tops of the suspension insulator strings, moving, permit rotation of the crossarm in the horizontal plane. The conductor tension on the side of the structure opposite the break is relieved as well by the displacement of the insulator string, approximately eight feet in length, attached to the broken conductor.

Grounding

Overhead ground wires were connected to earth at all unguyed structures by means of pole ground wires and helical coils installed on each pole of the structures. Pole ground wires and helices were installed on every guy stub pole, and overhead ground wires were connected to earth through guy strands and these pole ground wires. Where stub poles were not required, the overhead ground wires were connected to earth by means of guy strands and anchor rods. Helical-coil grounds were installed on the butts of all poles, and, where pole ground wires were not installed, grounding conductors were extended from the butts of the poles to within a few inches below the ground line. Time did not permit the installation of an adequate grounding system during the construction period, though ground coils were installed for use as parts of that system, to be installed later. Wood strain insulators were installed in all conductor guys. The ungrounded ends of all these guys and all adjacent hardware were securely bonded to the insulator attachments to prevent possible charring of wood and radio interference.

Counterpoise

During the construction period and before the conductors and overhead ground wires were installed, resistances to earth of type-*A*-structure ground installations were measured. In this way a complete survey was made of ground resistances throughout the whole length of the line. A counterpoise system based upon this survey was designed and installed immediately following completion of the overhead construction.

In sections of the line where resistances were 30 ohms or less, no counterpoise conductors were installed. In sections where ground resistances were highest, approximately 750 ohms, four conductors were installed along the right of way and parallel to its center line. Counterpoise systems of three, two, and one conductor, according to measured resistances, were installed in other sections of the line. Solid galvanized-iron conductors of size 5 Birmingham gauge were buried by means of cable-laying plows to depths of 18 inches. Cross connections were installed to connect together all counterpoise conductors and all existing structure ground installations.

At all dead-end and guyed structures, all grounds, which included anchor rods and helical-coil grounds were connected together to form special low-resistance grids, which were, in turn, connected to the counterpoise conductors by means of cross connections. The entire overhead ground-wire system then had been connected to the counterpoise system by means of pole ground wires of the unguyed structures and the overhead ground-wire guys of the guyed structures.

Table I. Design Data

Type of construction	Single-circuit wood pole braced, *H*-frame with bayonet extensions
Operating voltage (design)	230 kv
Total miles of line	158
Right of way width	250 feet
Horizontal separation between center lines of circuits on same right of way	100 feet
Level ground span	520 feet
Average span	500 feet
Maximum span	1,300 feet
Poles (kind and class)	Southern yellow pine, western red cedar, and Douglas fir, Class 2
Treatment	Western red cedar, butts only; others, 8 pounds full pressure creosote
Normal pole length	60 feet
Conductor and pole separation	24 feet
Crossarms	Fir timbers, spliced, 50 feet over-all
Pole ground wires	Solid copper number 4 American wire gauge
Minimum ground clearance	32 feet
Minimum clearance to structure	86 inches, 76 inches to wood members
Loading class	National Electrical Safety Code, Fourth Edition—heavy
Conductors	Hollow design, 1.1 inches diameter, 500,000 circular mils
Overhead ground wires	Three number 5 copper-covered steel strand 30 per cent conductivity
Conductor tension (maximum)	9,000 pounds (41.5 per cent of ultimate strength)
Ground-wire tension (maximum)	4,300 pounds (36 per cent of ultimate strength)
Insulators	Spaced $5^3/_4$ inches, 15,000 pounds combined mechanical and electrical strength
Suspension insulator assemblies	Single strings, 15 units
Strain insulator assemblies	Double strings, 17 units
Counterpoise	One to four conductors as required
Counterpoise conductors	Solid galvanized iron 0.22-inch diameter
Line construction started	February 15, 1942
Line construction completed	July 3, 1942
Counterpoise installation completed	November 20, 1942

Total Insulation

The normal impulse flashover voltage ($1^1/_2$x40-microsecond negative wave) of conductors to ground of all suspension structures is 2,425 kv, which insulation is obtained by means of 15 suspension insulators spaced $5^3/_4$ inches and a wood strain insulator equipped with a flash gap of five feet, in series between conductor and ground. The impulse flashover voltage from conductors to pole ground wires through air is also 2,425 kv approximately. Under conditions of extreme conductor displacement and minimum clearance the impulse flashover voltage to ground of these structures is

1,500 kv. Impulse level of dead-end structures was raised to 2,620 kv by installation of two additional insulator units.

Ground-resistance measurements have not been made since the counterpoise installation was completed, though it is believed that the resistance to ground of any two adjacent span sections of the counterpoise system is not more than 30 ohms. Under conditions of minimum clearance of conductors to ground, currents of 50,000 amperes through the structure would be required to cause flashovers. Under normal conditions currents of 80,000 amperes would be required to produce flashover conditions. Less than one interruption per 100 miles of circuit per year is expected.

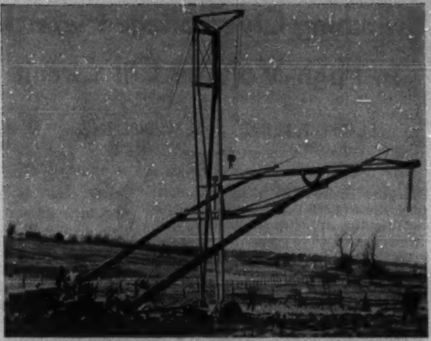

Figure 6. Special erection equipment

Conclusion

Circumstances which included an unfavorable season for outdoor construction and an extremely short construction schedule did not favor economical construction. Construction costs of these lines are not indicative of results that can be obtained under normal conditions. It is believed, however, that favorable costs can be obtained with wood construction for single-circuit 230-kv lines, though proof of this is not available at this time.

Time required for a thorough engineering study of the subject of 230-kv wood-pole line design was not available. The primary objective was to build a workable line within the shortest possible time and under circumstances which were adverse in many respects. Engineering studies, continued since construction work has been completed, suggest changes in details but indicate that the fundamental design is correct.

Wood construction for 230-kv transmission lines appears to be as practicable as for lower-voltage lines. Though steel may be available for construction of future 230-kv lines, it is believed that, in the interest of economy and improved operation, consideration should be given and further studies made regarding the use of wood supporting structures.

Switching Overvoltage Hazard Eliminated in High-Voltage Oil Circuit Breakers

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Synopsis: High-voltage transmission lines, when de-energized by conventional oil circuit breakers, are often subjected to high-voltage surges which present a real hazard to terminal equipment. The mechanism of surge generation is initiated by circuit-breaker restriking phenomena which are encouraged by the interruption of the large leading currents required to charge moderate and high-voltage lines. By interposing suitable resistors across the interrupting elements during the opening stroke of the breaker, the switching overvoltages may be either eliminated by preventing restriking or so reduced in magnitude in the event of restriking that the system is relieved of their hazard. The development of this achievement is presented chronologically:

1. **Theoretical considerations**
2. **Transient-analyzer studies**
3. **Power-laboratory switching tests**
4. **Actual field tests on the 220-kv lines of Southern California Edison Company Ltd. at Boulder power plant**

It has been recognized for many years that some of the most severe switching surges encountered in the field are those which attend the interruption of line charging current. Tests on miniature systems have indicated that transient voltages of the order of six or seven times normal line-to-neutral crest may be obtained during such switching.[1–3] Field data, in general, substantiate such predictions.

In this paper a specific problem of high transient switching voltages on the 220-kv lines of the Southern California Edison Company Ltd. is discussed. The particular line which was being subjected to these overvoltages was initially operated as a single line with three ground-fault neutralizer coils located at Boulder power plant, at Barre substation, and at Chino substation.[4] At the present time another line is in service, and both lines are normally bussed at Boulder power plant and operated as part of the extensive 220-kv solidly grounded system. After diagnosing the symptoms from a theoretical standpoint, a complete analysis was made, using the transient analyzer to determine the possible magnitudes of switching overvoltages under various possible system-operating conditions. Various means of subduing these overvoltages were carefully considered, drawing on experience in related fields, and it was concluded that equipping the circuit breakers with internal resistors of the proper ohmic value afforded the best solution.

The paper presents the story of this development starting from theoretical considerations. Some results of analytical studies are presented as well as power-laboratory test results. A large portion of the paper is devoted to the field testing at Boulder power plant and interpreting the results obtained.

The effectiveness of the method described in controlling the switching overvoltage hazard is significant, not only from the circuit breaker standpoint, but from the arrester, relaying, and over-all system-operating standpoint as well, since lines and terminal equipment are freed from the possibility of being subjected to destructive switching overvoltages, regardless of the method of system neutral grounding.

Conclusions

1. Transient overvoltages due to switching can be reduced to harmless values by equipping the circuit-breaker interrupters with internal resistors of the proper ohmic value (see Figure 11).

2. While it has been previously recommended that the ground-fault neutralizer be short-circuited during switching operations at these high operating voltages, these tests show conclusively that this short-circuiting operation is not necessary when the breaker is equipped with such resistors.

3. From the standpoint of the magnitude of switching overvoltages, it makes no practical difference whether the system is in tune with the neutral reactance or not when switching is done by means of breakers equipped with such resistors.

4. Holding the transient switching voltages to moderate levels reduces the arrester duty to that of discharging lightning surges alone, thus minimizing arrester operations.

5. When interrupting fault currents, oil circuit breakers equipped with such resistors experience reduced power arcing as a result of the reduced rate of rise of recovery voltage (see Figure 8).

6. The major results of the tests at Boulder power plant confirm earlier conclusions reached through theoretical approach followed by a complete transient-analyzer study and power-laboratory tests.

Theoretical Considerations

Since the phenomena of the interruption of leading (capacitive) currents at power-system voltages are encountered when de-energizing (1) transmission lines, (2) cables, and (3) capacitor banks[5] (see Figure 1), it should prove profitable to review the fundamental nature of the phenomena in order to grasp a working knowledge of the problems. In so doing, it will be compared in its essential differences with the interruption of lagging (inductive) currents of the short circuit. Terms and concepts will be revealed in this section. The study of this section however is not imperative to the knowledge of the results of the paper.

From the outset, a group of extremely contrasting relations arise. First the lagging current of the short circuit deals primarily with heavy currents which approximate the full interrupting rating of the breaker. All leading currents, however, are relatively small, and, consequently, the light current characteristics of the breaker are involved. In the tank-type oil breakers these differences are largely hydraulic, the interrupters being designed to do their most effective work at heavy currents. Hence the interruption of any leading currents by the tank-type breakers does not profit by the insulating characteristic provided by the most effective oil-blast action. Circuit differences are so great, however, that the above features become of secondary importance.

THE INDUCTIVE INTERRUPTION

A representative interruption of the inductive current is shown in Figure 2. Here, following contact separation, attempts at interruption occur at the first two current zeros. Each time the arc was *rekindled* by the rapid rise of recovery voltage across the breaker, approaching twice normal crest in a matter of microseconds. With the rekindling of

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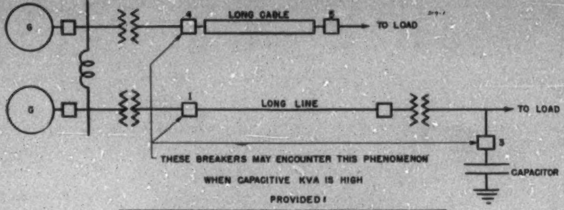

G
G
4
LONG CABLE
5
TO LOAD
1
LONG LINE
TO LOAD
3
CAPACITOR
THESE BREAKERS MAY ENCOUNTER THIS PHENOMENON
WHEN CAPACITIVE KVA IS HIGH
PROVIDED!

WHEN OPENING	BREAKER
1	2 IS OPEN
3	1 AND 2 ARE CLOSED
4	5 IS OPEN

Figure 1. Principal circuit conditions which may give rise to overvoltages of the type described in this paper

the arc, the accumulated circuit effects are completely liquidated at each current zero, and the current continues anew, as if nothing had happened, to repeat the attempt at the next current zero under more favorable conditions of contact parting and gap insulation. The characteristic of liquidating all accumulated debts at each current zero is a desirable property of the inductive circuit.

The Capacitive Interruption[2,5]

When the first current zero in the arc arrives in the passage of a leading current, interruption occurs quite easily. This comes about since the capacitive load retains the instantaneous voltage of the source at the instant of current zero, and for several hundred microseconds following there is little or no voltage across the breaker. The breaker thus mirrors an overconfident attitude in its ability to interrupt the circuit with the extremely small contact separation associated with the first current zero in the arc. All debts have not been liquidated, however, as one-half cycle later (see Figure 3) the bus voltage has reversed, and approximately double voltage thus appears across the breaker. If the breaker passes this peak without *restriking*, as does happen at limited kilovolt-amperes (short lines), then the confidence is justified, and no disturbance occurs. At higher kilovolt-amperes, however, when the arc develops more disturbance, the short arc gap cannot withstand the double voltage, and restriking occurs. At the time of restriking, the capacitor finds itself in the embarrassing position of being charged opposite to the desires of the bus voltage. In its eagerness to adjust the situation, a flow of current occurs which must be limited by the inductance of the circuit and, in good oscillatory style, permits the voltage to overshoot its mark by the initial difference. In this manner the voltage reaches minus $3E$ at the exact instant that the transient current passes through zero.

From this point two possibilities of major interest arise (Figure 3), namely:

(*a*). Interruption of the high-frequency current at the first current zero (case 1).

(*b*). Continuation of the high-frequency current, permitting it to die away, resulting in the re-establishment of the normal frequency capacitor current (case 2).

If the latter occurs, then interruption will again take place at the next current zero of the normal frequency current which is just one cycle after the first current zero in the arc. By this time the gap insulation will have increased, and subsequent restriking is usually avoided; hence final interruption is achieved. In this case the voltage to ground may rise to only three times normal crest on a single-phase basis. If on the other hand, the breaker permits the interruption of the high-frequency current at the first current zero following restrike, then the capacitor or line remains charged to as high as three times normal voltage crest. Then one-half cycle later, when the bus voltage again reverses, it permits a maximum of four times normal voltage to appear across the gap insulation. Subsequent restriking usually can be expected under these very severe conditions. In this manner voltages up to insulation breakdown values can be generated. The reduction of these overvoltages is the subject of this paper.

The above rough analysis of the interruption of the capacitive circuit, although more descriptive for the capacitor, applies equally well for the distributed case of the long open-circuited high-voltage line or cable. Here the voltage oscillations are more square-topped because of the traveling wave property of the load. As the line, for example, remains charged at the crest value of the voltage with the early interruption of charging current, double voltage then appears across the breaker with the reversal of the source voltage. If restriking occurs at the peak of this reversal, a voltage wave equal to the difference, or twice normal voltage crest, travels down the line to establish a charge equal and opposite to the initial trapped charge. The breaker current associated with this restrike is equal to the voltage of the traveling wave divided by the surge impedance of the line, about 500 ohms. When these waves reach the far end of the open line, the voltage wave reflects without change of sign to sweep back to the breaker, establishing a charge of approximately three times normal voltage crest. The accompanying current wave, however, reflects with change of sign to cancel the forward wave current. Hence current zero literally travels back to the breaker. When it arrives, the breaker arc is usually extinguished, thus leaving the line charged close to three times normal voltage crest. On a 200-mile line the complete round trip of such a wave from restrike to current zero at the breaker requires about 2,000 microseconds or about an eighth of a sixty-cycle wave. On a 10-mile cable the round trip requires about 200 microseconds.

Resistors

When resistors of the proper ohmic value are placed across the interrupting elements in the breaker, they function to reduce the probability of restriking by allowing the voltage on the load side to follow more closely the voltage of the source (see Figure 4). The resistors not only reduce the net voltage across the breaker but also act to distribute this voltage more equally within the breaker, thus lowering the maximum voltage per break which in turn further reduces the probability of restriking. It follows that should restriking occur (see Figure 4, case II), the net voltage difference (which defines the amplitude of the voltage oscillation about the generated voltage) is reduced, and hence the peak voltage to ground is lowered accordingly. To this reduction should be added the additional lowering of the peak voltage afforded by the damping attributable to the resistor. The combination of these several effects

1. Makes possible a higher capacitance kilovolt-amperes that may be switched with a given breaker without restriking.

2. Reduces the hazards of restriking in the kilovolt-amperes region where restriking is more probable.

The first of these advantages is the primary objective of the addition of resistors

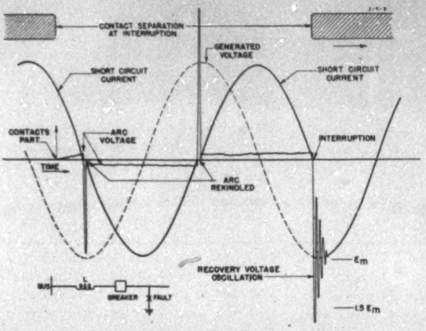

CONTACT SEPARATION AT INTERRUPTION
GENERATED VOLTAGE
SHORT CIRCUIT CURRENT
SHORT CIRCUIT CURRENT
CONTACTS PART
ARC VOLTAGE
INTERRUPTION
TIME
ARC REKINDLED
E_m
RECOVERY VOLTAGE OSCILLATION
$1.5\,E_m$
BUS
L
BREAKER
FAULT

Figure 2. Typical interruption of an inductive short circuit showing arc rekindling at early current zeros

Compare with the interruption of the capacitive circuit shown in Figure 3

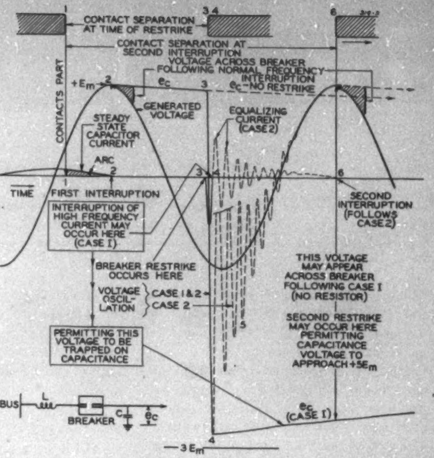

1
3 4
6
CONTACT SEPARATION AT TIME OF RESTRIKE
CONTACT SEPARATION AT SECOND INTERRUPTION
VOLTAGE ACROSS BREAKER FOLLOWING NORMAL FREQUENCY INTERRUPTION
CONTACTS PART
$+E_m$
2
e_c
3
e_c-NO RESTRIKE
GENERATED VOLTAGE
STEADY STATE CAPACITOR CURRENT
EQUALIZING CURRENT (CASE 2)
ARC
2
3
4
6
TIME
1
FIRST INTERRUPTION
SECOND INTERRUPTION (FOLLOWS CASE 2)
INTERRUPTION OF HIGH FREQUENCY CURRENT MAY OCCUR HERE (CASE I)
BREAKER RESTRIKE OCCURS HERE
VOLTAGE OSCIL-LATION
CASE 1 & 2
CASE 2
5
THIS VOLTAGE MAY APPEAR ACROSS BREAKER FOLLOWING CASE I (NO RESISTOR)
SECOND RESTRIKE MAY OCCUR HERE PERMITTING CAPACITANCE VOLTAGE TO APPROACH $+5E_m$
PERMITTING THIS VOLTAGE TO BE TRAPPED ON CAPACITANCE
BUS
L
BREAKER
C
e_c
e_c (CASE I)
4
$-3\,E_m$

Figure 3. Capacitive circuit interruption which shows a breaker-restriking phenomenon together with two possibilities of eventual interruption

This figure with the modification shown in Figure 4 is the theme of this paper

to breakers required to interrupt heavy capacitive kilovolt-amperes.

The resistor current is interrupted within the breaker as a plain-break interruption, care being taken that the resistor is so proportioned that the voltage surge associated with this final interruption does not exceed twice normal leg voltage crest. The design and arrangement of the resistor will be discussed later in the paper.

Transient-Analyzer Results

In order to make a thorough analysis of the effectiveness of shunt resistors in reducing the magnitude of switching surges, the transient analyzer[6] was used. The system studied in connection with this specific problem is shown in Figure 5. This miniature system is an approximate reproduction of the actual system at Boulder power plant on which the final field-test data were obtained.

By means of a synchronous switch which has been previously described,[6] each phase could be switched in sequence, or restrikes could be imposed in any one phase during the clearing process.

Oscillograms of such restriking are shown in Figure 6a for the first phase to clear with the system neutral solidly grounded. Voltages across the switch contacts are shown to reach values between four and five times normal line-to-neutral crest with two restrikes without resistors. When resistors are used (4,600 ohms in this case), these voltages are substantially reduced, as shown by other oscillograms in the same figure.

Line voltages to ground are also shown in Figure 6b. It is evident from these oscillograms that the shunt resistors are effective in

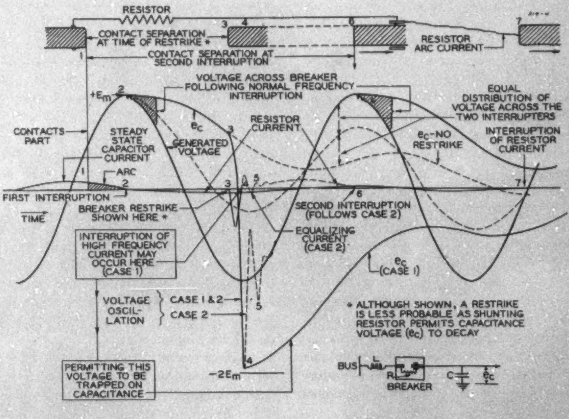
1. Reducing the a-c voltage on a line being switched out before the final break is completed.
2. Reducing the d-c voltage left on a line.

Bus voltages to ground are shown in Figure 6c. With no restriking, the bus voltage at the source is seen to drop noticeably because of the interruption of leading current. However, if restriking takes place, rapid changes in bus voltage can occur which may reach relatively high magnitudes as shown. Voltages on the bus side may be considerably higher than these if the neutral impedance is high.

A representative indication of the severity of overvoltages is the voltage across the breaker contacts during interruption. Therefore, the generalized

Figure 4. Typical capacitive circuit interruption when resistors of proper ohmic value are inserted in the circuit during the interrupting period

Compare with Figure 3


RESISTOR
3 4
6
7
CONTACT SEPARATION AT TIME OF RESTRIKE *
RESISTOR ARC CURRENT
1
CONTACT SEPARATION AT SECOND INTERRUPTION
VOLTAGE ACROSS BREAKER FOLLOWING NORMAL FREQUENCY INTERRUPTION
$+E_m$
2
EQUAL DISTRIBUTION OF VOLTAGE ACROSS THE TWO INTERRUPTERS
e_c
3
RESISTOR CURRENT
CONTACTS PART
STEADY STATE CAPACITOR CURRENT
GENERATED VOLTAGE
e_c-NO RESTRIKE
INTERRUPTION OF RESISTOR CURRENT
1
ARC
2
3
4
5
7
FIRST INTERRUPTION
6
BREAKER RESTRIKE SHOWN HERE *
SECOND INTERRUPTION (FOLLOWS CASE 2)
TIME
EQUALIZING CURRENT (CASE 2)
INTERRUPTION OF HIGH FREQUENCY CURRENT MAY OCCUR HERE (CASE 1)
e_c (CASE 1)
VOLTAGE OSCIL-LATION
CASE 1 & 2
CASE 2
5
* ALTHOUGH SHOWN, A RESTRIKE IS LESS PROBABLE AS SHUNTING RESISTOR PERMITS CAPACITANCE VOLTAGE (e_c) TO DECAY
4
$-2E_m$
PERMITTING THIS VOLTAGE TO BE TRAPPED ON CAPACITANCE
BUS
L
R
BREAKER
C
e_c

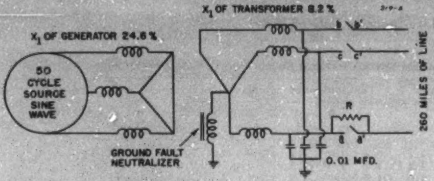


Figure 5. Miniature system studied with the transient analyzer the results of which are shown in Figure 6

curves of Figure 7 show this voltage as a function of the amount of shunting resistance *R* for various system conditions. It appears from these curves that there is no particularly critical value of shunt resistance required, but that any value of the order of 2,000 to 4,000 ohms would accomplish a very substantial reduction in overvoltage severity, regardless of the method of system grounding and for any phase to clear, that is, whether it is the first, second, or last phase to clear.

It can be shown that the effectiveness of a shunt resistor used in this manner depends upon the length of line. If a resistor is selected on the basis of sufficiently reducing transient overvoltages for the longest line that a given breaker is likely to be called upon to interrupt, then that resistor should be even more effective in reducing transient switching voltages for shorter lines, although the surge associated with the interruption of resistor current is likely to be slightly increased.

Laboratory Tests

The transient-analyzer solutions covering a wide range of probable circuit and breaker characteristics made another valuable contribution to the study by indicating

1. Critical circuit constants to be investigated.
2. The value of resistors which should prove most helpful in reducing overvoltages.

This information reduced the number of laboratory tests necessary, but even so over 400 tests were made in the power laboratory at Schenectady. However, the analyzer cannot indicate the restriking characteristics of the breaker, and hence power tests are essential. A study of the analyzer data indicated that, under the adopted assumptions with respect to breaker performance, a resistance between 2,000 and 4,000 ohms would be successful in holding the overvoltages, because of restriking, between 1.9 and 3.2 times normal over a practical range of circuit constants (including presence of ground-fault neutralizer).

Resistor designs were made up covering this range with the thermal requirements both on closing and opening under the worst field conditions in mind. The most satisfactory compromise, considering all factors, indicated a total resistance of 3,000 ohms per phase. Each resistor consists of a helically wound resistance wire which in turn is helically wound on a Herkolite tube. This winding is protected by an outer tube as shown in Figure 9. One resistor is connected in parallel with each interrupter as shown schematically in Figure 10. As the breaker opens, the power arc is interrupted in the interrupting elements, thus throwing the resistors (1,500 ohms apiece) in series with the line. As the crosshead (Figure 10) leaves the interrupting elements, the small unity power-factor resistor current is drawn as an arc between the external contacts and interrupted under plain-break conditions with a minimum of disturbance. The resistor also enters the circuit first when closing the breaker.

Leading current tests in the power laboratory were made by switching various banks of capacitors with a 230-kv oil circuit breaker, equipped with resistors as described above. Numerous tests were made also without resistors, although they were limited because of frequent arrester operations. Although the capacitor banks do not afford the traveling-wave phenomena associated with the long line, the switching problem is similar in all major respects. Therefore, the information obtained is applicable directly to the long line problem. The laboratory tests without resistors in the breaker did not reveal the true maximum voltages as protective arrester operations were encountered in many cases. The results of these tests demonstrated the ability of such resistors to hold switching overvoltages to very safe levels. This was particularly noticeable by the absence of arrester operations during tests in which the resistors were employed. Following the leading kilovolt-ampere tests the breaker, equipped with resistors, was tested on short-circuit interrupting duty

1. To test the mechanical features of the resistor element.
2. To observe the effect of the lower recovery rate; contributed by the resistor, on the arc duration.

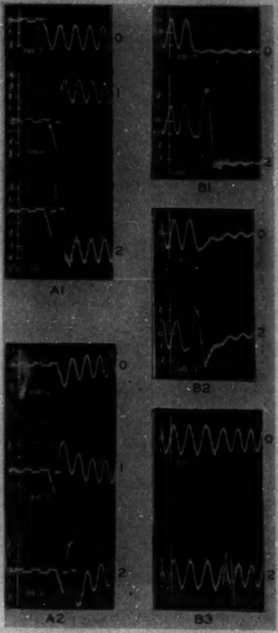


Figure 6. Transient-analyzer oscillograms illustrating the phenomenon of line-charging current interruption with and without shunting resistor in the miniature system of Figure 5

The ordinate is times normal line-to-neutral crest voltage

Numbers on oscillograms show number of restrikes in each instance

A. Voltage across the switch in phase *a*
*A*1. No shunting resistor
*A*2. 4,600 ohms shunting resistor
B. Line-to-ground voltage on line side of switch in phase *a*
*B*1. No shunting resistor
*B*2. 4,600 ohms shunting resistor
C. Line-to-ground voltage on source side of switch in phase *a*. No shunting resistor

Figure 8 shows the observed reduction in arcing time afforded by the reduced recovery rate. Breaker disturbance, contact burning, and oil carbonization are also reduced. The success of these tests led to the desirability of field confirmation on the Boulder circuits.

Field Tests

Such a series of field tests was made during the weekend of March 27 to 30,

Table I. Boulder Power-Plant Line-Dropping Tests—230 Kv

Test No.	Miles Line	Resistors	Neutral Reactor	Circuit Connection	Maximum Voltages*—Times Normal Crest									Crosshead Travel in Inches From Contact Separation to Interruption of Last Phase	
					Bus-to-Ground			Line-to-Ground			Across Breaker				
					A	B	C	A	B	C	A	B	C	Inside Interrupters	Final Break (Stroke = 36 Inches)
1A	132	Out	Short-circuited	2 generators 2d line	1.6	1.1	1.1	2.0	1.0	1.4	3.0	1.5	2.6	2 3/4	
1B					1.1	1.1	1.2	1.3	1.3	1.3	2.2	2.0	2.2	2 1/2	
1C					1.0	1.0	1.0	1.0	1.0	1.2	2.0	2.0	2.1	1	
1D					1.8	1.1	1.1	2.0	1.4	1.1	2.4	2.4	2.0	1 5/8	
1E					1.1	1.1	1.1	1.6	1.6	1.4	2.7	2.0	2.6	2 1/4	
2A	233	Out	Short-circuited	2 generators 2d line	1.1	1.1	1.0	1.6	1.3	1.6	2.4	2.2	2.3	2 5/8	
2B					1.1	1.1	1.1	1.3	1.2	1.7	2.2	2.4	2.5	2 5/8	
2C					1.3	1.1	1.6	1.2	1.4	1.9	1.8	2.4	3.0	2 3/4	
2D					1.6	1.2	1.5	2.2	1.2	1.8	3.0	2.0	3.0	3 3/8	
2E					1.1	1.2	1.2	1.4	1.7	1.7	2.4	2.4	2.2	2 1/2	
3A	132	In	Short-circuited	2 generators 2d line	1.0	1.0	1.2	1.0	1.0	1.0	1.0	1.0	1.0	1 1/8	10 1/8
3B					1.0	1.0	1.1	1.0	1.0	1.0	1.0	1.0	1.0	1	9 1/2
3C					1.0	1.0	1.2	1.0	1.0	1.0	1.0	1.0	1.0	1 1/8	9
3D					1.0	1.0	1.2	1.0	1.0	1.0	1.0	1.0	1.0	1	9 1/4
3E					1.0	1.0	1.2	1.0	1.0	1.0	1.0	1.0	1.0	1	11 1/2
4A	233	In	Short-circuited	2 generators 2d line	1.3	1.0	1.1	1.5	1.1	1.2	1.5	1.4	1.0	1 3/4	9 7/8
4B					1.0	1.1	1.1	1.1	1.1	1.1	1.1	1.2	1.3	1 1/8	9 1/4
4C					1.1	1.1	1.6	1.3	1.1	1.6	1.4	1.1	1.5	1 1/4	10 7/8
4D					1.0	1.1	1.1	1.1	1.0	1.0	1.1	1.4	1.0	7/8	8 3/4
4E					1.0	1.1	1.1	1.0	1.1	1.0	1.5	1.3	1.1	1	8 1/2
5A	132	In	Tap 7	1 generator	1.6	1.6*	2.0*	1.5	1.2	1.0	1.4	1.2	1.0	1 3/4	14
5B					1.7*	1.4*	1.5*	1.0	1.3	1.1	1.0	1.0	1.0	1 3/8	11 7/8
5C					1.7	1.7	1.7*	1.7	1.0	1.0	1.6	1.1	1.1	2 1/2	13 7/8
5D					1.6*	2.0*	1.5*	1.2	1.0	1.2	1.1	1.0	1.0	1 1/4	14 3/4
5E					1.9*	1.5	1.5*	1.7	1.1	1.0	1.6	1.2	1.0	2 1/2	14
6A	233	In	Tap 7	1 generator	1.6	1.6*	1.5	1.5	1.5	1.2	1.8	1.6	1.3	2 1/4	12 3/4
6B					1.5	1.5	1.4*	1.2	1.5	1.3	1.3	1.5	1.2	2 3/4	12 3/4
6C					1.4	1.7	1.4	1.3	1.4	1.3	1.2	1.5	1.2	2 3/4	11 7/8
6D					2.0	2.0*	1.4*	1.6	1.0	1.0	1.5	1.4	1.0	2 3/4	12 3/8
6E					1.4*	2.0	1.6	1.1	1.7	1.0	1.3	1.6	1.2	2 3/4	10 1/2
6F	233	In	Tap 3	1 generator	1.1	1.6	1.4*	1.2	1.4	1.3	1.5	1.4	1.3	2 5/8	11 1/4
6G					1.2*	1.6	1.7	1.1	1.6	1.5	1.4	1.5	1.4	2 7/8	11 7/8
7-1-A	132	Out	Short-circuited	1 generator	1.1	1.1	2.0	1.1	1.1	1.7	1.1	1.9	2.7	2 7/8	
7-1-B					1.1	2.0	1.9	1.1	2.0	1.9	1.5	2.6	2.4	3 1/8	
7-1-C					1.2	1.3	2.1	1.2	1.5	1.7	2.0	2.3	2.6	3 1/8	
7-1-D					1.2	1.4	1.5	1.1	1.2	1.1	1.5	2.1	1.8	3	
7-1-E					1.3	1.2	1.2	1.3	1.1	1.3	1.6	1.9	2.3	2 5/8	
7-2-A	132	Out	Tap 9** Tap 10** (Tuned system)	1 generator	2.4	2.2	2.2	1.8	2.0	2.7†	2.1	2.9	3.2†	4 1/4	
7-2-B					3.6	2.8	2.7	2.4†	1.6	1.6†	3.5†	3.2	4.3†	5	
7-2-C					2.6	2.3	2.2	1.6	1.6	2.1†	2.2	3.1	2.4†	4 3/4	
7A	132	Out	Tap 7	1 generator	2.9	4.4†	3.2	2.1	4.4†	3.2†	3.3		4.6†	6	

* Maximum voltages to ground recorded for both the bus and the line side of the breaker include voltages encountered either in the course of the primary interruption within the interrupters or in the course of the interruption of resistor current by the external break (see Figure 10). Voltages encountered in the interruption of resistor current are marked by an asterisk. For maximum voltage across the breaker only voltages occurring in the course of the primary interruption are recorded, since voltages across the breaker during the interruption of resistor current involve no stress on solid insulating material other than that occasioned by voltages to ground on either side of the breaker. Voltage across the breaker during the interruption of resistor current never exceeded twice normal line-to-neutral crest.

† Reading affected by flashover in voltage divider. Approximate correction applied.

** Two reactors were connected in parallel; one on tap 9 and one on tap 10.

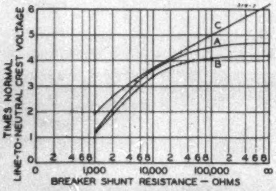


Figure 7. Effect of breaker resistors in reducing transient overvoltages during switching

Voltages shown are the maximum possible across the breaker for two restrikes in the phase being switched for any phase to clear, that is, first, second, or last

A—System neutral solidly grounded

B—System neutral grounded through ground-fault neutralizer tuned to 260 miles of line

C—System neutral grounded through ground-fault neutralizer tuned to half (130 miles) of the total of 260 miles being switched

1942, on the 230-kv number 8 oil circuit breaker in the switchyard of the Southern California Edison Company at Boulder power plant. This breaker is a tank-type breaker which contains multibreak interrupters[7] and has an interrupting rating of 2,500,000 kva and an interrupting time rating of five cycles. The voltage-limiting resistors, previously described, are mounted on the interrupters as shown in Figures 9 and 10.

All tests were circuit opening in which only the charging current of the connected line (far end open) was interrupted—commonly termed line-dropping tests. In this type of interruption, leading currents involved are small. Thus, within reasonable limits, arcing time is no important consideration. On the other hand, unless precautions are taken, breaker restriking quickly generates overvoltages which may be harmful to the breaker itself and to other connected equipment. Control of these overvoltages was therefore the primary concern in these tests, and the purpose of the test program was to determine

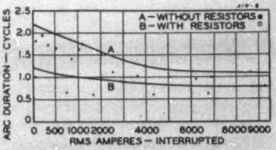


Figure 8. Breaker arcing time during the interruption of inductive short circuits, showing the improvement as a result of reduced recovery rates and better internal voltage distribution brought about by resistors in the breaker

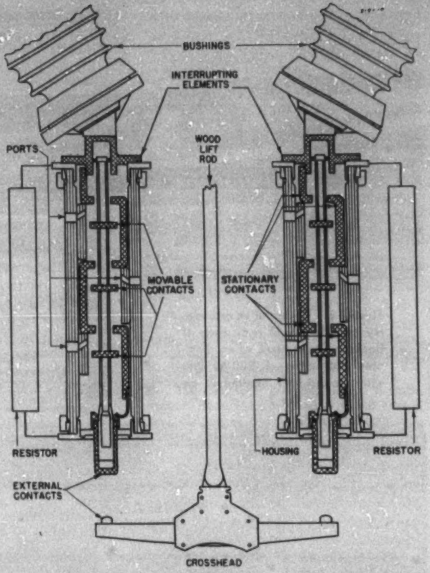
1. The nature and magnitude of the overvoltages under various system conditions.

Figure 9 (left). General Electric multibreak interrupter unit for 230-kv oil circuit breaker with resistor attached

Two such interrupters are used per pole as shown in Figure 10

Figure 10 (right). Schematic diagram showing the multibreak interrupters with resistors attached

The resistor current is interrupted in the gap formed by the external contacts



2. The effectiveness of resistors assembled in the breaker, in parallel with the interrupters, in reducing these overvoltages to harmless values.

This approach naturally required that the line-dropping test program should include various practical system conditions with and without resistors in parallel with the interrupters. The following conditions were subjected to change:

1. The length of line being disconnected.
2. The neutral connection.
3. The nature of the power source.

Circuit Conditions

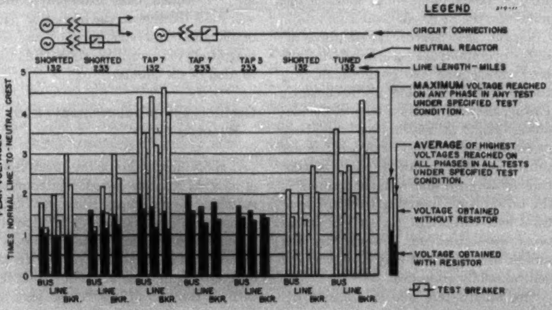
1. In some tests the entire 233-mile line to Chino substation was disconnected at Boulder power plant; in others only the 132-mile section to Pisgah substation.

2. In some tests the neutral was solidly grounded; in others it was grounded through reactance. Three different values of reactance were used; one of these values was that required for tuning as a ground-fault neutralizer to the line length being disconnected; the other two values, corresponding to other neutralizer tap settings, have been used to tune the line in conjunction with additional reactors at the far end of the line and consequently correspond to reactances above the tuned value.

3. In some cases two local generator-transformer units were connected to the bus, and the remainder of the system was tied in through the second line to Chino substation. In the others only one generator-transformer unit was connected to the bus, and there was no tie with the rest of the system.

Figure 11. Pictorial summary of results of oil-circuit-breaker tests made at Boulder power plant March 28 and 29, 1942

Comparison of voltages caused by interruption of transmission-line charging current. Here the overvoltages obtained with and without breaker resistors, under various line and ground conditions are shown



Test Results

A total of ten groups of tests was made at Boulder power plant covering practical combinations of circuit and breaker arrangement. Table I describes the results of these tests with respect to the maximum voltages developed and with respect to the breaker crosshead travel necessary for interruption. The overvoltage record, with and without resistors, is presented graphically in Figure 11 in such a way that the effect of the resistors, of the

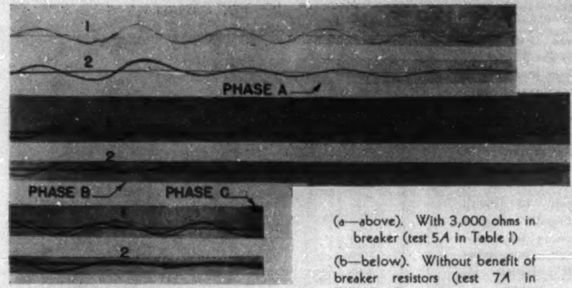
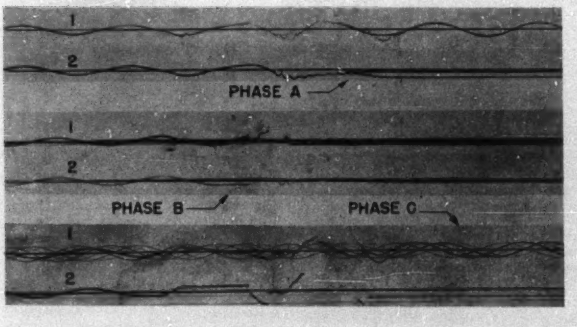


Figure 12. Comparative cathode-ray oscillograms of tests made at Boulder power plant when dropping 132 miles of 230-kv line with ground-fault neutralizer on tap 7

(a—above). With 3,000 ohms in breaker (test 5*A* in Table I)

(b—below). Without benefit of breaker resistors (test 7*A* in Table I). During this test a 220-kv bushing flashed over

1. Voltage to ground on bus side of breaker
2. Voltage to ground on line side of breaker



length of line, and of the ground-fault-neutralizer tap setting are readily apparent.

In the course of these field tests, three arrester operations were observed, one on phase *a* and two on phase *c*. These arresters are located near the terminals of the 82,500-kva transformer bank at Boulder power plant which is about one mile from the Southern California Edison Company switchyard. These three arrester operations all occurred during the tests which used the ground-fault neutralizer and no resistors in the breaker. During an earlier series of tests at Boulder power plant, at which time the above problems were being diagnosed, under similar system conditions (with the neutralizer untuned and without resistors in the breaker) six lightning-arrester operations were observed. No arrester operations were observed, however, for ten similar tests made at that time with the neutral solidly grounded.

The interrupters were completely examined at four points in the test program. There was no damage to the contact or insulation parts as a result of any of the tests made when the resistors were connected. On the last inspection following tests with the ground-fault neutralizer and without resistors (during one of which tests with the neutralizer untuned the breaker flashed over externally), there was some damage to the insulation parts of the interrupters but not of sufficient magnitude to prevent the successful interruption of the circuit.

Oscillograph Measurements and Records

A good illustration of the effectiveness of internal resistors in reducing transient switching overvoltages is shown in the cathode-ray oscillograms of Figure 12, which places together for easy comparison a test with a 3,000-ohm resistor in the breaker (Figure 12a) and a duplicate test without resistors (12b). These tests are numbers 5*A* and 7*A* respectively (Table I), in which 132 miles of line were dropped with the ground-fault neutralizer on tap 7. Figure 12a shows no overvoltages of any consequence although there is restriking during the clearing period. In contrast the restriking associated with Figure 12b (no resistors) results in violent transient disturbances which were so high as to cause sparkover of a protective gap in the cathode-ray-oscillograph measuring circuit which made it impossible to determine accurately the actual crest voltage obtained. The voltages reached for this test, however, were high enough to sparkover the 220-kv bushing on the source side of the breaker. This would require a maximum of 1,050-kv crest, depending upon several factors, the most important of which are the wave shape of the transient voltage and the lower humidity, both of which would lower the actual flashover.

The oscillograms of Figures 13a and 13b are the magnetic oscillograph records of the same field tests as presented in Figure 12, namely tests 5*A* and 7*A* respectively of Table I. In Figure 13a the duration of the unity-power-factor resistor current is clearly shown. Characteristic restrike current surges are shown in Figure 13b. Here four separate restrikes took place, all of which interrupted with the arrival of the first current zero of the high-frequency oscillation. Figure 13 also exhibits oscillograph measurements of phase current, trip coil current, and breaker travel.

Voltage to ground on each side of each pole of the test breaker was indicated on the cathode-ray oscillograph. The deflection plates of this oscillograph were energized by means of capacitance potentiometers, the upper section of which was furnished by the carrier-current coupling capacitors on the two transmission lines. This provided an arrangement capable of reproducing satisfactorily voltages of all types from the high-speed transients obtained upon a restrike in the circuit breaker to the slowly leaking charge left on the lines upon final interruption.[9] The two voltages for each pole of the breaker were recorded on parallel traces on the same film, so that the voltage across the breaker pole could be determined readily by graphical subtraction.

Discussion of Results

Figure 11 shows the effect of the resistors upon the overvoltages attending switching. As is indicated by this figure three pairs of test groups were taken in such a manner that direct comparison can

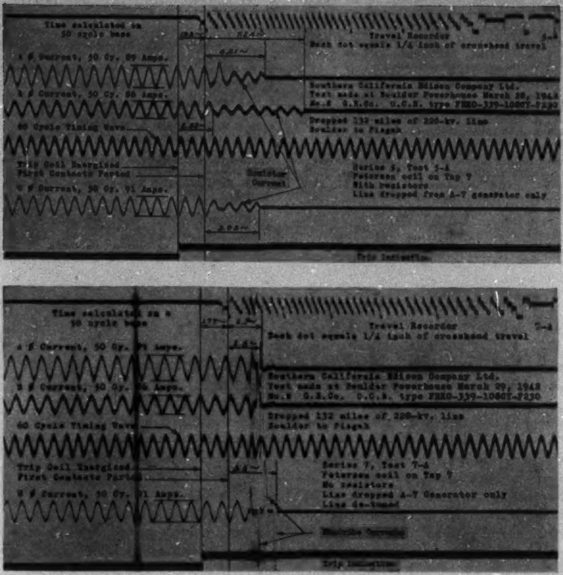


Figure 13. Magnetic oscillograms associated with the voltage records of Figure 12

(a—top). With breaker resistors. Note resistor current

(b—bottom). Without resistors. Four separate restrikes occurred on this test of the type shown in Figure 3, case I

be made to show the effect of the resistors. In these cases, and also in the other groups taken with the resistors connected, the voltage was limited to twice normal line-to-ground peak or less—both when the resistor was being switched in and when the resistor current was being interrupted. When the resistors were not connected, on the other hand, the voltage across the breaker reached three times normal with the neutral solidly grounded and more than four times normal with a neutral reactor connected.

Correspondingly, as shown in Table I, the resistors bring about a considerable reduction of arc length inside the interrupters as a result of the improved internal voltage distribution and the improvement in the rate of rise of recovery voltage. The residual current which remains in the resistors is then interrupted by the disconnecting break (formed by the crosshead, Figure 10) in from about 15 per cent to approximately 35 per cent of their available break distance. Here also the resistors are effective in preventing any build-up of voltage beyond the values shown in Table I and Figure 11.

The fact that the resistors produce such a substantial reduction in overvoltage makes them preferable to other measures, since they afford protection thereby to other terminal apparatus. Unless the breaker is sufficiently positive in action to prevent restriking and its associated overvoltages, therefore, the use of resistors appears desirable whenever the length and voltage of the lines are such as to give rise to trouble of this type. Because of the dependence of this restriking phenomena upon so many factors, the most important of which is the breaker characteristic, it is impossible to indicate here the capacitance kilovolt-amperes where restriking can be expected to occur. Tests at 15 kv on one manufacturer's breakers,[9] for example, indicated a limit of 10,000 capacitive kilovolt-amperes above which overvoltages could be expected without the aid of internal resistors. The 132 miles of line dropped, as exhibited in Figures 12 and 13, represents about 35,000 capacitive kilovolt-amperes. These values cannot be compared directly with those shown at 15 kv, however, because of the wide difference in voltage levels. The above makes clear that when small capacitive kilovolt-amperes are to be interrupted, with standard power breakers (short lines or cables, protective capacitors, and so forth), no trouble should be expected.

The operations in all the tests presented in this paper were confined to dropping a length of open transmission line, but, since breaker operation upon all except three-phase faults involves dropping at least one unfaulted phase conductor, the phenomena will be present in practically all breaker operations.

The results of these tests are in good qualitative agreement with the results of miniature-system tests as regards the principal features:

1. With the untuned neutral reactor, voltages obtained without the resistor were much higher than those obtained with the neutral solidly grounded.
2. The resistors brought about a substantial reduction of voltage in all cases, and, the higher the initial overvoltages, the greater was the percentage reduction.

A comparison of the overvoltages obtained for the tuned ground-fault neutralizer with those obtained for the solidly grounded system indicates the desirability of supplementing miniature test with full-scale tests under actual conditions. This is emphasized by the fact that the voltages associated with test series 7–2 are very high, relative to those obtained for test series 1, which was the solidly grounded case with the same line length. From earlier studies, on the other hand, it had been concluded on the basis of the transient-analyzer results that interrupting line charging current in a ground-fault-neutralizer system is no more difficult from a switching standpoint than in the same system with neutral solidly grounded. The reason for this disagreement apparently lies in the fact that surges communicated to one phase by clearing or restriking in another were of much more importance in the ground-fault-neutralizer tests than with the neutral solidly grounded. Not only did this have a direct bearing on voltage magnitudes, but in a number of cases it appeared to be directly responsible for additional restrikes which contributed still more to the overvoltages. In the grounded neutral tests there was never more than one restrike which contributed appreciably to the building up of voltage, but several times there were two in the ground-fault-neutralizer tests. Since the miniature-system tests were based on the assumption of two restrikes with the worst timing, the full-scale grounded-neutral tests produced overvoltages considerably lower than predicted. The ground-fault-neutralizer overvoltages were about equal to predicted values, the loss caused by the fact that the worst timing was not realized being approximately balanced by the increase arising from interphase reaction.

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Keep Them Rolling

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THE electric locomotive of World War I days has broadened its usefulness to a position of recognized importance among the nation's available motive power now so necessary to the successful conduct of the present world's struggle.

This is largely attributed to the recent and swift growth of the Diesel-electric locomotive, essentially an electric locomotive carrying its own power plant, for both road and switching service.

Barely keeping pace with this new giant are the comparatively small but important groups of skilled men specializing in the care of this motive power and comprising a new branch of the established maintenance and repair department of steam railroads. The continued smooth and vital operation of transportation equipment pushed to its utmost depends upon these men; yet even these ranks are being thinned to supply the demand for skilled men in today's highly mechanized warfare.

The conservation of such highly strategic elements as time, material, and man power can find no better application than in the field of preventive maintenance. Like any preventive measure applied in time, it requires the minimum of time, effort, and facilities and can be accomplished fortunately by men with less training and skill than the more costly repair crew of high skill with necessary shop facilities.

Good generalship on the home front demands the application of the engineering method, based on fundamentals, to appreciate the nature of failures and then to guide and direct the efforts of available man power and facilities not only in repair but in the prevention of failures.

It is difficult to expound anything new in the field of maintenance, but it is possible, and the purpose of this article is to point out certain fundamentals underlying the most common maintenance functions applying to traction motors and generators. It is the expectation that such a review will stimulate responsible engineers and operators to examine and test their own practice in this light and establish preventive methods to keep the equipment rolling and out of the repair shop.

Supervisors confronted with the loss of expert workmen who carry routine practices in their heads or with the influx of new help must be especially on the alert for neglect of preventive maintenance methods and must get close again to the job to see that specific standing instructions are available and that schedules and records are not neglected.

The neglect of some simple detail by one man may start a chain of events to culminate in a serious breakdown and require an entire crew of men with shop facilities already sorely taxed to make corrections. The lack of only one-half minute's attention to cleaning a terminal may cause the contact surfaces to overheat and burn off when most heavily loaded, and a motor failure resulting when the motor is most needed brings a train in late with remaining motors overheated. This is sabotage, unintentional but effective.

Lubrication—The Oldest Problem of Maintenance

Lubrication constitutes one of the oldest and most common of maintenance functions and yet in spite of the available background provides many problems ranging from the selection of lubricant to the method and frequency of application.

Lubrication of electric traction equipment falls into three divisions, namely, sleeve bearings, antifriction bearings, and gearing. The basic purpose of lubrication in all of these is to separate the moving elements from each other by a film of oil which must support the entire load on these moving parts. Interruption of this oil film for any reason immediately allows contact between surfaces, and destructive wear begins.

SLEEVE BEARINGS AND OIL WICKS

Waste-packed sleeve bearings are common for traction-motor-support axle bearings. Fundamentally the waste packing is a wool-yarn oil wick which raises oil from the oil well to the journal surface through a window in the bearing lining. The space between the tightly packed woolen strands forms small capillary tubes, and it is between these strands and not through them that oil rises by capillary forces.

A properly packed bearing consists of two distinct parts, each having separate functions. The first is the wick portion of straight wool yarn placed vertically and evenly adjacent to the journal, one end extending down into the oil well and the other end left extending out until the second portion of the packing is in place. The second part is packed tightly in loops or folds, the purpose of which is to back up and compact the wick so as to keep it against the journal and to create the necessary small spaces for capillary action. The first portion of the packing, left extending, is now tucked securely on top of the packing so that the wick is able to resist drag in either direction of rotation.

The packing of bearings should be entrusted only to men trained and skilled in this operation. Fortunately the principle is simple, and once understood the skill is easily acquired.

New waste, clean and dry from the vendor, may be oil repellent and not con-

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ducive to good wick action. Therefore, it must be thoroughly oil soaked to insure complete wetting of all fibers and then drained before packing bearings with it.

Because of the very nature of wick action, it is important to select lubricants free from clots of tarry residue and sludge to keep from clogging the small capillary spaces between the strands and thereby restrict the free flow of oil up through the wick.

Winter and summer grades of oil must be considered where operation goes through the extremes of temperature, because an oil of sufficient viscosity to maintain an adequate oil film through summer operation is not likely to have a pour point sufficiently low in the extreme of winter operation to permit its flow through the wick to the journal surface. This invites lubrication failure until the heat from the dry bearing raises the temperature of the oil to restore flow. Meanwhile, wear has taken place in the bearing, or destruction occurs before lubrication is restored.

One grade of oil will do only where the seasonal temperature variation stays within the operating range of the lubricant. This is the subject of individual decision, based on local operating conditions, but the underlying fundamental remains unchanged.

When gauging oil level, especially during certain seasons of the year, condensation and water-soaked road beds cause accumulation of water in oil wells. The measuring stick may give a false indication of the true amount of oil in the well, because it will be wetted by even a small layer of oil floating on top of a large accumulation of water in the well. Periodic removal of drain plugs should be called for or samples may be obtained from the bottom of the well with a glass tube to check on water content.

A PRESCRIPTION FOR LUBRICATING ROLLER BEARINGS

Antifriction bearings for supporting motor and generator armatures are almost universally grease lubricated. Roller bearings require a film of oil over the highly polished steel surfaces. More than this film is excess and leads to many known evils attendant with overgreasing. A roller-bearing compartment approximately two-thirds full of grease is ideal. This allows space for the grease to be thrown clear of the spinning bearing and yet provides ample grease reserve. When a bearing compartment is too full of grease, it is forced into the spinning bearing, and churning of the grease takes place, producing excess temperatures, rapid breakdown, and oxidation of the grease. The internal pressures built up within the bearing compartment under these conditions cause the grease to be expelled past the bearing seals into the motor.

The grease expelled from the commutator end bearing accumulates under the commutator shell and flows back to clog armature-core air passages. The restricted flow of ventilating air may increase the temperature of the winding dangerously.

Excess grease, thrown over the inside of a motor, accumulates enough dirt to initiate electrical failure. The presence of grease and oil on the commutator surface is detrimental to the carbon brush performance and leads to commutation difficulties.

Since it is difficult, if not impossible, to determine the grease content in the bearing housing when greasing from a pit, the importance of relying on a correct grease schedule is evident. The amount of grease added to each bearing and the frequency of application depend upon service conditions. It is important that a regular greasing schedule should be established only after first experimentally determining the correct quantity and the time interval for the particular service requirements.

Continuous operation at high speed such as in streamliner service makes greater demands on bearing lubrication than slow-speed switching. The life of grease is dependent upon temperature, and this is dictated by motor duty and cab temperatures. General greasing recommendations can be made, but these must be verified to see that they suit local conditions.

Stubborn arbitrary decisions lead to disaster in broken bearings, wrecked motors, flat wheels, and delayed trains. Only open-minded realism can avert it.

This is not so difficult to do as it first may appear. There are many occasions during the course of operation where motors are available for removal of bearing caps so that grease content can be determined accurately. A few such inspections quickly give an accurate picture of the adequacy of the schedule being enforced. Records of such inspections supplemented by other checks such as excess grease throwing or removing plugs and obtaining samples with a hooked wire enable operators to keep a "pulse" on actual lubrication needs and revise greasing schedules correctly.

Records and schedules must represent what is taking place in the greasing pit. All efforts may be nullified by carelessness and haphazard methods during the greasing operation. It must be known just how much grease is being delivered by the greasing equipment in use. The extra shot, just for good luck, repeated frequently will upset any supervisor's records.

A desire for a perfect grease is normal and healthy, but, before it is released with a train, determine which will fail first, the motor bearing or the grease. Repeated demonstrations have shown that overfilled bearings overheat in high-speed operation, and it is recognized that unless greasing schedules are carefully controlled, overgreasing is common.

High-temperature greases are available, but the steep temperature rise attendant with overgreasing can cause destruction of the bearing steel before the breakdown temperature of such grease is reached. When a grease melts at not over approximately 150 degrees centigrade, there is opportunity for it to escape through the labyrinth seals, ease the internal pressure, and bring about a reduction in bearing temperature before destruction occurs. The grease, after such experience, is damaged, but the motor remains unharmed while the grease may be easily renewed.

PRECAUTIONS EXTENDING BEARING LIFE

Cleanliness is of extreme importance in the operational life of the highly polished precision-built roller bearings. Allowances cannot be made for any condition under which a maintainer may have to work. When grit enters the bearing, it is helpless to resist its destructive action. There are no extenuating circumstances—dirt must be kept out.

Caps for grease fittings are available, or fittings can be wiped clean before applying the grease gun; otherwise grit will be forced into the bearing with the grease. Grease guns must be filled under conditions safeguarded against contamination.

A check on the tightness of fittings and grease pipes is a "must" in the regular inspection schedule. A loose pipe clamp will allow vibration of the pipe until it drops or breaks off, leaving an opening directly into the bearing housing for entrance of water and dirt.

The policy of removing motors on a mileage basis allows detection of defects and application of comparatively simple corrective measures which otherwise would result in the costly destruction of the motor and tie-up of operation. It supplies evidence from which errors in operation or maintenance can be corrected, whereas inspection after ultimate failure has occurred often finds such helpful evidence destroyed.

This provides opportunity to remove hardened grease residue from the bearing housing and to measure contents as a check on grease schedules. The bearing can be cleaned and carefully inspected to detect defects such as loose cage rivets, excess wear, chipping, and breakdown of race or roller surfaces.

When a defective bearing is found, the motor parts involved should be checked for alignment. This can be done when the motor is reassembled and before bearing caps are assembled. An indicator clamped to the armature shaft and rotated so that it bears on the face of the outer bearing race will check the alignment of the bearing with respect to the armature axis. This should be done on both commutator and pinion end bearings, together with a check on end play and bearing clearance before closing up the assembly. Such procedure will detect a frame or framehead that may have warped from overheating or impact and thereby hastened the breakdown of the original bearing. This will enable corrective measures to be applied before the motor is returned to service and thus prevent the damage of the replacing bearing.

LUBRICATION IS ESSENTIAL TO GEARING

Gearing is probably the least understood mechanism entrusted to maintenance men. Were it generally known what effort manufacturers put into the design, heat treatment, and shaping of each tooth to the thousandth of an inch, gearing would receive the respect and attention it deserves. Few maintenance men understand the basic principle underlying the operation of involute gearing most commonly used for traction application. There is a common misconception that teeth are shaped so that they may roll on each other. Actually, the teeth surfaces slide on each other, the roll being confined to a very small portion of the profile, and the tremendous forces of moving a train are transmitted to the drivers through this sliding contact between gear teeth. It is a vital necessity to separate this contact by a lubricating film; otherwise, destructive wear is sure and certain.

Gear lubricants in common use depend upon their adhesiveness to keep the tooth covered with a protective load-carrying film. This stickiness is essential as centrifugal force tends to throw the lubricant off the teeth. The lubricant is renewed as it dips into the quantity contained at a proper level in the bottom of the gear case. The properties of the gear compound must be such as to flow readily into the path of the rotating gear and yet be sticky enough to adhere to the tooth surface, and have load carrying ability. These properties are affected by temperature. Most compounds do not have sufficient range to cover the extremes in temperature encountered from summer to winter. Careful selection of proper grades for winter and summer operation must be made where wide temperature variations are encountered. The heavy summer grade will set up solid at low temperature, and the gear will channel through it without being wetted. A winter grade of thin lubricant during the heat of summer operation will fail to stick to the teeth at speed, or at heavy load is too thin to separate the gear teeth.

A few hours of heavy load running without adequate lubricant may lead to irreparable damage. Gearing will overheat and damage the heat treatment of the steel. Excess pinion temperature by conduction through the armature shaft can overheat the roller bearing. The unequal expansion of inner and outer races, absorbing the internal bearing clearance until loaded beyond limits by internal radial stress, can cause complete destruction of the bearing and frame heads, and even destruction of the armature core when it drops on the field pole pieces at high speed, all because of faulty gear lubrication.

A necessary adjunct to gear lubrication is the gear case or gear cover. Its two functions are to keep the lubricant in and to keep foreign material out. The tongue and groove joint between upper and lower halves, the baffles and seals around axle and pinion fit originally installed by the manufacturer are seldom maintained. This is so discouraging and futile that in some designs they are completely omitted.

Contamination of gear lubricant with material picked up from the road bed makes an excellent lapping compound with no chance for grit to settle out in the thick viscous lubricant, and the very object of lubrication, to prevent wear, is defeated.

Every time a gear case is dropped for wheel change or motor change-out it is an opportunity to renew seals, check gear teeth for signs of lubrication failure, and take necessary preventive measures.

COG WHEELS OR GEARING: WHICH?

Fortunately, gearing has considerable power of recovering and after many abuses continues to transmit power even after its fundamental tooth profile has been destroyed and turned into mere cog wheels. Gear teeth seldom fail in service but continue to operate until attention is arrested by their appalling appearance, and the gears are changed out from sheer fright.

Too much attention is centered on the strength remaining in worn gear teeth from a breakage standpoint with a total disregard for the carefully designed shape of the tooth profile. There is a vast difference between cog wheels and gearing. The precise original shape of the involute tooth, while not necessarily the only possible shape, is to maintain uniform angular velocity between gear and pinion. Deviation from a proper shape for uniform angular motion for any reason makes smooth uniform rotation between pinion and gear impossible, and jerky power transmission begins, attended by severe instantaneous stresses and torsional vibration.

When gear, pinion, and axle linings remain mated over a long period, the wear is gradually adjusted between the members without causing serious deviation from normal operation. This may continue until bearing lining wear effects a gradual increase in gear center distance, and the gearing may also wear to a depth sufficient to produce a step in each tooth profile.

Now, if the gear center distance is restored by installing new linings, it is evident that the tooth tip will be caused to ride alternately up and off the step formerly worn into each tooth, and there is interference. The same occurs when a worn gear or pinion is mated with a new one. Tremendous stresses are set up at the interference point until the teeth are battered into agreement. While this is going on, the armature shaft, bearings, and the armature winding are enduring the punishment of bending and vibratory torsional stresses.

The interdependence between apparently unrelated parts is startling; lubrication failure in gearing or axle linings may be followed by a broken armature shaft or a grounded armature winding resulting from vibratory stresses beyond the limits of the construction during the breaking-in period of unrelated gearing.

As soon as operators recognize the characteristics of true gearing, they will give immediate consideration to such practice as keeping gear and pinion mated even though the motors themselves are changed around. When a motor is changed out, the pinion is removed and installed on the ingoing motor, thereby keeping the same pinion and gear running together. In order to retain existing gear center distance, the linings also should be removed and installed on the ingoing motor.

When worn gearing must be matched with new, or where interference in tooth profile may result for any reason, consideration should be given to having the worn gearing replaced immediately with the same dispatch that worn wheels are ruled off the road. Fundamentally the question resolves itself into a choice between the additional miles to be obtained from damaged gearing and the punishment being sustained by a costly traction motor transmitting its power through cog wheels no longer capable of their original gearing function. Where armature failures are frequent, this aspect of the problem is worthy of serious study in the interests of economy and availability.

Present-day facilities for pulling pinions, easily controlled means for heating pinions, and the measurement of correct advance simplify the pinion-mounting operation to such extent that the practice of keeping pinions and gears mated is simple, quick, and attractive in view of the benefit to be gained.

Precautionary Tactics From the Service Pit

INSULATION

There is much that can be done to forestall trouble in motors and generators while they are mounted in the locomotive. A regular check of insulation resistance with a "Megger" when a locomotive comes in from a run is a simple and quick operation; yet it may detect a defect which might result in a ground failure on the very next run.

Make high-potential tests sparingly and carefully, preceding them with a Megger insulation check; otherwise a puncture or creepage failure may result from the test which could have been avoided by finding first the weak spot with the "Megger" and cleaning, drying, or repairing locally as necessary.

BRUSHES AND BRUSH RIGGING

Care must be exercised when installing new brushes to prevent the snapping down of spring levers with such force as to chip or shatter the brush. Carelessness in this respect may ruin a brush before it has a chance to give useful service.

Install a new brush in such a manner that the pigtail is placed on the far side of the carbon way away from the side on which the pigtail terminal screw is located. This permits an easy loop to be made over the brush, with little chance of its being bent over the edge of the carbon way and thus hang a brush up so as to prevent free vertical movement. Where pigtails bear against the edge of the carbon ways or spring levers, there is rapid wear of the individual strands by prolonged vibration until the pigtail is cut through and left free to wave about, with possibilities for motor flashover or ground.

Dusty operating conditions allow granules of dirt to collect between brush and brush holder carbon way which results in causing the brush to stick so that the freedom of vertical motion is restricted, and the brush does not ride the commutator properly. This contributes to sparking, flashovers, rapid commutator, and brush wear. These granules leave unmistakable evidence in the side walls of the brush in the form of lightning-shaped threadlike grooves, radiating from the top to the bottom of the brush where it makes contact with the carbon way. The correction is to find where and how grit enters the equipment and then take preventive measures if possible, and to slide the brushes frequently in the holders and blow the clearance space clear of dirt.

The screw which secures spring lever shunt to brush holder body is worthy of careful inspection, being hard to get at and commonly overlooked, yet capable of serious damage to the motor. When this screw works loose and causes poor contact between the spring lever shunt and the brush holder body, the current normally carried by the shunt is caused to flow through the brush holder spring instead. Under heavy load, this current may be of sufficient magnitude to anneal the spring and cause loss of spring tension on the brush. The failure of the brush to maintain contact with the commutator causes arcing and overloading of other brushes, which ultimately leads to flashovers or complete destruction of the brush holder by melting off from sustained arcing. The possibility for this screw to work loose completely and fall onto the commutator where it jams against a brush holder and causes a deep groove to be cut into the commutator, short-circuiting all segments, is more serious, making it necessary to cut the motor out of service and remove it for major repairs.

COMMUTATORS REVEAL HISTORY OF LOCOMOTIVE HANDLING

Observation of commutator surface appearance is a quick means for sizing up the general health of the equipment, and poor appearance is evidence to the practiced eye that trouble exists, but it is not always so simple to ascertain quickly the true cause. Fortunately commutators are capable of long suffering, and like gearing they seldom fail suddenly but give ample warning of impending trouble in time to hunt down the cause. Poor commutator condition does not imply always that the cause is within the machine, but may be traceable also to external faults or conditions, the effects of which reveal themselves on the commutator and leave their record burned into its surface.

For instance, it is not uncommon for enginemen trained in steam practice to stall the locomotive completely, intentionally or forgetfully, for unduly long periods with partial power on the motors. This is harmless in steam and even good practice under some circumstances but is capable of extreme damage to the electric drive. This may occur when a train is braked to a stop with power on to keep the train slack stretched out for a smooth start and then power is not shut off for the duration of the stop. Again, a train may be held on an inclined spur with power on while the train crew look for blocks to hold a car so that it can be uncoupled from the train.

Fundamentally, any operation which brings a motor to standstill while heavy current continues to flow causes intense local heating at the point of contact between brushes and commutator, especially if brushes are tipped to contact along one edge of their face by a slight backward movement of the armature just before the full stop was made. These local contact spots when unduly prolonged may reach red or white heat and anneal the copper commutator segment involved and also burn the shellac binder in the mica segment or mica commutator cone.

This local damage, varying from one overheated bar to as many as four groups of bars spaced 90 degrees apart in severe cases, does not immediately incapacitate a motor. The overheated segment now annealed, or the burned mica, lacks the necessary strength to keep its position under the action of centrifugal force at high operating speed and creeps above the general commutator bar surface to become a high bar with the attendant marking and burning of the surface, or the shattering of brushes and frequent flashovers.

Such bars may be found near or in the telltale burned spots at equally spaced intervals 90 or 180 degrees apart, and it may be evident by the discoloration at their ends, marking them from the others, or they may require searching for with a Scleroscope or some equivalent means.

While it is possible to keep such motors in operation by frequent cleaning or stoning of the commutator surface, ultimately a major repair is necessary to remove the annealed bars and patch, or to renew mica cones and reassemble with new hard bars.

A closer regular pattern of burned bars, especially in generators, traceable to external causes, may be brought about by the engineman failing to make a backward transfer to the series connection upon encountering a grade with a heavy train, so that the speed is dragged down, and the current of motors in parallel connection exceeds the commutation ability of the generator and, when prolonged, burns the commutator and overheats the armature winding.

A similar commutator condition is also possible by the accumulative flashing at brushes over a period of time, brought about by a delayed transition from series to parallel motor connections, in which case the control equipment requires attention and adjustment.

Sluggish reverser operation during extremely cold weather, when air-compressor oil condensate may reach reverser air cylinders, can cause severe shock to the entire transmission apparatus by allowing Diesel-engine speed to rise well above idling speed before the circuit is closed through the reverser contacts.

Sluggish air brakes may lead to emergency or convenient plugging of motors into reverse while the train is still moving ahead to effect a quick stop. The magnitude of the commutation task under such conditions is bound to blast the commutator surface, leaving a wake of splattered copper beads and craters in the surface to upset the smooth riding of the brushes.

RESURFACING COMMUTATORS DURING LAYOVER TIME

There are occasions, whatever may be the cause, when locomotive commutators require resurfacing in order to continue operation. Much of this can be accomplished quickly and easily in the locomotive unless the condition is such as to justify removal to a repair shop.

Such resurfacing is commonly done with a commutator resurfacing stone held by hand or in a tool and applied to the commutator, rotating at sufficient speed to do a good grinding job. When the stone is hand held, it should be large enough to permit a maximum arc of contact with the surface. This will allow it to ride over the low spots, rather than down into these hollows, and thus cut the high spots down first and thereby tend to produce a cylindrical surface. Remove a brush holder when it prevents the use of an ample stone size.

When irregularities are beyond correction with a hand stone, a rigid tool or fixture can be adapted to the special needs of the machine to hold the cutting stone steady or to mount a conventional Carboloy-tipped lathe cutting tool for the rough cut: then one can finish with a stone.

The brushes should be removed during the resurfacing operation to prevent their rapid wear and dusting. Clean the oily and greasy surfaces before proceeding with the resurfacing so as to prevent adherence of the grinding dust and to permit easy cleaning of the equipment after finishing with the operation. When possible, erect cardboard shields so as to baffle the interior of the machine against entrance of this dust.

Install two old brushes to supply power for driving the motor armature during the stoning. The axle, mounting the motor, is then by suitable means jacked up so that the armature and wheels are free to rotate. The remaining wheels of the locomotive must be blocked or have the brakes set so as safely to hold the locomotive stationary while the one axle is raised and left free to rotate.

Driving power may be obtained simply from the locomotive's own Diesel-electric power plant by going into the first operating position which, in most cases, will provide sufficient speed to do an excellent resurfacing job. The current required to rotate the armature is so low that no damage will result to the stationary motors through which this same current is flowing. An alternative, if preferred, is to obtain power from a welding set or some other suitable d-c power source.

In the case of a generator a similar resurfacing operation is possible in the locomotive in first starting and warming up the Diesel engine so as to permit a quick easy start later, then shutting down and removing most of the brushes from the commutator, leaving only a few easy-to-reach brushes in about three quarters of the readily accessible brush holders of each polarity.

The engine is again started, and these remaining brushes are removed, and the resurfacing operation is ready to proceed without affecting the brushes, which, as in the case of the motors, would be excessively worn during a prolonged resurfacing operation. Block out all contactors to insure no voltage during the process.

THE "AIR CURE"

Thorough cleaning of the apparatus is essential, and special attention must be given to the undercut spaces between segments to see that all dragged over copper, accumulated dirt, and carbon are removed.

A powerful tool for the detection and cleaning of these spaces is a procedure referred to as "air curing." A strong jet of dry compressed air is blown against the commutator, held close to, and directed across the surface, so that the impact will dislodge material in the groove while the commutator is rotated at gradually increasing speed and maximum-no-load voltage.

In the case of a generator, this is done with motor contactors blocked out to open the load circuit and field contactors closed in to obtain maximum excitation at full engine speed

In the case of a motor, this is done at available voltage within the speed limitation of the motor, which for practical purposes is sufficient. It is possible, when necessary, to obtain full motor voltage at full speed by resorting to separately controlled motor-field excitation circuit in order to keep within the maximum motor-speed limitation.

When sparking or rings of fire appear under the impact of the air jet, the speed and voltage should be held until they clear up. It may, in stubborn cases, be necessary to shut down and dig the troublesome spots clean before proceeding.

It is well to apply this air-cure process to motors or generators reported to flash over repeatedly for unknown and unfound causes, as a means of locating weak spots in commutator segment insulation.

The operator should protect himself during resurfacing and air-cure processing against flying particles, dust, and flashover by wearing goggles, respirator, and suitable gloves in keeping with approved safety practice.

CABLES, CLEATS, AND CONNECTIONS

Motor cables and cleats should be examined each trip as the swing of cables between locomotive and motor concentrate stress at the cable cleats which becomes the point of wear and cracks in insulation. Such defects, if not discovered and repaired, will lead to motor or power-plant failure, especially during wet weather when creepage over and through these broken surfaces will result in a ground which may be serious enough to burn off a motor cable.

Motors are at times removed in emergencies. and during the rush the contact surfaces of the terminals may be overlooked so as to make contact through a dirty surface which, later in operation will cause overheating and the melting of the soldered connections between terminal and motor cable, which may become serious enough to burn the connection off completely, open-circuit, or ground the equipment.

LOOSE BANDS CAN LOSE ARMATURES

The loss of a power plant may overload the remaining equipment in order to bring the train in. Operators may take pride in bringing a train in under such conditions and justly so, but the maintenance man must be on the alert to see what the consequences of such operation are to the remaining equipment. Enough damage may result when the equipment is heavily overloaded to cause loosening of bands, shrinking of insulation, and slot wedges which permit the vibration of armature windings. The mechanical vibration of conductors ultimately wears insulation thin so that final failure occurs by electrical breakdown when it is least expected and under comparatively easy operating conditions. Such failures result from former abuse.

Similar damage from overheating is caused by operation without traction motor blowers when they are necessary for adequate ventilation of the motors, or by insufficient air when the fan belts slip.

Overloaded motors should be changed out as soon as possible without waiting for the completion of their standard reconditioning mileage. Remove the armature immediately to make a careful inspection of bands, wedges, and insulation.

Extreme temperature gradient will exist within the motor during overload operation. In parallel ventilated traction motors the armature temperature may be expected to increase from the commutator end to the pinion end. The pinion end winding will be hottest, because it is insulated and covered over by a band and is ventilated by the hottest air, while the commutator end of the winding can conduct its heat to the exposed surface of the commutator. Where overloads persist as on a grade, or where they are repetitive, such as in frequent starting of a train with one of two power plants shut down, pinion end temperature easily may reach a value sufficient to melt the solder on the pinion-end band and yet leave the commutator-end band and commutator-rise soldering intact.

Such damage may easily escape detection by casual observation through the commutator-end inspection opening. Hence the importance of disassembling to be able to view, tap, and feel the pinion-end band for signs of loosening.

A band unsoldered, especially at speed, will cause its relaxation, and this along with shrinkage of varnish and insulation at these extreme temperatures permits vibratory movement of the windings. Prolonged operation with such mechanical motion wears through the insulation and culminates in ultimate electrical failure.

This mechanical condition detected in time has good prospects of successful repair if the band and band insulation are removed and the armature winding is thoroughly treated in good insulation varnish and again rebound. This treatment is comparatively quick and inexpensive as compared with a serious failure caused by neglect, necessitating a complete rewind in order to restore it to service. The prospect of successful operation, after delayed treatment, is difficult to predict. Slot wedges should be checked also and renewed when damaged, as these may allow the winding to burst out of the slots even though the end bands are renewed.

This is a good time to emphasize the importance of proper anchoring of the ends of the binding wire. The anchor should be such that it will hold even after the solder is thrown off. It will permit operation so as to bring the train in, which otherwise may have resulted in losing a band and bursting the winding.

There are various designs for anchoring of the band, the basis of which is to have the start and finish of the band caught in the same clip extending through from one side of the band to the other. The ends of the band are looped back and then secured by pulling the end of the clip back under the turns adjacent to the end. A band so secured may slip and relax slightly when unsoldered but will not let go.

When overloads are sustained so that all parts of the armature tend to reach uniformly high temperature, all bands will unsolder as well as the commutator-riser connections, and this is easily detected by observation through the commutator-end inspection opening. Operators are quick to remove such obviously damaged motors, but unfortunately where outright failure has not occurred, the tendency is to assume that all is in order and to continue operation, while under the surface of the relaxed armature band the vibrating winding is eating into the vitals of the armature.

Failures do not happen—they are caused, and they deserve close engineering inspection and analysis with unprejudiced zeal to search the events leading up to the failure before corrective measures in design, operation, or maintenance can be applied intelligently and effectively.

Synchronous Motors With Controlled Excitation for Suddenly Applied Loads

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It is common practice to operate synchronous motors with full-rated field current supplied from a constant-voltage exciter. In a number of cases involving variable loads, automatic control equipment has been used to vary the excitation with the load. This control causes the field current to be reduced below the rated value during light-load periods when it is not needed and increases the excitation when the load is applied, in order to prevent the motor from pulling out of step. By this means both operating efficiency and system voltage regulation have been improved.

Operating efficiency is improved by reduction of losses caused by motor field current and armature current. Field-current losses are reduced, because this current during light-load periods is less than the rated value. Armature current losses in the motor and in the power-supply system are also reduced, because the motor reactive kilovolt-amperes during light-load periods is less than that with full excitation applied.

System voltage regulation is improved, because the motor does not furnish unnecessary reactive kilovolt-amperes during light-load periods when it would tend to raise the voltage to an undesirably high value. Also, regulation is improved, because, as the load is increased, the leading reactive kilovolt-amperes furnished by the motor do not diminish and may even be increased, thus offsetting the tendency of the load to reduce the system voltage.

The advantages described have previously been recognized[1] in respect to motors whose loads vary gradually, so that plenty of time is available for increasing the excitation as the load increases. The advantages of use of automatic control for motors for loads which are applied suddenly has not been generally appreciated. This paper presents the results of an investigation of the use of synchronous motors with controlled excitation for suddenly applied loads. Quantitative results are given covering a range of machine and control characteristics and operating conditions. It was found that the advantages of improved operating efficiency and system voltage regulation can be obtained even when loads are applied suddenly. Also, when the peak loads are high relative to the required continuous load rating of the motor, certain additional advantages are available.

Suddenly applied loads with short-time peak values much greater than the average load are encountered in numerous applications for synchronous motors, such as for some metal rolling mills, rubber mills, and similar drives. In such cases the size of the motor is often determined principally by the peak load. If the motor is excited from a constant-voltage exciter, the field must be designed to carry continuously sufficient current for maintaining synchronism with peak load applied. If, however, the field need carry only the excitation required by the load, and this field current is automatically increased when peak load is applied, a smaller motor may often be used. It was found that with properly co-ordinated designs of motor and control, for a wide range of ratings and operating conditions, substantial savings in critical materials, principally copper and steel, will be obtained. For the example illustrated in Table I it was possible to employ a design of motor and control which resulted in an over-all saving of 2,000 pounds of copper and 12,000 pounds of steel. These savings are of particular importance at the present time of shortages of these materials.

In the case outlined, it was found that the addition of the excitation control equipment did not result in an increase in over-all cost. For ratings considerably

Table I. Example of Synchronous-Motor Application Involving Suddenly Applied Loads.

Comparison of Two Motors, Each for 3,500-Kw Motor-Generator Sets for Reversing Blooming Mill

	Motor A	Motor B
Type of excitation supply	Automatically controlled	Constant voltage
Continuous horsepower rating	4,900 horsepower	4,900 horsepower
Power-factor rating	1.0 power factor	0.8 power factor* or 1.0 power factor
Exciter continuous rating	40 kw	40 kw
Maximum peak load	300 per cent	300 per cent
Approximate total weight of copper (motor and control)	4,000 pounds	6,000 pounds
Approximate total weight of steel (motor and control)	48,000 pounds	60,000 pounds
Operating efficiency (duty cycle, 100 per cent rms load, comprising repeated 250 per cent short-time peaks alternating with no-load periods)	93.1 per cent	91.1 per cent

* For motors with 300 per cent steady-state pull-out torque, the design requirements of 0.8 power factor and 1.0 power factor motors are approximately the same.

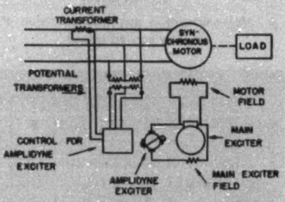

Figure 1. Circuit diagram, synchronous motor with controlled excitation for suddenly applied loads

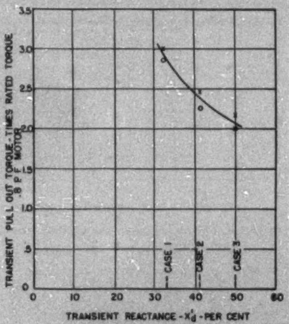

Figure 2. Effect of transient reactance on transient pull-out torque

Conditions of cases 1, 2, and 3

x—Motor pulls out with this value of suddenly applied torque

o—Motor remains in step with this value of suddenly applied torque

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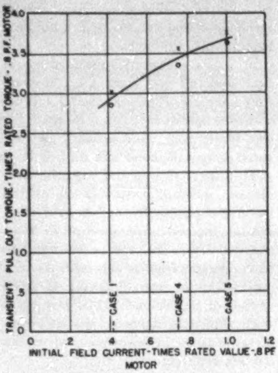


Figure 3. Effect of initial field current on transient pull-out torque

Conditions of cases 1, 4, and 5

x—Motor pulls out with this value of suddenly applied torque

o—Motor remains in step with this value of suddenly applied torque

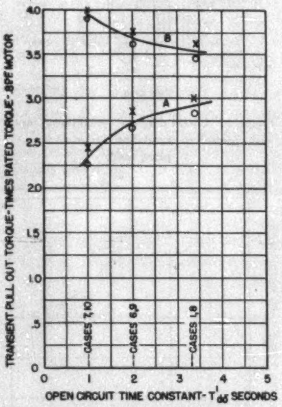


Figure 4. Effect of open-circuit time constant on transient pull-out torque

x—Motor pulls out with this value of suddenly applied torque

o—Motor remains in step with this value of suddenly applied torque

A—Conditions of cases 1, 6, and 7. Field voltage rises at rate of 110 per cent of rated field voltage per second to ceiling value of 180 per cent

B—Conditions of cases 8, 9, and 10. Field voltage rises instantly to ceiling value of 300 per cent of rated field voltage

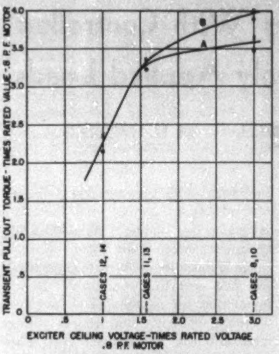


Figure 5. Effect of exciter ceiling voltage on transient pull-out torque

x—Motor pulls out with this value of suddenly applied torque

o—Motor remains in step with this value of suddenly applied torque

A—Conditions of cases 8, 13, and 14—$T_{d0}'=3.42$ seconds

B—Conditions of cases 10, 11, and 12—$T_{d0}'=1.0$ second

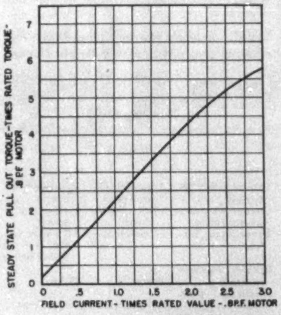


Figure 6. Steady-state pull-out torque

smaller than that of the example given, the over-all cost is slightly higher than for comparable motors and control of conventional design.

It was found that the use of controlled excitation makes possible motor designs which for many cases have lower values of full-voltage starting current than comparable conventional designs. When full-voltage starting is to be used, this feature will reduce the disturbance to the system caused by starting the motor.

It is emphasized that a completely coordinated design of motor and control is necessary to assure satisfactory performance and to make available the advantages described. In some cases, the design of the motor will be determined by requirements of the continuous rating, reactive kilovolt-amperes for power-factor correction, starting torque, or pull-in torque, as well as the peak load. The influence of these other factors should be evaluated in each of such cases.

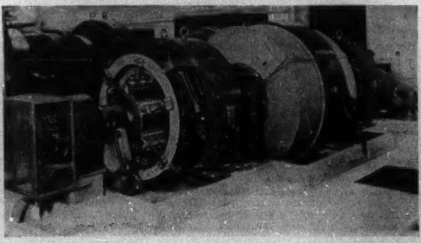
A typical excitation control system is illustrated by Figure 1. The automatically controlled amplidyne exciter varies the motor excitation by varying the voltage applied to the field of the main exciter. The control adjusts motor excitation to load requirements by responding to changes in motor input.

Effects of Design Factors

In this paper, the value of suddenly applied torque just sufficient to cause pull-out is called the transient pull-out torque. By calculating motor performance for a variety of machine constants and operating conditions, their effects on the value of this torque were determined. These effects are illustrated by Figures 2, 3, 4, and 5. For purposes of comparison, Figure 6 is included, showing the steady-state pull-out torque. This figure applies for all the cases investigated. Figures 2 to 6 are all plotted in terms of the rated torque and rated field current of an 0.8 power-factor motor with a steady-state pull-out torque as given by

Figure 7. Motor-generator set used for tests

Motor rating—2,100 horsepower, 2,100 kva, 0.8 power factor, 900 rpm, 13,800 volts, three phase, 60 cycles



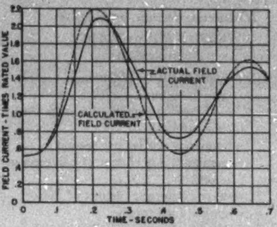


Figure 8. Comparison of calculated field current with test results

Motor rated 2,100 horsepower, 2,100 kva, 0.8 power factor, 900 rpm, 13,800 volts, three phase, 60 cycles

270 per cent load suddenly applied

Figure 6. The results can be interpreted, however, as applying to motors with other power-factor ratings and steady-state pull-out torque characteristics, by properly modifying the scales for the curves. The methods of modifying them are given in the appendixes.

Figure 2 shows the effect of transient reactance on transient pull-out torque. The maximum suddenly applied torque which can be carried is seen to vary almost in inverse proportion to the value of transient reactance.

Figure 3 shows the effect of initial field current. While decreases in the current lower the transient pull-out torque, it is seen that this initial current may be well below rated value without greatly reducing the maximum safe torque.

Figure 4 shows the effect of open-circuit time constant for two types of exciter voltage changes. Curve *A* corresponds to an excitation voltage which, as soon as load is applied, rises at a constant rate of 110 per cent of rated field voltage per second toward a maximum or ceiling voltage of 180 per cent of rated field voltage. The equivalent of this type of response is obtainable by means of the type of excitation system illustrated by Figure 1. Curve *B* corresponds to an excitation voltage which rises instantly, upon the application of load, to a ceiling value of 300 per cent of rated field voltage. This more rapid type of response is obtainable only by a regulating system with exceptionally high speed characteristics such as are obtainable by use of an electronic main exciter. It may be seen that increases in the time constant are helpful when the excitation system is of the type shown by Figure 1, but that increases in the time constant decrease the transient pull-out torque when an electronic exciter with a relatively high ceiling voltage is used. Figure 4 shows that for machines with the longer time constants, the transient pull-out torque available with an excitation control of the type shown by Figure 1 approaches closely to the torque available with an electronic exciter with a high ceiling voltage. For the lower values of time constant, corresponding to relatively smaller motors, the advantages of an electronic main exciter or its equivalent are more pronounced.

Figure 5 shows the effect of ceiling voltage on transient pull-out torque. The curves of Figure 5 apply to use of an excitation system which raises the field voltage immediately to the ceiling value.

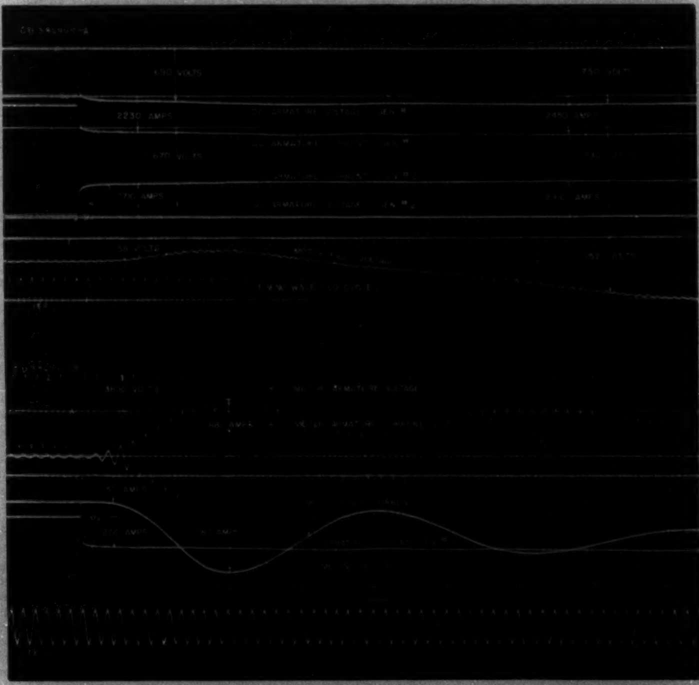


Figure 9. Oscillograms of test of synchronous motor with controlled excitation for suddenly applied loads

These curves are plotted for two different values of time constant. The curves show that there is relatively little to be gained by employing very high ceiling voltages. When the field voltage does not rise immediately to the maximum value, in some cases there will be nothing gained by use of a high ceiling voltage, because the motor will definitely pull out or will definitely reach stable synchronous operation before the exciter reaches its ceiling.

The effects of design constants and operating conditions other than those investigated for this paper may be determined readily by the method of analysis outlined in the appendixes.

Test Results

To check the reliability of the analysis and calculations of this paper, tests were made on the 2,100-horsepower 900 rpm 13,800-volt, 0.8 power-factor motor driving the motor-generator set shown by Figure 7. The pull-out torque of this set, with rated excitation applied, is 220 per cent. An excitation system of the type shown by Figure 1 was used and adjusted to supply at no load a field current of approximately 50 per cent of rated excitation. A load of 270 per cent of rated load was suddenly applied to the motor by connecting the two d-c generators of the set to a water box load. The motor did not pull out of step. Figure 8 shows a comparison of calculated field current with measured field current. Further tests were made in which the transient reactance of the motor was increased by adding external inductance to the field circuit of the machine. By successive trial applications of load, the load just sufficient to cause pull out was found. This load corresponded closely to the transient pull-out torque as determined by calculations. Figure 9 shows a typical oscillogram taken during the tests.

Conclusions

Following is a summary of the conclusions reached:

1. For a wide range of ratings of motors for loads with relatively high, suddenly applied short-time peaks, use of suitable automatic excitation control offers, without increasing over-all initial cost, the following advantages over use of motors excited by constant-voltage exciters:

(*a*). Savings in critical materials, particularly copper and steel.

(*b*). Increase in over-all operating efficiency.

(*c*). Improvement of supply-system voltage regulation.

(*d*). Reduction of full-voltage starting current.

2. Properly designed excitation control systems operating in exciter field circuits are adequate for the majority of cases in the range investigated.

3. The transient pull-out torque will be increased by reducing machine reactances, particularly the direct-axis transient reactance.

4. The transient pull-out torque will be increased by increases in the following:

(*a*). Initial field current.

(*b*). Rate of rise of field voltage caused by action of excitation control.

(*c*). Exciter ceiling voltage, in some cases.

5. The transient pull-out torque available when a suitable excitation system is used, is in most cases greater than the steady-state pull-out torque available with the field current for rated continous load.

Appendix A. Assumptions

The following assumptions were used:

1. The direct- and quadrature-axis armature reaction magnetomotive forces are proportional to reactive voltage drops equal to $i_d(X_d-X_l)$ and $i_q(X_q-X_l)$, respectively.

2. The field current, I_s, required to overcome saturation is a function only of the total direct-axis air-gap flux and the field current.

3. The value of I_s may be determined by the relationships of Figure 14 and the assumed empirical relation, $e_s=e_{ld}+0.12I_{fd}$.

4. The effects of amortisseur windings and other rotor circuits in addition to the field winding are negligible except in producing a damping torque proportional to the rate of change of rotor angle.

5. Armature resistance is negligible.

6. Impedance in the power system supplying the motor is negligible.

Appendix B. Nomenclature

The following nomenclature is used in this paper:

e_t = terminal voltage per unit
e_{ld} = direct-axis component of air-gap voltage
e_d' = direct-axis transient internal voltage
e_s = equivalent voltage determining saturation requirements. ($e_s=e_{ld}+0.12I_{fd}$)
i = armature current
i_d = direct-axis component of armature current
i_q = quadrature-axis component of armature current
I = total field current
I_{qd} = field-current requirement based on quadrature-axis synchronous reactance
I_{fd} = field-current requirement excepting effect of saturation
I_s = field current required to overcome saturation
X_d = direct-axis synchronous reactance
X_q = quadrature-axis synchronous reactance
X_d' = direct-axis transient reactance
X_l = leakage reactance
δ = angle in electrical degrees between e_t and the direct axis
D = damping-constant, torque per radian of angular displacement per radian of time
H = inertia constant, seconds; $H = \dfrac{0.231(WR^2)(\text{rpm})^2\times 10^{-6}}{(\text{Base kva})}$
T_{do}' = open-circuit time constant, seconds
t = time in seconds
Δ indicates change in quantity during time interval, Δt
f = frequency, cycles per second
v = angular rate of change of δ, degrees per second
P_e = electrical torque output of motor
P_L = applied load torque
P_D = damping torque
P = net decelerating torque

Numbered subscripts refer to step numbers in step-by-step calculation.

Armature currents, armature voltages, reactances, torques, and inertia and damping constants are expressed in per unit on motor kilovolt-ampere, voltage, speed and frequency rating. On some figures, torque values are expressed in times rated torque of an 0.8 power-factor motor. For these figures it is assumed that the rated horsepower equals rated kilovolt-ampere. For motors (of any power-factor rating) having a ratio of rated horsepower to rated kilovolt-amperes other than unity, the torque scales of these figures should be divided by this ratio. Field currents and voltages are expressed in per unit of the value required for no load, rated voltage on the air-gap line. On some figures, field-current values are expressed in times rated current for an 0.8 power-factor motor. For these figures it is assumed that the rated current is 2.7 per unit. For any motor (of any power-factor rating) whose rated field current has a different per-unit value, the field-current scales of these figures should be multiplied by 2.7 and divided by the per-unit value of rated field current.

Appendix C. Method of Analysis

Approach to Problem

To determine whether or not a motor would remain in synchronism after application of a particular load under a particular set of conditions, a calculation was made resulting in a plot of rotor angle versus time. If this angle (by which the rotor axis is out of phase with the line voltage) does not exceed 180 degrees and approaches a constant value less than 90 degrees, it was concluded that the motor would not pull out.

To determine the transient pull-out torque of a particular machine under a particular set of conditions, the calculation just mentioned was made for several different values of applied load. By this means, two load values were found, one only slightly greater than the other, the larger of which causes pull-out and the lesser of which does not. The transient pull-out torque lies between these two values.

The effects of variation of machine constants and of excitation system performance were studied by determining the transient pull-out torques for various combinations of machine constants and operating conditions

Table II. Schedule of Cases

Case	Initial Load	Initial Field Current Per Cent of Rated*	Field-Voltage Response Per Cent of Rated* Per Second	Final Field Voltage Per Cent of Rated*	Synchronous Reactance X_d	Quadrature Reactance X_q	Leakage Reactance X_l	Transient Reactance X_d'	Damping Constant D	Inertia Constant H	Frequency f Cycles Per Second	Open-Circuit Time Constant (Seconds) T_{d0}'	Saturation Characteristics
1	0	41.5	110	180	1.63	1.00	0.16	0.33	10	1.79	60	3.42	Figs. 13, 14
2	0	41.5	110	180	1.63	1.00	0.16	0.415	10	1.79	60	3.42	Figs. 13, 14
3	0	41.5	110	180	1.63	1.00	0.16	0.50	10	1.79	60	3.42	Figs. 13, 14
4	0	74	110	180	1.63	1.00	0.16	0.33	10	1.79	60	3.42	Figs. 13, 14
5	0	100	110	180	1.63	1.00	0.16	0.33	10	1.79	60	3.42	Figs. 13, 14
6	0	41.5	110	180	1.63	1.00	0.16	0.33	10	1.79	60	2.0	Figs. 13, 14
7	0	41.5	110	180	1.63	1.00	0.16	0.33	10	1.79	60	1.0	Figs. 13, 14
8	0	41.5	Infinite	300	1.63	1.00	0.16	0.33	10	1.79	60	3.42	Figs. 13, 14
9	0	41.5	Infinite	300	1.63	1.00	0.16	0.33	10	1.79	60	2.0	Figs. 13, 14
10	0	41.5	Infinite	300	1.63	1.00	0.16	0.33	10	1.79	60	1.0	Figs. 13, 14
11	0	41.5	Infinite	160	1.63	1.00	0.16	0.33	10	1.79	60	1.0	Figs. 13, 14
12	0	41.5	Infinite	100	1.63	1.00	0.16	0.33	10	1.79	60	1.0	Figs. 13, 14
13	0	41.5	Infinite	160	1.63	1.00	0.16	0.33	10	1.79	60	3.42	Figs. 13, 14
14	0	41.5	Infinite	100	1.63	1.00	0.16	0.33	10	1.79	60	3.42	Figs. 13, 14

* −0.8 power-factor motor (rated field voltage and current assumed = 2.7 per unit).

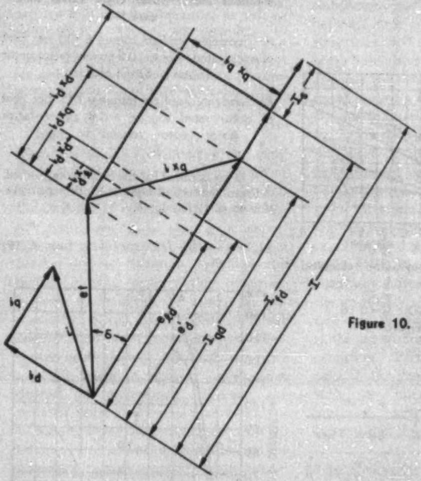


Figure 10. Synchronous-motor vector diagram

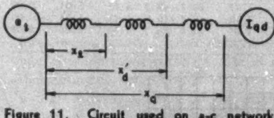


Figure 11. Circuit used on a-c network analyzer

A schedule of cases studied is given by Table II. From the results of the various cases investigated, the effects of design factors were determined and plotted on Figures 2, 3, 4, and 5.

It may be observed from the form of the equivalent circuits that the pull-out power is increased in exact proportion to reduction in machine reactances if the ratios between these reactances is held constant. Therefore, all calculations were made with a single conservatively high value of direct-axis synchronous reactance, and it was not necessary to investigate separately the effect of changes in this reactance.

Method of Calculation

On the basis of the assumptions listed in Appendix *A*, Figure 10 may be drawn to represent the motor at all times, before, during, and after application of load.[2,4] Figure 11 corresponds to that portion of Figure 10 which is independent of saturation and of saliency of the poles of the rotor. The circuit of Figure 11 was set up on the a-c network analyzer,[3] illustrated in Figure 12. Reactance values representative of synchronous motors were used for the circuit of Figure 11. Readings of e_{td}, e_d', and P_e were obtained for a range of values of δ and I_{qd}. Using these readings and the relationships given on Figures 10 and 14, corresponding values of I, which took pole saliency and saturation into account, were calculated. From the analyzer readings and the calculated values of I, curves such as those of Figures 15 and 16 were plotted. These were used in making step-by-step calculations of δ and I versus time. A sample step-by-step calculation is given by Table III.

The calculations of Table III proceed from left to right for each time interval, using last-calculated values of quantities required for making each calculation.

A time interval of 0.05 second was used for all calculations, since this was found by trial to give satisfactory accuracy. Use of a smaller interval will not materially increase accuracy.

The quantities given in Table III were calculated as follows:

The initial conditions were recorded for $t = -0.05$ second.

By definition

$$\delta_1 = \delta_0 + \Delta\delta_0 \text{ etc.} \tag{1}$$

and

$$e_{d1}' = e_{d0}' + \Delta e_{d0}' \text{ etc.} \tag{2}$$

I is determined from Figure 15 for the previously calculated values of δ and e_d'.

P_e is determined from Figure 16 for the previously calculated values of δ and I.

Figure 12. A-c network analyzer used for study of synchronous motors with automatic excitation control

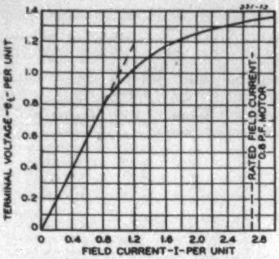


Figure 13. No-load saturation curve

P_L is the assumed applied load for the case studied.

From assumption 4

$$P_D = \frac{D}{360(f)(\Delta t)}(\Delta v)(\Delta t) \tag{3}$$

Since the sum of all torques on the rotor is zero

$$P = P_L - P_e - P_D \tag{4}$$

Since the decelerating torque

$$P = \frac{2H}{360f} \times \frac{dv}{dt} \tag{5}$$

$$(\Delta v)(\Delta t) = \frac{360f(\Delta t)^2}{2H} P \tag{6}$$

except for the first interval, for which from equation 55 of Reference 4,

$$(\Delta v_1)(\Delta t) = \frac{360f(\Delta t)^2}{4H} P_1 \tag{7}$$

By definition

$$\Delta\delta = (v_1)(\Delta t) = (v_0)(\Delta t) + (\Delta v_0)(\Delta t) \text{ etc.} \tag{8}$$

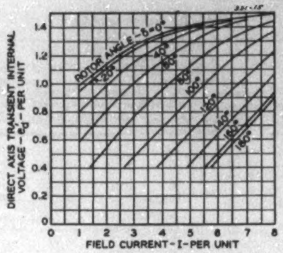


Figure 15. Direct-axis transient internal voltage, e_d'

$X_d = 1.63$ $X_q = 1.00$
$X_d' = 0.33$ $X_l = 0.16$
Saturation per Figure 14

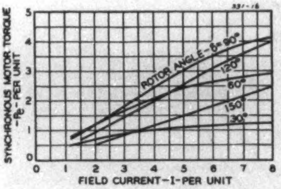


Figure 16. Synchronous-motor electrical torque

$X_d = 1.63$ $X_q = 1.00$ $X_l = 0.16$
Saturation per Figure 14

Table III. Sample Step-By-Step Calculation—Case 1A

Suddenly Applied Load, $P_L = 2.25$ Per Unit

Step	t	δ	e_d'	I	P_e	P_L	P_D	P	(Δv)(Δt)	Δδ	E	E-I	$\Delta e_d'$
0	−0.05	0	1.00	1.12	0	0	0	0	0	0	1.12	0	0
1	0.00	0	1.00	1.12	0	2.25	0	2.25	17.0	17.0	1.12	0	0
2	0.05	17.0	1.00	1.28	0.32	2.25	0.16	1.77	26.7	43.7	1.27	−0.01	−0.00
3	0.10	60.7	1.00	3.00	1.69	2.25	0.41	0.15	2.3	46.0	1.42	−1.58	−0.02
4	0.15	106.7	0.98	5.85	3.33	2.25	0.43	−1.51	−22.8	23.2	1.57	−4.28	−0.06
5	0.20	129.9											

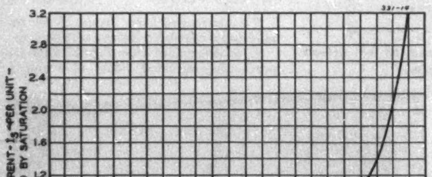


Figure 14. Field current required by saturation

$e_s = e_{td} + 0.12 I_{fd}$
$I = I_{fd} + I_s$

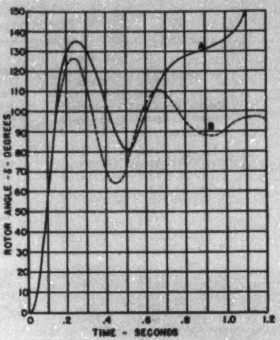


Figure 17. Calculated performance of synchronous motor—rotor angle versus time—case 1

A—Suddenly applied torque = 300 per cent of motor rated torque − 0.8 power-factor motor (motor pulls out of step)

B—Suddenly applied torque = 285 per cent of motor rated torque − 0.8 power-factor motor (motor remains in step)

E is as assumed for the case being studied.

From the second equation following equation 4a of Reference 5,

$$\Delta e_d' = \frac{\Delta t}{T_{do}'}(E - I) \tag{9}$$

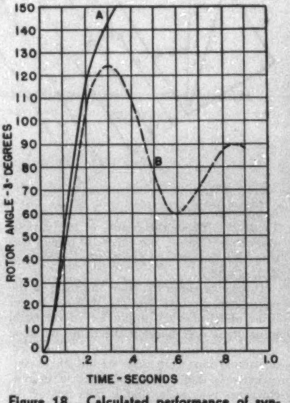


Figure 18. Calculated performance of synchronous motor—rotor angle versus time—case 3

A—Suddenly applied torque = 220 per cent of motor rated torque − 0.8 power-factor motor (motor pulls out of step)

B—Suddenly applied torque = 200 per cent of motor rated torque − 0.8 power-factor motor (motor remains in step)

A Multiorifice Interrupter for High-Voltage Oil Circuit Breakers

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Synopsis: High-voltage oil circuit breakers for high-speed operation generally use some form of multibreak contact arrangement. These breakers perform satisfactorily but lack the simplicity of the conventional slower-speed two-break construction. The development of an unusually effective multiorifice oil-flow interrupting unit now makes it possible to reduce the required number of breaks to the point where even three-cycle 230-kv breakers appear practical with only two interrupting units per pole.

High-power laboratory tests on low magnetizing and charging currents, as well as short-circuit currents corresponding to interrupting ratings of 2,500,000 kva and above, have demonstrated the exceptional arc-extinguishing effectiveness and low arc-energy dissipation of this multiorifice interrupter for high-speed oil circuit breakers.

PRESENT-DAY requirements relating to the manufacture and operation of high-voltage oil circuit breakers demand the least use of critical materials and the minimum time out of service for inspection and maintenance. Modern circuit-breaker designs, therefore, must have the highest possible interrupting efficiency in order to limit the arc-energy dissipation during circuit opening with attendant deterioration of oil, contacts, and interrupter parts. Since physical size and material usage as well as maintenance depend on interrupting efficiency, the designer must give primary consideration to an improved interrupter in order to achieve any essential simplification of manufacture and operating procedure.

In the voltage range from 115 kv to 230 kv, most oil breakers for eight-cycle operation and some for five-cycle time are constructed with two interrupters in a pole unit.[1,2] The mechanical arrangement is simple, and the potential advantages associated with an improved interrupter are to be gained therefore from decreased energy release, which leads to decreased size and improved performance. For three-cycle operation at the higher voltages, the structure becomes necessarily more complicated because higher operating speeds are required, and in general it has been necessary to utilize a multiplicity of interrupting devices and contacts in each pole unit.[3]

While the multibreak design has shown satisfactory performance, a comparison between such contacts and the simpler structures used in the conventional two-break assemblies for eight-cycle operation suggests the desirability of reversing this trend toward a large number of breaks. By making use of newly developed and highly effective oil-flow interrupting units, it appears practical to build even the highest-speed breakers with but two interrupting units per pole.

An interrupting function of the high-voltage high-speed circuit breaker is the interruption of line-charging current when little or no load is being carried. Operators have expressed concern over the possibility of producing high destructive overvoltages as a result of restriking of the arc within the breaker during such operation. Particular attention has been paid to the possibility, and test results with a new interrupter are shown which minimize restriking by prompt effective interruption of low currents.

The Evolution of a New Interrupter

The problem of high kilovolt-ampere interruption under oil has always been one of achieving a sufficient rate of deionization following a current zero, so that a high-voltage gradient can be quickly withstood across the open contacts with the minimum amount of energy dissipated in the arc prior to the current zero. Obviously, if means can be found to increase the volts per inch which can be interrupted, then a comparatively simple structure employing only two interrupters per pole unit is possible, even with the short contact separation which can be achieved in three cycles. The minimum mechanical time required from energizing the trip coil until the contacts part on a three-cycle breaker is approximately $1^1/_2$ cycles. This means that the maximum contact separation must be achieved in $1^1/_2$ cycles, and with reasonable mechanical speeds the contacts can be parted approximately three inches in this time. On the basis of two interrupters for 230 kv, and assuming line to ground of 132 kv across a pole unit, the interrupting voltage gradient to be achieved therefore must be in excess of 22,000 volts per inch.

The interrupting ability of an oil circuit breaker has been attributed to the turbulence produced within the gas bubble formed by the arc when the contacts are separated.[4] Such turbulence requires continuous formation of gas from the oil, and intimate association between the arc and the walls of oil which define the bubble is thus required. In the plain-break circuit breaker proximity between the arc and the oil walls is not readily achieved, since the pressure generated within the bubble quickly drives the oil away from the arc core. The interrupting ability of the plain break has been greatly improved by enclosing it in various forms of arc-control devices. These devices limit the extent to which gases may expand within the oil bubble, and therefore the pressure within it is increased to such an extent that intimate contact between the arc and the oil is achieved. These pressures, however, also increase the arc voltage and the energy dissipated.

It has been found that the interruption can be improved much by flowing oil past the arc by separately applying pressure. This flowing oil stream has the beneficial effects of supplying cool oil and fresh un-ionized gas to the arc core, and of tearing bubbles of spent ionized gas away from the region surrounding the core. Thus the size of the gas bubble in which the arc plays can be limited without developing excess pressures within it, and the desired proximity of oil and arc

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Results of typical step-by-step calculations are shown on Figures 17 and 18.

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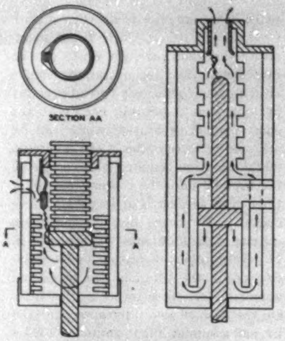


Figure 1. Oil-flow interrupters for high-voltage oil circuit breakers

Left—Vertical-flow interrupter
Right—Center-flow interrupter

can be achieved without high arc energy. It has been found also that, if the oil is caused to flow vertically along the axis of the arc, the disruptive effect of the oil stream itself on the arc core will be minimized. The arc in this case is not lengthened or bent about barrier plates; its voltage drop and energy loss are therefore low. Nevertheless, at current zero the gases formed from the decomposed oil are highly turbulent, and deionization is exceptionally effective.[5]

B. P. Baker has previously described a vertical-flow interrupter and its application to oil-poor circuit breakers.[6] This interrupter consisted of a novel structure for developing an oil flow by mechanical means, the oil being directed longitudinally along the arc in a restricted channel vented at one end only, as shown in Figure 1a. This device was remarkable for the low arc voltage, and therefore low arc energy and oil depreciation, associated with the interruption of high short-circuit powers exceeding 2,000,000 kva. This device was tested both in the laboratory and in the field.

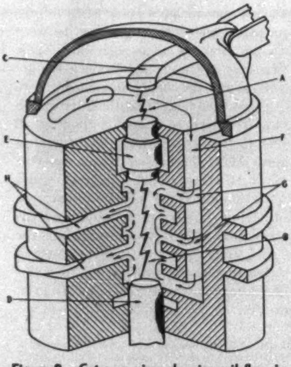


Figure 2. Cutaway view showing oil flow in multiorifice grid interrupter

A—Pressure break
B—Interrupting break
C—Upper contact
D—Lower contact
E—Intermediate contact
F—Oil-flow channel
G—Inlet passages
H—Exhaust passages

Experimental tests were made also on a center-flow device in which a contact was drawn down through a succession of orifices, while a flow of oil was driven longitudinally around the contacts and up along the arc through the orifices and out a vent at the top, as shown in Figure 1b.

Experience with these interrupters indicated that a high-voltage gradient could be developed only over a limited axial distance, because the oil stream soon became contaminated with ionized gas and was therefore not effective throughout the entire length of the interrupter. This phenomenon suggested that the effectiveness of the device can be greatly enhanced by building it in the form of a multiplicity of orifices through which the arc is drawn and through which the oil is caused to flow in an axial direction. If a fresh supply of oil is directed to each orifice, and if the used gases and oil are vented at each orifice, then the high-voltage gradient can be maintained in any desired number of inches. Consequently, the structure shown in Figure 2 was constructed. In such a structure the arc is not unduly disturbed prior to current zero, and its length is kept at a minimum. The pressures are definitely limited by the comparatively large open venting arrangement, and at the same time the deionization at current zero is extremely effective.

The oil flow can be produced in a number of ways, for example, by means of a piston which provides the necessary pressure. It is also feasible to utilize a second arc in the same interrupting structure which generates gas and thus forms sufficient driving pressure to cause the oil flow through the orifices and the main interrupting arc. This means has the advantages of producing a greater flow of oil as the current to be interrupted is increased. It also does not increase the mechanical load as with an oil piston. Consequently, the second pressure arc has been adopted in conjunction with the multiple-orifice structure and provides a simple small and efficient over-all interrupter as shown in Figure 5.

Description of the Multiorifice Interrupter

The cutaway view of the multiorifice interrupting unit shown in Figure 2 illustrates the essential features of the device. For maximum speed of arc rupture, a pressure break (*A*) and an interrupting break (*B*) are opened simultaneously by raising an upper contact (*C*) and drawing down a moving contact (*D*) away from a relatively stationary intermediate contact (*E*). Gas generation by the upper arc places oil under pressure and causes it to flow through a pair of oil-flow channels (*F*) to three pairs of inlet passages (*G*). Two pairs of exhaust passages (*H*) are located between the inlets, all channels being separated by fiber plates with a central orifice (*J*) in which the arc is drawn.

The arrows indicate the flow of oil which serves to keep the arc centered in the orifices, sweeping the arc products out of the vents and producing an extremely high rate of deionization of the

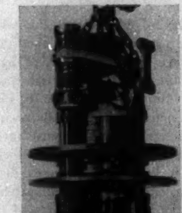


Figure 3. 287-kv three-cycle multiorifice grid-contact assembly with shields removed

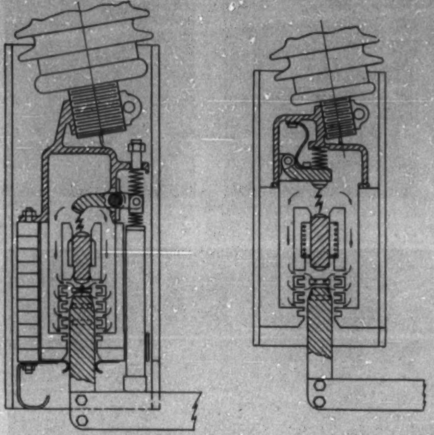
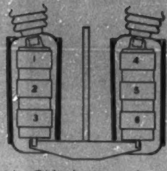


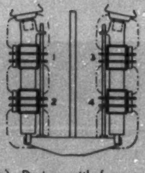
Figure 4. Multiorifice grids for 230-kv five-cycle breaker and 138-kv eight-cycle breaker

Left—230-kv five-cycle breaker multiorifice-grid assembly

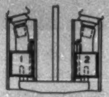
Right—138-kv eight-cycle breaker multiorifice-grid assembly



(a). Old design with six radial field grids



(b). Design with four multiorifice grids



(c). New design with two multiorifice grids

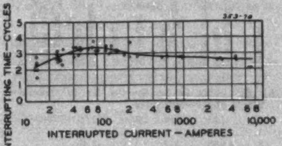
Figure 6. Evolution of 230-kv three-cycle breaker designs for 2,500,000 kva

arc space at current zero. The design lends itself naturally to the selection of more or less inlet–orifice–outlet units according to the voltage rating. Since the oil-flow passages are arranged in parallel, the quantity of oil to be set in motion by the driving pressure will be greater for increased voltage ratings, and a longer pressure arc is provided to overcome the increased pressure drop in the vertical oil-flow channels.

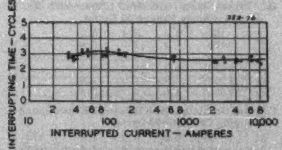
For maximum speed and effectiveness of arc rupture, the pressure break and main interrupting break are opened practically simultaneously as shown in Figure 4a, although satisfactory slower-speed performance is obtained with a consecutive sequence of contact parting as shown in Figure 4b. With the arrangement of Figure 4a, five-cycle operation is obtained with normal contact speed sufficient to give adequate contact separation in $2^1/_2$ cycles. In order to obtain three-cycle operation, either four grids per pole unit may be used, or the contact speed may be increased to give longer separation in $1^1/_2$ cycles, and two grids may be used. In this case, heavier construction must be incorporated into the grids to withstand somewhat higher pressures. The pole-unit levers also must be made heavier in order to stand the higher operating speeds and the effects of heavier accelerating springs. This higher contact speed can be obtained by increasing the breaker lift-rod speed and by operating the grid contacts [illegible] multi[illegible] tact [illegible] rod. A multigrid assembly for three-cycle operation at 287 kv has been built and tested with six units in series per pole. Three of these multiorifice grids are shown in Figure 3, arranged with a side operating rod for actuating all contacts simultaneously.

In the grids shown in Figures 4a and 5, an insulating operating rod along the side of each contact assembly is attached to a rocking arm which operates the contact in the pressure chamber at the top of the grid unit. Clamped between the upper and lower contacts in the closed position is the intermediate contact which separates the pressure and interrupting chambers. The operating rod is raised during the closing operation by the bridging contact crossbar which also carries the lower moving contact. This contact is withdrawn from the grid in the open position to provide electrical isolation. An accelerating spring is compressed during closing, as are the contact springs, and both provide initial acceleration. On opening, the main lift rod is tripped, and

Figure 5. 230-kv five-cycle multiorifice grid-contact assembly with shield removed



(a). 287-kv breaker—3,500,000 kva six grids—198 kv to ground single phase



(b). 230-kv breaker—2,500,000 kva four grids—132 kv to ground single phase

Figure 7. Multiorifice-grid performance in three-cycle high-voltage breakers

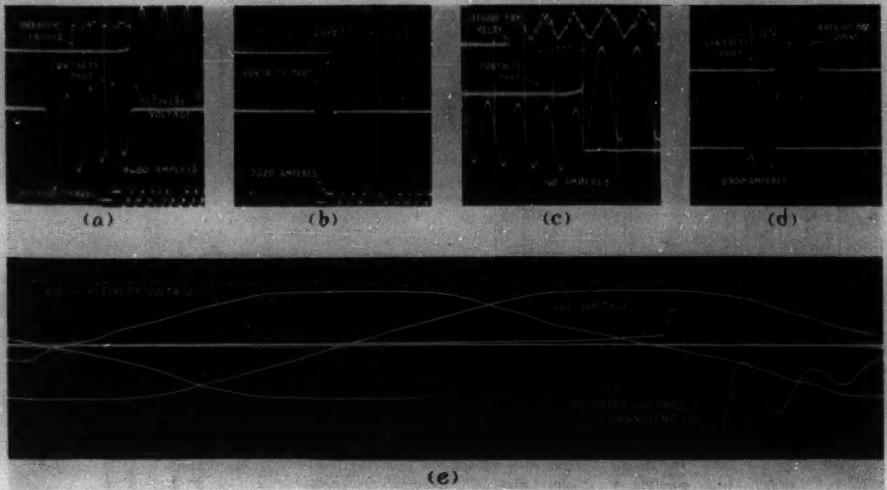


Figure 8. Oscillograms of short-circuit tests on three-cycle multiorifice grid breakers

(a). Six grids, 4,680 amperes, 198 kv to ground

(b). Six grids, 7,020 amperes, 198 kv to ground

(c). Four grids, 162 amperes, 132 kv to ground

(d). Four grids, 8,300 amperes, 132 kv to ground

(e). Cathode-ray oscillogram of same test as 8c

the bridging crossbar drops away from the grids carrying the lower contact. The upper contact opens simultaneously, and the arc is extinguished just before the crossbar leaves the grid to provide an insulating disconnect break in clear oil. Voltage distribution is provided by resistors, while the voltage gradient in the neighborhood of metallic parts is relieved by well-rounded metallic shields.

Figure 6 shows schematically the results which have been achieved. In 6a the earlier form of 230-kv three-cycle grid arrangement using six grids is illustrated. In 6b by using the multiorifice design, it has been possible to obtain very short arcing time over the entire current range with only four grids. In 6c two grids only are shown for the same purpose, and tests indicate their adequacy for future breaker designs.

Resistors for Voltage Control

A resistor assembly consisting of graphite-impregnated molded blocks mounted in a fiber tube is connected across each multiorifice grid stack, primarily for the purpose of improving the division of voltage among the series interrupters.[8] Figure 15 shows a group of curves indicating the calculated division of voltage between the contact assemblies on the high potential and grounded terminals of a typical high-voltage steel tank breaker as a function of the parallel resistance value for various

Table I. 287-Kv Three-Cycle Breaker Data—3,500,000 Kva (7,000 Amperes)

Test No.	Interrupted Current (RMS Amperes)	Arcing Time (Cycles)	Main Gap Per Grid (Inches)	Interrupting Time (Cycles)	Maximum Voltage Across Breaker (Per Cent Normal Crest)
A. Six Grids in Series—198 Kv to Ground—Single Phase—60 Cycles					
1	160	1.4	2.0	2.8	
2	2,640	1.2	1.6	2.6	
3	4,680	1.2	1.6	2.6	
4	4,520	1.3	1.8	2.7	
5	4,600	1.4	2.0	2.8	
6	7,020	0.7	0.7	2.1	
7	7,020	0.7	0.7	2.1	
B. Three Grids (One-Half Pole)—198 Kv to Ground—Single Phase—60 Cycles					
1	160	1.6	2.4	3.0	
2	170	1.4	2.0	2.8	
3	2,440	1.4	2.0	2.8	
4	3,850	1.1	1.4	2.5	
5	3,700	1.2	1.6	2.6	
6	5,900	1.0	1.2	2.4	
C. One Grid (One-Sixth Pole)—33 Kv to Ground—Charging Current—60 Cycles					
1	8	0.2	0.2	1.6	177
2	8	0.1	0.1	1.5	173
3	24	0.1	0.1	1.5	192
4	24	0.3	0.3	1.7	176
5	48	0.9	1.0	2.3	165
6	48	0.8	0.8	2.2	176
7	96	0.8	0.8	2.2	186
8	96	0.8	0.8	2.2	118

Table II. 230-Kv Three-Cycle Breaker Data—2,500,000 Kva (6,300 Amperes)

Test No.	Interrupted Current (RMS Amperes)	Arcing Time (Cycles)	Main Gap Per Grid (Inches)	Interrupting Time (Cycles)
A. Four Grids in Series—132 Kv to Ground—Single Phase—60 Cycles				
1	30	1.6	2.1	3.0
2	162	1.7	2.3	3.1
3	675	1.5	1.9	2.9
4	4,050	1.3	1.5	2.7
5	7,420	1.2	1.3	2.6
6	7,900	1.2	1.3	2.6
7	8,300	1.0	0.9	2.4
B. Four Grids in Series—198 Kv to Ground—Single Phase—60 Cycles				
1	1,150	1.2	1.3	2.6
2	2,650	1.5	1.9	2.9
3	2,400	1.4	1.7	2.8
4	3,350	1.3	1.5	2.7
5	3,350	1.5	1.9	2.9
6	6,200	1.2	1.3	2.6
7	6,560	1.5	1.9	2.9

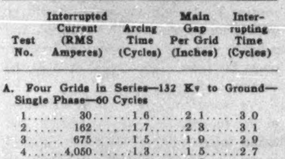
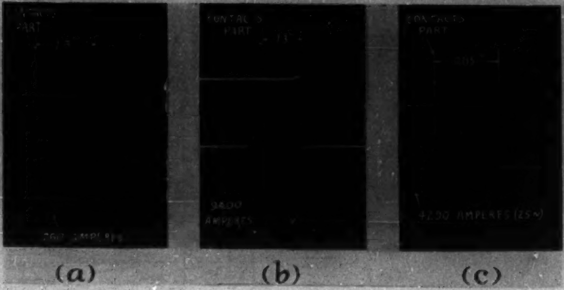


Figure 9. 230-kv five-cycle circuit-breaker performance—two grids—132 kv to ground



(a). Two grids, 260 amperes, 132 kv to ground (b). Two grids, 9,400 amperes, 132 kv to ground (c). Two grids, 4,290 amperes, 82.5 kv to ground

Figure 10. Oscillograms of tests on 230-kv five-cycle multiorifice grid breaker

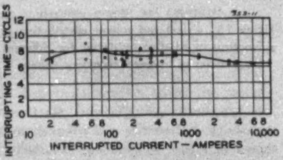


Figure 11. 138-kv eight-cycle circuit-breaker performance—two grids—88 kv to ground

natural frequencies of the transient recovery voltage. The combination of resistance and capacitance involved causes the voltage across the two contact assemblies to be out of phase with each other, and thus the corresponding percentages add up to somewhat more than 100 per cent. The resistance value selected for a 287-kv multiorifice grid design was 360,000 ohms per breaker terminal, giving according to Figure 15 as good or better distribution than with capacitance shielding[7] for all frequencies up to about 2,000 cycles per second. It should be noted that, after the high-frequency transient has been damped out, the normal-frequency voltage will be divided 50 per cent—50 per cent by the resistance. During the interruption of charging currents the voltage distribution is better with resistance dividers than with capacitance shielding, since the voltage builds up across the breaker contacts at normal frequency. Proper voltage distribution during the interruption of charging currents forces each grid to bear its full share of restored voltage. Thus the resistor in combination with sufficient contact speed and oil driving power quickly brings the total dielectric strength of all grids sufficiently high to prevent continued restrikes which might multiply the transient overvoltage. A liberal and quickly achieved contact separation for the oil driving arc has been found to provide good driving pressures even with low currents.

Table III. 230-Kv Five-Cycle Breaker Data—2,500,000 Kva (6,300 Amperes)

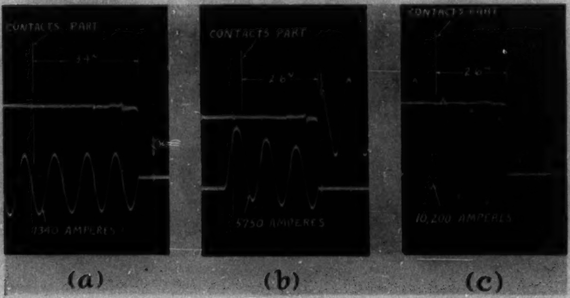
Test No.	Interrupted Current (RMS Amperes)	Arcing Time (Cycles)	Main Gap Per Grid (Inches)	Interrupting Time (Cycles)	
A. Two Grids in Series—132 Kv to Ground—Single Phase—60 Cycles					
1	260	1.8	2.4	4.3	
2	1,060	2.0	2.8	4.5	
3	3,920	1.6	2.0	4.1	
4	5,980	1.5	1.9	4.0	
5	5,340	1.8	2.4	4.3	
6	5,450	1.5	1.9	4.0	
7	7,500	1.7	2.2	4.2	
8	9,400	1.3	1.6	3.8	
B. Two Grids in Series—82.5 Kv to Ground—Single Phase—25 Cycles					
				Time Base	
				25 Cycles	60 Cycles
1	495	1.10	4.1	2.15	5.2
2	485	0.85	3.0	1.90	4.6
3	965	0.90	3.2	1.95	4.7
4	925	1.05	3.9	2.10	5.0
5	3,010	0.80	2.6	1.85	4.4
6	3,080	0.85	2.8	1.90	4.6
7	4,290	0.85	3.0	1.90	4.5
8	4,290	0.85	3.0	1.90	4.6

Lower values of ohmic resistance have been tried experimentally but appear unnecessary with this type of grid. Resistors low in ohmic value are rather bulky to be able to handle the high E^2/R power losses even for time intervals of only a few cycles.

Another advantage obtained with even a moderately high value of resistance in parallel with the interrupter is the reduced voltage gradient along the insulation with the contacts in the open position. The effect of the resistance connection is to keep both ends of the contact assembly at the same potential while the disconnect gap is open. Large well-rounded shields grade this potential off into the oil.

Discussion of Test Data

The curves in Figure 7 show the interrupting time as a function of interrupted current for a three-cycle 287-kv six-unit multiorifice grid breaker tested at 198 kv



(a). Two grids, 1,340 amperes, 88 kv to ground (b). Two grids, 5,750 amperes, 88 kv to ground (c). Two grids, 10,200 amperes, 88 kv to ground

Figure 12. Oscillograms of tests on 138-kv eight-cycle multiorifice grid breaker

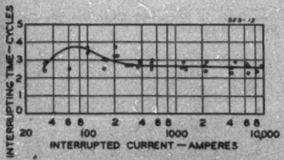
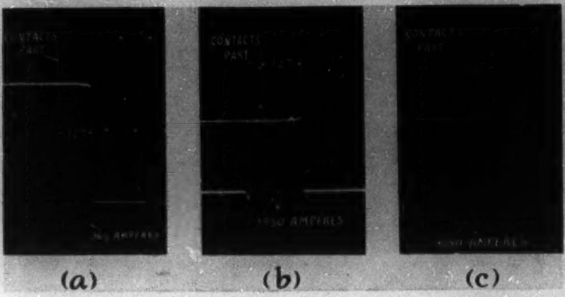


Figure 13. 230-kv three-cycle circuit-breaker performance—two grids—132 kv to ground



(a). Two grids, 360 amperes, 132 kv to ground

(b). Two grids, 3,950 amperes, 198 kv to ground

(c). Two grids, 9,350 amperes, 132 kv to ground

Figure 14. Oscillograms of tests on 230-kv three-cycle multiorifice grid breaker

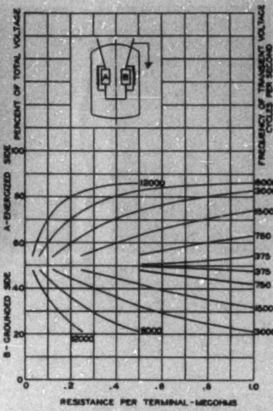


Figure 15. Calculated voltage distribution as a function of parallel resistance for different frequencies of transient recovery voltage

to ground single-phase, and a three-cycle 230-kv four-unit breaker tested at 132 kv to ground. The first curve, Figure 7a, covers a current range from 14 amperes to 7,000 amperes at a voltage 20 per cent above the normal line-to-ground potential on a 287-kv system. The test at maximum current is a direct demonstration of the ability to meet a 3,500,000 kva interrupting-capacity rating. The interrupting time was less than three cycles for all currents above 210 amperes (six per cent of rating), and did not exceed 3.8 cycles at any current. Table I contains test data for the curve in Figure 7a as well as test data with the full 198 kv across only *one-half* the pole unit of three grids in series. This demonstrates a 100 per cent factor of safety in voltage interrupting ability. Also included in the table are charging-current interrupting tests with one-sixth voltage, or 33 kv, across one of the six multiorifice grids in series with a capacitor bank in the laboratory. A current range from 8 to 96 amperes was available; all interruptions took place in less than three cycles. Part *C* of Table I shows that the maximum voltage across the interrupter during these tests did not exceed 192 per cent where the normal crest voltage is taken as 100 per cent.

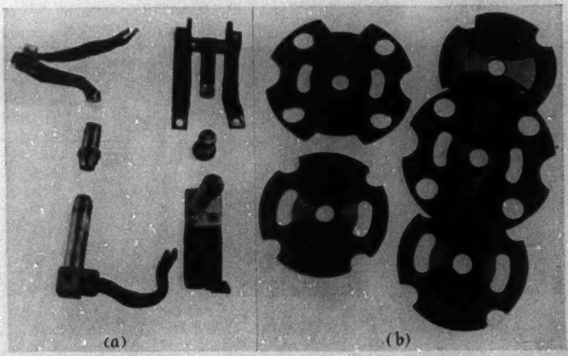
The second curve, Figure 7b, covers a current range from 30 amperes to 8,300 amperes at normal line-to-ground voltage for a 230-kv system, the maximum current corresponding to a three-phase short circuit of 3,300,000 kva. The maximum interrupting time in the low-current range was only 3.4 cycles. By simply increasing the mechanical speed of contact opening, a flat three-cycle characteristic can be obtained over the entire current range. Table II includes not only test data at 132 kv to ground, but also at 50 per cent higher voltage or 198 kv to ground.

Representative oscillograms for these three-cycle multiorifice grid breakers are shown in Figure 8, including a cathode-ray oscillogram of one of the low-current tests showing that no greater than normal amplitude in the recovery-voltage transient is obtained.

Figure 9 gives a curve of interrupting performance for 230-kv simultaneous-contact-parting multiorifice grids of the type illustrated in Figure 5a for five-cycle rating. The maximum current interrupted was 9,800 amperes at 132 kv, over 50 per cent above the rated interrupted capacity. The remarkable ease

Table IV. 138-Kv Eight-Cycle Breaker Data —2,500,000 Kva (10,500 Amperes)

Test No.	Interrupted Current (RMS Amperes)	Arcing Time Both Gaps (Cycles)	Main Gap Per Grid (Inches)	Interrupting Time (Cycles)
Two Grids in Series—88 Kv to Ground—Single Phase—60 Cycles				
1	20	3.3	2.7	7.1
2	52	5.2	6.0	9.0
3	175	4.2	4.4	8.0
4	460	3.7	3.4	7.5
5	1,340	3.4	2.9	7.2
6	3,800	2.7	1.6	6.5
7	3,690	2.6	1.4	6.4
8	3,800	2.8	1.9	6.6
9	5,750	2.6	1.4	6.4
10	6,200	2.3	0.9	6.1
11	10,200	3.0	2.2	6.8
12	10,200	2.6	1.4	6.4



(a). Upper, intermediate, lower contacts

(b). Inlet, orifice, and vent plates

Figure 16. Multiorifice grid parts after interrupting tests

Table V. 230-Kv Three-Cycle Breaker Data—2,500,000 Kva (6,300 Amperes)

Test No.	Interrupted Current (RMS Amperes)	Arcing Time (Cycles)	Main Gap Per Grid (Inches)	Interrupting Time (Cycles)
A. Two Grids in Series—132 Kv to Ground—Single Phase—60 Cycles				
1	32	1.5	3.0	2.9
2	360	1.2	2.1	2.6
3	4,230	1.2	2.1	2.6
4	6,600	1.3	2.4	2.7
5	6,600	0.9	1.3	2.3
6	6,370	1.4	2.7	2.8
7	8,350	1.0	1.5	2.4
8	9,350	1.3	2.4	2.7
B. Two Grids in Series—198 Kv to Ground—Single Phase—60 Cycles				
1	240	1.4	2.7	2.8
2	485	1.7	3.0	3.1
3	963	1.1	1.8	2.5
4	3,950	1.2	2.1	2.6
5	4,510	1.3	2.4	2.7

with which such heavy short circuits are cleared gives evidence of the low arc voltage and the energy of the multiorifice interrupter. This is especially important for 25-cycle application where the arcing time tends to be slightly longer than at 60 cycles, because of the greater time interval before a current zero occurs at which the contacts are parted sufficiently to interrupt. Table III includes not only data at 132 kv 60 cycles, but also at 82.5 kv 25 cycles. Oscillograms of representative low- and high-current tests are shown in Figure 10.

The interrupting performance of the eight-cycle consecutive-contact-parting multiorifice grids for 138 kv, as illustrated in Figure 5b, is shown by Figure 11. The current range covered was from 16 amperes to over 10,000 amperes at 88 kv to ground, or 15 per cent above normal line-to-ground voltage. Data are given in Table IV, and oscillograms for three tests are shown in Figure 12. The simplicity of contact structure makes this design very attractive for application where high-speed operation is not essential.

It has been pointed out earlier in the paper that the ultimate of three-cycle operation at 230 kv with only two units per pole has been demonstrated to be a practical possibility. Using a pair of multiorifice grids similar in operation to the one shown in Figure 5a but equipped with a multiplying linkage for operating the lower moving contacts at high speed, the results shown in Figure 13 were obtained. Currents from 30 to 9,300 amperes at 132 kv to ground single phase were interrupted satisfactorily. All short circuits above 210 amperes were cleared within three cycles. Data are given in Table V not only at 132 kv but at 198 kv to ground single-phase, and Figure 14 includes three representative oscillograms.

All interrupting tests were made in the high-power laboratory described by MacNeill and Batten[9] last year. The circuit voltage-recovery rates during the tests were quite high since the breakers were connected to the transformers with short cable lengths. All tests at 132 kv were on a circuit having a maximum circuit voltage-recovery rate of 3,500 volts per microsecond. At 198 kv the corresponding rate was 5,500 volts per microsecond.

An examination of all oscillograms shows not only short arcing time but very low arc voltage. The energy dissipated in the new interrupter where two grids are used for five-cycle operation is approximately one third to one half of that commonly considered acceptable in present-day interrupters. For three-cycle operation at 230 kv, the reduction in the number of interrupters lowers the total energy dissipation to approximately one fourth that of current designs.

There is a complete absence of demonstration during interruption. Measurements of tank pressure with two multiorifice grids show maximum pressure impulses of only 60 pounds when interrupting high currents of approximately 10,000 amperes.

Multiorifice grids have been tested for large numbers of consecutive high-current interrupting tests. Examination of the parts after such tests shows very little burning and erosion of the orifice plates or other grid parts. For example, Figures 16a and 16b show several orifice plates and contacts taken from a grid which had withstood 22 interrupting tests from 200 to 10,000 amperes. All but eight of these tests were above 3,000 amperes.

Conclusions

A multiorifice type of interrupter has been found to have excellent interrupting ability in the high-power laboratory. Its arc-energy dissipation is markedly lower than with present interrupters. In the 230-kv three-cycle field the greatest improvements are possible, because the number of interrupters per pole unit can be reduced from six to two.

The interruption of low currents is achieved with short arcing time by this interrupter, and means have been introduced into its design to restrict overvoltages due to restriking.

A saving in critical materials is made possible by the use of this interrupter which is consistent with the current war effort.

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Substation-Transformer Emergency Overloading Practice

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MEMBER AIEE

Synopsis: Although much has been written in recent years regarding allowable overloading of transformers, the practical matter of matching existing load curves with such allowable limits has continued to present some difficulties. The interim report of the AIEE transformer subcommittee gives one approach to the problem, taking into account the ambient temperature and the duration of the overload. However, in order to take full advantage of the transformer's inherent load-carrying ability, it is necessary to consider additional factors such as the individual transformer characteristics and the specific shape of the overload curve.

This paper shows a method by which all of the influencing factors can be taken into account easily. Allowable overloads thus determined are often considerably higher than those indicated by the AIEE method. The application of the proposed method is sufficiently simple to justify a semiannual analysis of expected overloads with respect to substation-transformer emergency ratings. It consists in

1. Determining the allowable 24-hour steady overload that can be carried by the transformer without exceeding certain agreed-upon limits.
2. Converting the specific load curve to a 24-hour steady load which would cause equal aging of the transformer insulation.

In this manner both the transformer load-carrying ability and the actual irregular load are reduced to a common basis for ready comparison. A 24-hour overload period is ordinarily of ample duration to allow for replacement or repair of faulty equipment.

Nomographs are included for quick determination of allowable transformer 24-hour overloads, taking into account ambient temperature, top-oil rise, average copper rise, and ratio of copper to core loss. Empirical multipliers are also given which facilitate the conversion of the load curve to equivalent 24-hour steady load in no more time than it would take to calculate load factor.

ALLOWABLE overloading of transformers during emergency conditions has received considerable attention during recent years, and many valuable papers on the subject have appeared in the technical literature. The object of this paper is to describe the method developed by The Detroit Edison Company for determining the allowable emergency overloading of substation transformers.

In any consideration of allowable transformer overloading, there are three factors which must be taken into account: namely, the magnitude of the overload, its duration, and its frequency of occurrence. The magnitude of the overload is dependent upon the number of transformer units in a substation, the duration is dependent upon the time required for replacement or repair of faulty equipment, and the frequency of occurrence must be obtained from experience records.

Magnitude of Overload

Many substations consist of two or more transformers, or transformer banks, of equal size arranged in such a way that each may carry a normal day-to-day load of a value somewhere near its name-plate rating. When a transformer fails, or other trouble develops causing that unit to be shut down, it is obviously desirable that the remaining units of the substation have sufficient overload capacity to carry the entire load until the defective unit can be replaced or other trouble repaired. This means in the case of a substation having only two transformer units, it would be highly desirable that each unit have an emergency rating equal to about twice its allowable day-to-day loading. Where there are three or more transformer units in the substation, the emergency rating of each can be correspondingly lower.

There are also many suburban-type substations each consisting of a single bank of three single-phase transformers, connected in delta delta. In case of failure of one of these transformers, it is customary to cut it out of service, leaving the remaining two to carry the load in open delta until the defective transformer can be replaced. In these cases the emergency rating of the transformers should be about 70 per cent greater than their allowable day-to-day loading.

Duration of Overload

It is the practice of the company to keep a limited number of emergency spare transformers of appropriate sizes at strategic locations for use in the event of failure of operating units. Experience has shown that a defective transformer, other than a very large unit such as installed at bulk power step-down stations, can usually be replaced with a spare within about 24 hours.

Frequency of Occurrence

Substation records indicate that transformer failures can be expected about once every 16 years per substation. If the transformer is radially fed by underground cable, the expectancy of cable failure must also be taken into account; likewise, any other failures that may result in the loss of a transformer from the system must be considered. Consequently, it has been assumed that, all things considered, any one transformer unit may be subjected to emergency operation not oftener than about once every five or ten years.

Need for Simple Method

In much of the technical literature on the subject of transformer overloading it has been the practice to express allowable emergency overloads in terms of steady loads of given durations following either a period of no load or a period of full load. The recent interim report of the AIEE transformer subcommittee, dated June 22, 1942, follows this same plan. While this defines the load-carrying ability of the transformer in quite specific terms, the expression is not wholly compatible with the irregular shape which characterizes the majority of actual load curves.

By means of the so-called rigorous method, based upon the generally accepted theory governing transient changes in oil and copper temperature with changing transformer load, it is possible to determine whether it is feasible to carry an overload of any given shape and magnitude. While this method is useful in checking isolated cases, it is far too cumbersome and time consuming for routine application in connection with hundreds of transformer installations.

The method described in this paper is comparatively simple and its application requires little time. It consists in first expressing the allowable transformer overload in terms of a steady 24-hour load, and then converting the potential

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overload curve to a steady 24-hour load of such magnitude that it will cause the same insulation aging as the irregular curve. Thus the transformer capabilities and the potential overload are reduced to a common basis for ready comparison.

Allowable Emergency Transformer Overloading

The well-known "eight-degree rule," which states that the rate of insulation aging doubles for each eight degrees centigrade increase in temperature, has been generally accepted as a rough measure in determining the life of transformer insulation under varying temperature conditions. There are fairly wide variations from the strict application of the rule, as shown by tests of F. M. Clark.[1] There is also no definite generally accepted bench mark from which to start. L. C. Nichols[2] shows $1^1/_2$ years' life for 105 degrees centigrade temperature compared with V. M. Montsinger's[3] seven years' life for the same temperature.

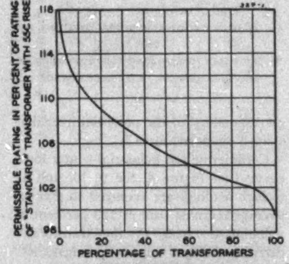

Figure 1. Variation in load-carrying ability of 126 substation transformers ranging in size from 100 to 10,000 kva

In the practice adopted by The Detroit Edison Company, the allowable emergency overload capacity of transformers has been based upon the "eight-degree rule" and Nichols'[2] life curve including safety factor. Montsinger's[3] curve could have been used just as well. No brief is held for either curve; both are based on the "eight-degree rule," and neither authority claims to foretell with any degree of accuracy the actual transformer life. By following either curve and using a reasonable hot-spot temperature as a guide, all transformers can be treated alike and subjected to a uniform degree of insulation aging during emergencies.

On the basis that the emergencies occur only once in five or ten years and last for a single 24-hour load period, it was thought reasonable to allow one per cent life consumption for each 24-hour emergency. This was found on Nichols' curve to correspond to a hot-spot temperature of 120 degrees centigrade for 24 hours, which was considered to be a reasonable average value provided that:

1. The maximum hot-spot temperature never exceeds 150 degrees centigrade.
2. The maximum top-oil temperature never exceeds 115 degrees centigrade.

Investigations showed that with the worst conditions expected to be encountered these upper limits would not be exceeded. As an over-all rule it was decided that under no conditions should the peak load on a transformer be allowed to exceed 200 per cent of name-plate rating.

VARIATIONS IN TRANSFORMERS

Experience indicates that there is considerable variation in temperature rise and corresponding load-carrying ability between different transformers of the same name-plate rating. One can well afford to take advantage of this extra marginal capacity. Figure 1 shows the variation in load-carrying ability of 126 substation transformers ranging in size from 100 to 10,000 kva. The 126 transformers are compared with a "standard" 55 degree centigrade rise transformer having a 45 degree centigrade top-oil rise and a ratio of full-load copper loss to iron loss of two. It is seen that about 50 per cent of the transformers studied permit five per cent greater continuous loads than the 55 degrees centigrade transformer and, that five per cent of the transformers permit 13 per cent greater continuous loads without exceeding the allowable temperature limits.

The transformer variables which determine the amount of overload that can be carried without exceeding a given hot-spot temperature rise are top-oil rise, average copper rise, and ratio of copper loss to core loss. The ambient temperature then fixes the actual hot-spot temperature and resultant insulation aging.

AMBIENT TEMPERATURES

United States Weather Bureau statistics for the Detroit area show that summer (May through October) ambient temperatures seldom exceed 35 degrees centigrade. Similarly, for winter (December through February) there are a few days with ambient temperatures as high as 13 degrees centigrade. On the basis of these records the summer and winter ambient temperatures selected for establishing allowable emergency transformer loading were 35 degrees centigrade and 13 degrees centigrade, respectively.

NOMOGRAPHS

As a means for determining quickly the allowable 24-hour emergency loading of transformers for the chosen ambient temperatures, the nomographs shown in Figures 2 and 3 were developed. They are based on the following generally accepted fundamental relationship:

$$T_{hs} = T_f\left(\frac{1+NR^2}{1+N}\right)^{0.8} + T_{cu}R^2 + T_a \qquad (1)$$

where

T_{hs} = hot-spot temperature, degrees centigrade
T_f = top-oil rise at full load, degrees centigrade
N = ratio of copper loss to core loss at full load
R = ratio of load to name-plate rating
T_{cu} = copper rise above top oil at full load plus ten degrees centigrade allowance for hot-spot differential, degrees centigrade
T_a = ambient temperature, degrees centigrade

To demonstrate the use of the nomographs, two examples are shown in dashed lines for self-cooled transformers, A and B, having the characteristics shown in Table I.

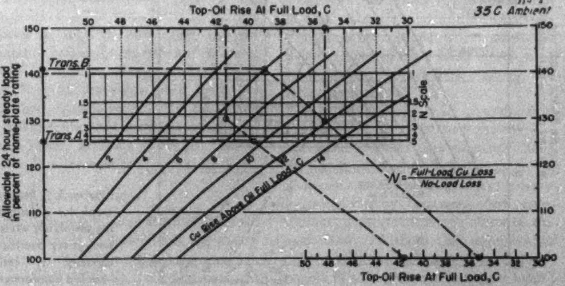

Figure 2. Nomograph for determining allowable summer emergency overload capacity of self-cooled transformers

Table I

	Transformer *A*	Transformer *B*
Name-plate rating	500 kva	1,500 kva
Copper loss	4,070 watts	10,850 watts
Core loss	1,750 watts	4,296 watts
N	2.32	2.53
Top-oil rise	41.5 degrees centigrade	35.2 degrees centigrade
Average copper rise above top oil	9.5 degrees centigrade	6.0 degrees centigrade
Weight of core and coils	3,200 lb	9,700 lb
Weight of tank	1,600 lb	4,950 lb
Weight of oil	2,200 lb	6,050 lb

The procedure consists of first locating the point of intersection of a vertical line from the upper scale of top-oil rise with a horizontal line of the proper *N* value. Then line up a straight edge between this point and the top-oil rise on the bottom scale. With the straight edge held in this position, its intersection with the curve for the proper copper rise above oil determines the allowable 24-hour steady load. This value, expressed as per cent of name-plate rating, is read by projecting the point of intersection horizontally to the scale on the left.

From the nomographs for summer and winter conditions, the allowable 24-hour steady overloads for the two examples are:

	Transformer *A* (Per Cent)	Transformer *B* (Per Cent)
Summer	126	141
Winter	146	162

Load-Curve Conversion

As stated before, it is difficult to compare an irregular-shaped overload curve with values of allowable transformer overloads. However, by converting the actual overload curve to an equivalent steady aging load, it can be compared directly with the allowable 24-hour steady load previously determined for the transformer. To do this, the 24-hour day is divided into three parts: morning, afternoon, and evening. An average load is obtained for each of these periods in the following manner. The average of the three highest consecutive hourly readings any time between midnight and noon is designated as the average morning load. The average of the three highest consecutive hourly readings any time after noon and up till 9 p.m. is designated as the average afternoon load. The average of the three highest consecutive hourly readings any time between the selected afternoon readings and midnight is designated as the average evening load. There is no fixed time for dividing afternoon from evening. The three groups of consecutive hours should be so selected that the resultant equivalent aging load obtained by substituting the three average loads in the following formula is a maximum:

$$EAL = 0.10L_{\min} + 0.30L_{\text{int}} + 0.52L_{\max} \qquad (2)$$

where

EAL = equivalent steady aging load
$L_{\min}$ = the smallest of the three average loads
L_{int} = the intermediate of the three average loads
$L_{\max}$ = the largest of the three average loads

Three general examples are shown: Figure 4 for typical industrial winter load, Figure 5 for typical combined industrial and residential summer load, and Figure 6 for typical residential summer load. In each of these figures, curve 2 is the equivalent steady aging load corresponding to the potential irregular-shaped overload, curve 1.

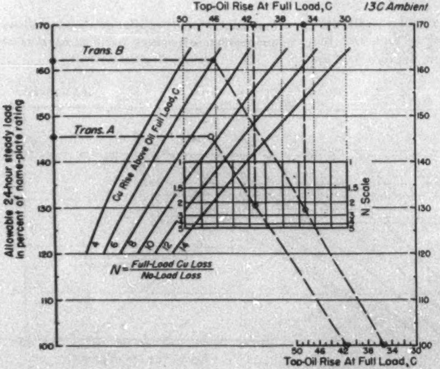

Figure 3. Nomograph for determining allowable winter emergency overload capacity of self-cooled transformers

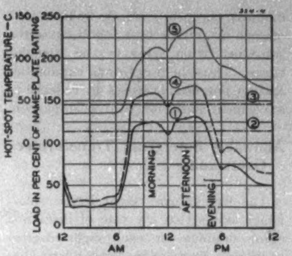

Figure 4. Typical industrial winter load and allowable emergency overload of transformer *A*

1. Potential winter emergency transformer overload based on existing substation load, per cent
2. Equivalent steady aging load of curve 1, per cent
3. Allowable 24-hour steady winter overload of transformer *A*, per cent
4. Maximum allowable winter overload, per cent
5. Hot-spot temperature corresponding to overload curve 4, degrees centigrade

Considering the industrial winter load, curve 1 of Figure 4, the 9, 10, and 11 o'clock readings are averaged to give an average morning load of 123 per cent of name-plate rating of the transformer carrying the load during the emergency. Similarly, the 1, 2, and 3 o'clock readings give an average afternoon load of 129 per cent and the 4, 5, and 6 o'clock readings give an average evening load of 98 per cent of name-plate rating.

Then, according to formula 2.

$EAL = 0.10 \times 98$ per cent $+ 0.30 \times 123$ per cent $+ 0.52 \times 129$ per cent $= 114$ per cent of name-plate rating as shown by curve 2 in Figure 4. Thus, this particular load which has a peak of 131 per cent will cause the same amount of transformer aging as a steady 24-hour load of 114 per cent.

Assuming that this overload is to be carried by transformer *A* previously referred to, its allowable emergency overload capacity, 146 per cent, has been shown in Figure 4 as curve 3. A comparison of these two values, 114 per cent and 146 per cent, indicates that the transformer has ample overload capacity under these conditions.

The maximum load of the shape shown by curve 1 in Figure 4 that can be carried by the transformer is obtained readily by direct proportions and is shown by curve 4. In other words, the maximum peak load that can be carried by the transformer under these conditions is the peak of curve 1, 131 per cent, multiplied by the ratio, 146/114 which gives 168 per cent load in terms of name-plate rating. So much for transformer *A*.

Next, it is assumed in Figures 5 and 6 that the loads shown are to be carried by transformer *B*, and, correspondingly, curves 4 and 3 show the transformer capabilities to be compared respectively with the potential load curve 1 and its equivalent, curve 2.

Accuracy of Method

The load-curve conversion method described above, which may be referred to as the short-cut method, is intended only for use with load factors in the usually encountered range of 50 to 90 per cent. The great majority of load curves fall in this range. When a load has 90 per cent or better load factor, it is probably safest to assume it to be a continuous round-the-clock load. Such loads are usually typical of electrochemical plants, oil refineries, ice plants, and other similar industries not too frequently encountered.

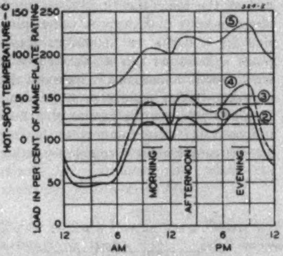

Figure 5. Typical industrial-residential summer load and allowable emergency overload of transformer B

1. Potential summer emergency transformer overload based on existing substation load, per cent
2. Equivalent steady aging load of curve 1, per cent
3. Allowable 24-hour steady summer overload of transformer *B*, per cent
4. Maximum allowable summer overload, per cent
5. Hot-spot temperature corresponding to overload curve 4, degrees centigrade

The accuracy of this short-cut method has been checked by comparison with the rigorous method in which it has been assumed that the hot-spot temperature rise of copper over top oil varies as the square of the load, that the hot-spot differential is ten degrees centigrade at full load, and that the top-oil rise under transient load conditions varies in accordance with the following generally accepted formula:

$$\theta=\theta_u(1-e^{-\frac{Wt}{C\theta_u}}) \qquad (3)$$

where

θ = top-oil rise at end of time t
W = total losses
C = thermal capacity of core and coils, tank and oil
θ_u = ultimate oil rise (varying as 0.8 power of losses for self-cooled transformers)
e = 2.718
t = time

The three examples given in Figures 4, 5, and 6 show hot-spot temperatures, curve 5, calculated in the above manner for the 24-hour allowable emergency loads, curve 4, determined by the short-cut method. The hot-spot temperatures converted to insulation aging and per cent life consumption indicate errors of −1.6 per cent, −2.0 per cent, and +2.0 per cent in allowable peak loads for the three examples cited. In most cases it will be found that the short-cut method gives results accurate to within plus or minus five per cent in allowable loads.

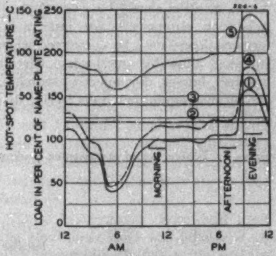

Figure 6. Typical residential summer load and allowable emergency overload of transformer B

1. Potential summer emergency transformer overload based on existing substation load, per cent
2. Equivalent steady aging load of curve 1, per cent
3. Allowable 24-hour steady summer overload of transformer *B*, per cent
4. Maximum allowable summer overload, per cent
5. Hot-spot temperature corresponding to overload curve 4, degrees centigrade

Load-Factor Method

It has been suggested at times that load factor be used in determining the ability of a transformer to carry a given overload. In general, the lower the load factor, the higher the allowable peak load; however, there are such wide variations in allowable peak loads for curves of equal load factor but different shape, that load factor alone cannot be used as a reliable indicator. For instance, Figure 7 presents plotted data showing the range of allowable peak load values for 240 different load curves of various shapes and load factors, each of which has equal aging effect on the transformer insulation. It is seen, for example, that for load curves having approximately 60 per cent load factor, the allowable overloads range from 11 per cent to 43 per cent of the equivalent steady load. Montsinger[4] has presented similar conclusions and stated that two load curves, each with 50 per cent load factor, may vary in allowable overload from 16 per cent to 40 per cent on the basis of equal aging.

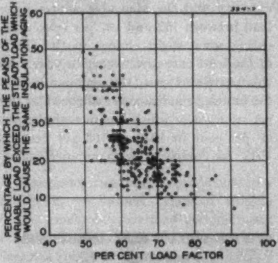

Figure 7. Range of peak load values for 240 different load curves of various shapes, each of which has equal aging effect on the transformer insulation

Routine Application of Method

It has been company practice for a good many years to obtain transformer load readings from recording ammeters at unattended substations and from operators' log sheets of hourly load readings at attended substations. It was the custom to obtain summer and winter peak loads from these records and, with some rough regard to the number of hours of peak load duration, to establish firm ratings for each substation.

In 1939 the short-cut load-conversion method of analyzing load curves was developed and put into practice. This permitted substation firm ratings to be increased materially with safety, because it insured uniform treatment of all transformers, irrespective of the shape of the load curve. Without this assurance of equal treatment and close control, large factors of safety were necessary to insure safe operation of the poorest transformer in combination with the most severe load curve of any general class. The application of this new method requires some additional effort when the load curves are analyzed, but this effort has proved to be more than justified in view of the increased substation firm ratings thus established.

Figure 8 is typical of the continuing records kept for all substations. Dexter substation is of the suburban type and consists of three 333-kva transformers connected in closed delta. Under emergency conditions there would be two transformers operating in open delta with a combined name-plate capacity of 577 kva. Since 1939, the allowable summer peak load has ranged between 151 and 158 per cent of emergency name-plate capacity and the allowable winter peak load between 187 and 197 per cent, indicating some slight change in the shape of the load curve from year to year. In this particular case, the summer load is the limiting condition as evidenced by the per cent usage which has increased from 69 per cent in 1939 to 105 per cent in 1942, as compared with an increase of winter usage from 58 to 86 per cent. If the load continues to grow in that community, the 105 per cent usage for the summer of 1942 indicates that a transformer change is probably needed before the summer of 1943.

In such cases the short-cut load-conversion method is verified by the more accurate rigorous method to be sure that the change is needed in fact. Comparative calculations of this type, including those made during the development of the short-cut method, show an average error of 0.6 per cent for some 32 cases. In one case the error was 9 per cent on the low side, in two cases 5.5 per cent on the high side, and in all other cases it was well within the expected plus or minus 5 per cent.

While not within the scope of this paper, mention should be made that all substation equipment—not just transformers alone—must be considered when studying allowable overloads. Circuit breakers, busses, disconnecting switches, and all such equipment must receive due consideration.

Auxiliary Cooling to Increase Emergency Rating

As stated before, the need for the highest emergency rating is in those substations having only two transformer units. Even by working the transformers to the 24-hour, 120 degrees centigrade hot-spot limit, the emergency rating may still be inadequate as compared with the normal day-to-day loading limit. In such cases, auxiliary cooling is sometimes employed to increase the emergency rating so that it is no longer the bottleneck in substation loading.

Either fans or water spray can be used. Where the auxiliary cooling is desired for emergency use only and a reliable supply of water is available, the water-spray method is by far the more economical, and at the same time it gives a much larger increase in rating. A water-spray ring for a 6,000- or 10,000-kva three-phase transformer can be installed for a few hundred dollars, and it increases the emergency rating some 30 or 40 per cent. Figure 9 presents the results of a load test on a 6,000-kva transformer showing a 37 per cent increase in emergency rating

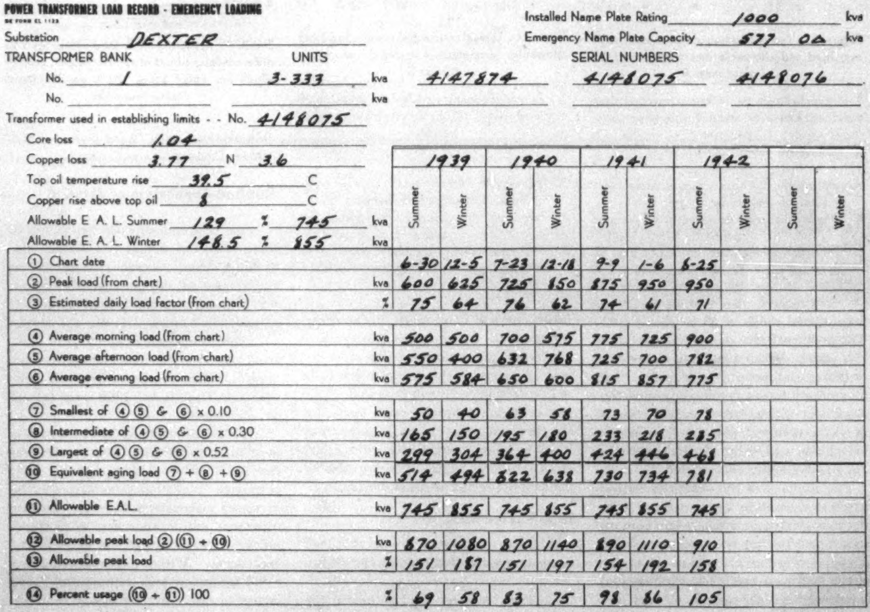

Figure 8. Typical record of substation loading

POWER TRANSFORMER LOAD RECORD - EMERGENCY LOADING
DE FORM EL 1123

Substation DEXTER

Installed Name Plate Rating 1000 kva
Emergency Name Plate Capacity 577 OΔ kva

TRANSFORMER BANK — UNITS — SERIAL NUMBERS
No. 1 — 3-333 kva — 4147874 — 4148075 — 4148076
No. — kva

Transformer used in establishing limits - - No. 4148075
Core loss 1.04
Copper loss 3.77 N 3.6
Top oil temperature rise 39.5 C
Copper rise above top oil 8 C
Allowable E. A. L. Summer 129 % 745 kva
Allowable E. A. L. Winter 148.5 % 855 kva

		1939		1940		1941		1942			
		Summer	Winter	Summer	Winter	Summer	Winter	Summer	Winter	Summer	Winter
① Chart date		6-30	12-5	7-23	12-18	9-9	1-6	8-25			
② Peak load (from chart)	kva	600	625	725	850	875	950	950			
③ Estimated daily load factor (from chart)	%	75	64	76	62	74	61	71			
④ Average morning load (from chart)	kva	500	500	700	575	775	725	900			
⑤ Average afternoon load (from chart)	kva	550	400	632	768	725	700	782			
⑥ Average evening load (from chart)	kva	575	584	650	600	815	857	775			
⑦ Smallest of ④ ⑤ & ⑥ × 0.10	kva	50	40	63	58	73	70	78			
⑧ Intermediate of ④ ⑤ & ⑥ × 0.30	kva	165	150	195	180	233	218	235			
⑨ Largest of ④ ⑤ & ⑥ × 0.52	kva	299	304	364	400	424	446	468			
⑩ Equivalent aging load ⑦ + ⑧ + ⑨	kva	514	494	622	638	730	734	781			
⑪ Allowable E.A.L.	kva	745	855	745	855	745	855	745			
⑫ Allowable peak load ② (⑪ ÷ ⑩)	kva	870	1080	870	1140	890	1110	910			
⑬ Allowable peak load	%	151	187	151	197	154	192	158			
⑭ Percent usage (⑩ ÷ ⑪) 100	%	69	58	83	75	98	86	105			

for loads at 120 degrees centigrade hot-spot temperature.

Permanently installed fans on large transformers usually represent an investment of thousands instead of hundreds of dollars, and, in the 120 degrees centigrade hot-spot range, they increase the emergency rating a matter of only 10 to 15 per cent. Portable fans represent a much lower investment and have the further advantage that one set of fans can serve any one of many locations at the time of emergency; however, they too increase the emergency rating only moderately. Figure 10 shows comparative test data for a bank of three 5,000-kva self- and fan-cooled transformers, with and without the fans in operation. Here it is seen that, in the emergency overload range of operation, the benefit from fans is relatively moderate.

Fans do, however, have a definite field of application. They are most valuable where the day-to-day rating of a transformer must be increased, either to forestall the installation of added capacity, or to help carry the normal load through the hot summer months. Water spray is not recommended for day-to-day use and should be considered only where the emergency rating needs a boost.

Tests at Bulk Power Step-Down Stations

The company has seven bulk power stations, where voltage is stepped down from 120 to 24 kv, each consisting of two or more transformer banks of 15,000- to 45,000-kva rating. Load readings from these stations are analyzed semiannually along with the load readings from distribution substations. However, in the case of bulk power stations when the load has reached a value such that the potential emergency overload approaches the calculated limit of the transformers, actual field tests are conducted, subjecting each transformer bank and all its related equipment to one emergency overload cycle, the equivalent of 24 hours operation at 120 degrees centigrade hot-spot temperature. During the past two years, three banks of three 10,000-kva self-cooled transformers, and two banks of three 5,000-kva self- and fan-cooled transformers have been tested. Figures 10 and 11 show typical results of such tests. Table II shows the comparison between calculated and test values for final top oil and final average copper temperatures.

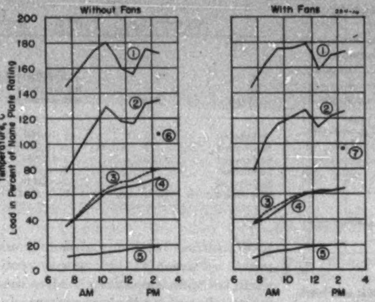

Figure 10. Emergency overload test of a bank of three 5,000-kva self- and fan-cooled transformers

1. Load in per cent of name-plate rating
2. Hot-spot temperature, degrees centigrade (calculated)
3. Top-oil temperature, degrees centigrade (calculated)
4. Top-oil temperature, degrees centigrade (test)
5. Ambient temperature, degrees centigrade
6. Average copper temperature at shutdown (without fans), 111 degrees centigrade by calculation, 107 degrees centigrade by test
7. Average copper temperature at shutdown (with fans), 96 degrees centigrade by calculation, 98 degrees centigrade by test

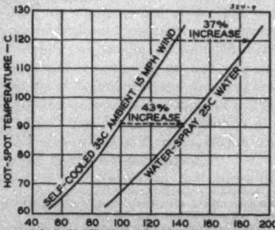

Figure 9. Increase in allowable transformer emergency loading by use of water spray, based on test of 6,000-kva three-phase transformer

These of necessity were field tests, the results of which are influenced by such uncontrolled factors as the sun and clouds, ambient temperature, and wind. All tests were started on calm, sunny days but conditions sometimes changed before the tests could be completed. Under the circumstances, the test and calculated values were considered to be in reasonable agreement.

The importance of conducting such emergency tests cannot be overemphasized, even if they do nothing more than acquaint the operators with what to expect when the station equipment is overloaded. Tests are bound to bring to light unusual conditions that might otherwise be overlooked. It is one thing to set up emergency-load limits of from 150 to 200 per cent of name-plate rating for a large group of stations, and another to be sure that everything in a particular station including the bus, circuit breakers, cable, current transformers, disconnect switches, and so forth, can carry the specified overload safely. The anticipated emergency usually is thought of as likely to happen at some indefinite time in the distant future, the result being that certain lesser details are currently overlooked. On the other hand, if an overload test is scheduled definitely, the responsibility for considering every possible contingency becomes immediate. Such tests insure that the established overload limits can be met safely under the more trying conditions of an actual emergency.

Potential Difficulties to Be Avoided

Experience thus far has indicated the possibility of several types of trouble during emergency-overload periods, none of which is associated with the limits in thermal ability of the equipment to carry the load safely. Any one of these might prove serious if first encountered during an actual emergency, rather than during a staged test.

Among such possible troubles are those related to relaying. It is entirely possible that although relay settings may be such as to give proper breaker operation for any system fault under normal loading conditions, they may cause faulty operation when the transformers are carrying 150 to 200 per cent of name-plate rating. All possible load and fault combinations therefore should be carefully checked.

Transformer oil will expand beyond normal limits when the unit is carrying high overloads and may cause trouble, particularly in the case of transformers served by a common inert gas system where the oil is likely to back up into the gas equipment. In some cases it has been necessary to set up instructions for emergency lowering of the oil level in order to avoid such occurrence.

Oil-filled gas-pressure bushings may leak because of increased pressure at the high temperature if proper adjustment of oil level and gas pressure is not made.

Switchboard ammeters often will be found to go off scale during an emergency. If there is any time an operator needs to

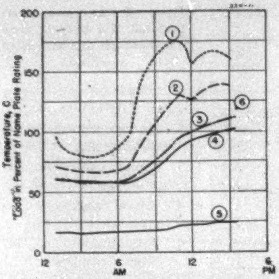

Figure 11. Emergency overload test of a bank of three 10,000-kva self-cooled transformers

1. Load in per cent of name-plate rating
2. Hot-spot temperature, degrees centigrade (calculated)
3. Top-oil temperature, degrees centigrade (calculated)
4. Top-oil temperature, degrees centigrade (test)
5. Ambient temperature, degrees centigrade
6. Average copper temperature at shutdown; 115 degrees centigrade by calculation, 117 degrees by test

Table II

Type	Bank	Name-Plate Rating (Kilovolt-Amperes)	Final Top-Oil Temperature (Degrees Centigrade)		Final Average Copper Temperature (Degrees Centigrade)	
			Calculated	Test	Calculated	Test
Self-cooled	1	3–10,000	108	93	113	109
Self-cooled	2	3–10,000	110	101	115	117
Self-cooled	3	3–10,000	107	101	112	113
Self-cooled	1	3– 5,000	85	78	118	113
Self-cooled	2	3– 5,000	80	73	111	107
Fan-cooled	2	3– 5,000	65	65	96	98

know the load readings, it is certainly during an emergency. Temporary test instruments can be used in some cases, but at important stations it is well to install a single high-scale ammeter for each transformer bank.

While all tests have been conducted at overload ratings equivalent to 24-hours operation at 120 degrees centigrade hot-spot temperature, the firm rating of bulk power stations has been based upon one week's operation at 112 degrees centigrade hot-spot temperature. More time is thus allowed for the replacement of a faulty unit in these stations. The transformers are large and not too easily transported. Some two hundred distribution and industrial-type substations with their smaller and more easily handled units are given a firm rating based on the 24-hours operation at 120 degrees centigrade hot-spot temperature.

Conclusions

Allowable emergency overloads for substation transformers, as determined by the method outlined, have been used for only a few years in establishing the firm ratings of substations, and thus no large amount of experience has yet been accumulated. While the maximum temperature limits selected may appear to be high compared with earlier practice, it does not follow that such limits always will be reached during an emergency. Rather, these limits have been set as ceilings that may be encountered only if an emergency occurs under the worst combination of circumstances.

If future experience indicates the wisdom of lowering these limits, that can be done easily without changing basically the method described. Meanwhile, and particularly during the war, the savings in critical materials and in investment made possible by the higher substation firm ratings, should more than offset any isolated cases of abnormal life consumption.

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Kilowatts, Kilovars, and System Investments

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MEMBER AIEE

WHAT portion of the total system investment can one equitably allocate to the system kilovar requirements? Or, looking at it in a little different way, what portion of the system kilowatt capacity is used by kilovars? The answer to these questions obviously depends upon the method of supplying the kilovars. It is indicated in this analysis that supplying the kilovar requirements of a system in general may be divorced from supplying the kilowatt requirements and that the two factors may be dealt with as independent commodities. This approach shows that maximum usage of system capacity is made in producing kilowatts.

This two-commodity concept has developed very rapidly during the past decade. Until the advent of the present-day capacitor, kilovars were largely supplied by kilowatt generators in the power plant and transmitted to the point of consumption. Synchronous condensers, by providing regulated kilovars, were used primarily for regulating high-voltage transmission lines. Smaller ones, however, have been used on occasions by central stations and industrials for supplying kilovars locally. It is on this basis that systems were designed and largely operated. The availability of a low-priced kilovar generator—called a capacitor—has contributed materially to the acceptance of the two-commodity philosophy. This concept will now be developed and its relation to our immediate objectives indicated.

An electric system is built and operated for the purposes of serving consumers with electric service. A consumer basically wishes—in addition to light, heat, and so forth—for his motor to spin and do work for him. It so happens that this service requires two types of electric energy. The major type, which is also the real pay load on a system, is known as active or kilowatt component.

It is this component that turns the lathes, grinds the flour, produces airplanes, and turns out the finished product for the consumer. The other type of energy known as reactive or kilovar component is the type that is needed to magnetize transformers and induction motors or any type of electric apparatus needing magnetic flux supplied by the power system in order to function. This latter type of energy does not cause the disk of a watt-hour meter to turn, but it can use system kilowatt capacity to generate it and deliver it to the consumer. The amount of system kilowatt capacity required to provide this kilovar requirement is obviously determined by the method of supplying it, which may be done in either of two ways or by a combination of them both:

1. By producing it in the kilowatt generators in power plants and transmitting it through the system to the point of consumption.

2. By producing it in auxiliary kilovar generators, such as capacitors, synchronous motors, and synchronous condensers, which generally are located at or near the load, and therefore, the problem of delivering them to where they are needed is solved.

An analysis of these two methods of supplying kilovars is given in the following section.

Basic Assumptions

Although several minor assumptions are used in the body of the paper as the occasion arises, the three basic ones given herewith obtain throughout:

1. That the total system investment may be segregated and allocated separately to generating equipment and to equipment having either voltage drop or current magnitude as the factor which determines its limiting output rating. Obviously a certain portion of the total investment may not fall logically into one of these three classifications, but it is felt that the error introduced by this assumption is small.

2. That steam and hydraulic turbine generator units can deliver their full kilovolt-ampere rating at unity power factor in a sufficiently large majority of the cases to permit its acceptance as a general fact, particularly as the error in this assumed fact will have a negligible effect on the conclusions.

3. That the progressively increasing kilovar requirement—the system I^2X—because of current flowing through the reactance of the system from the load to the generator be neglected. In other words, only the kilovar requirements of the load are used in plotting the data. This means that still more of the system investment really should be charged to the flow of kilovars when it is supplied by kilowatt generators. This assumption offsets to a large extent the errors introduced by assumptions 1 and 2, as this neglected component can amount to a large portion of the load requirements in particular instances.

General Conclusions

1. Kilowatts and kilovars can be dealt with most economically as two independent commodities using separate generators, in the general case. The amount of system facilities used by both in getting from generating source to load is determined by the economic balance between the added cost of the facilities and the increasing costs of lower-voltage smaller-size capacitor units required for connection adjacent to load.

2. A value of two to six per cent of the system investment is chargeable to the kilovar requirement when it is supplied near the load by auxiliary kilovar generators as contrasted to 20 to 25 per cent when supplied by kilowatt generators in the power stations for systems operating around 80 per cent power factor.

3. If rates are established for kilovars based on the cost of supplying them by kilowatt generators, they will provide an excessive inducement for the consumer to generate his own kilovars. The important system design and operating factor of kilovar control will then tend to pass out of the hands of the operating company into the hands of the consumer. This tendency is aggravated by each price reduction on kilovar generators, and their price today is only about 25 per cent of the 1929 level.

4. Approximately 330 pounds of critical materials are required to produce one kilovar when supplied by kilowatt generators and delivered through the system, as compared to five pounds (includes all mounting structures, housings, dielectric liquid, paper, and so forth) when supplied by capacitor kilovar generators near the load. Even dividing this ratio by a factor of 10 to allow for errors

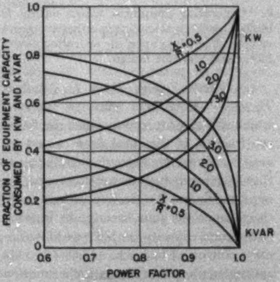

Figure 1. Allocation of system capacity usage when voltage drop is limiting factor

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in assumptions leaves a sufficiently large differential to substantiate the conclusion that kilovars in general should be produced by special generators near the load.

Analysis

I. Kilovars Supplied by Kilowatt Generators and Transmitted Through the System

Since a system can deliver its maximum pay load when operating at 1.0 power factor (if there is no stability limitation), this part of the discussion will be based on the viewpoint that the extent to which this maximum kilowatt capacity is reduced by the need for supplying kilovars determines the amount of system capacity consumed by the kilovar generation. With this premise, the equipment of a system naturally divides itself into three groups, as determined by the different ways in which kilovars use system kilowatt capacity, namely:

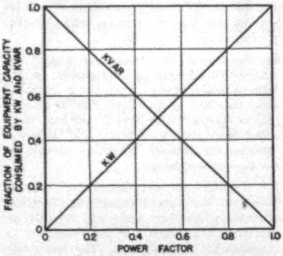

Figure 2. Allocation of system capacity usage when ampere capacity is limiting factor

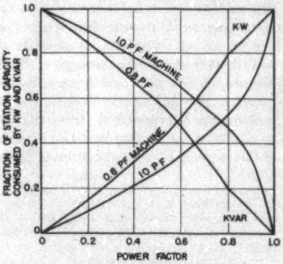

Figure 3. Allocation of steam-turbine-generator capacity usage

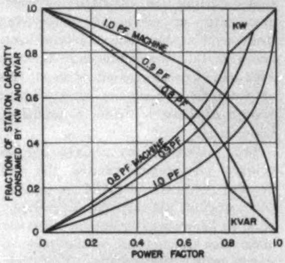

Figure 4. Allocation of hydroelectric-turbin generator capacity usage

1. Equipment such as overhead lines wherein voltage drop is the limiting factor.
2. Equipment such as transformers and cables wherein ampere capacity and therefore thermal loading are the limiting factor.
3. Generating equipment whose output is limited by thermal loading in either armature or field, depending upon the operating power factor and machine design.

1. *Where Voltage Drop Is Limiting Factor.* Equations can be derived which will give for any specified condition the fraction of the total drop caused by active and reactive current. On the basis that each component uses system capacity in proportion to its effect on voltage drop, these equations give directly the fraction of the total capacity in this type of equipment consumed by each. Such relations are shown plotted in Figure 1 as a function of power factor and the ratio of X/R of the circuit. These curves show that 30 to 70 per cent of the system voltage-limited capacity is consumed by the kilovars of an 80 per cent power-factor load in circuits having an X/R ratio of 0.5 to 3.0 respectively.

2. *Where Ampere Capacity Is Limiting Factor.* A piece of equipment such as a transformer, a regulator or a cable, having current magnitude as its limiting factor, can deliver full kilovolt-amperes at 1.0 power factor, that is, its full rating in kilowatts. At any other given power factor it can deliver only that fraction of its full kilowatt rating represented by the power factor; for example, at 0.8 power factor it can deliver 0.8 of its rating in kilowatts. Hence, for a given thermal (or ampere capacity), the portion of the capacity used by the active current is simply the per-unit power factor, and that portion used by the reactive current is unity minus the per-unit power factor. This division in the usage of the system capacity is on the basis of the kilowatt capacity used by each. These relations are shown plotted in Figure 2 for various power factors.

3. *Thermal Capacity of Generators.* This case is somewhat complicated by the fact that a generator is designed for operation at a predetermined power factor, and by the limitations of the prime movers, cooling water, steam facilities, and so forth. However, a frequent practice in respect to turbine-generator ratings is to install 0.8 power-factor generators with turbines good for 5/4 generator name-plate kilowatt capacity. This procedure permits 125 per cent name-plate kilowatt output to be delivered at 1.0 power factor. For several years it has been the practice to furnish 0.8 power-factor turbogenerators to utilities on this basis unless requested to the contrary, and also many utilities have specifically required units so rated in the interests of obtaining peak capacity. Therefore, it is felt that one reasonably may assume that a fair portion of the units in service today will deliver full kilovolt-amperes at 1.0 power factor and could be so operated if other sources for kilovars were made available. Some units admittedly have limitations such as end structure heating, when operating at increased power factor, and therefore each case should be checked specifically in determining its limits in this respect.

That this philosophy of operation and the possibilities ascribed to it in the preceding paragraph are reasonable is supported by the following quotation:[1]

"Table I shows the present turbine-generator capacities as of December 8, 1941, totaling 919,550 kw, which is greater than the original kilowatt ratings by 20 per cent, primarily because of increases in power-factor ratings."

This paper is a report on the kilovar-supply program of the Public Service Electric and Gas Company of New Jersey.

The above discussion also applies to modern 0.8 or 0.9 power-factor hydroelectric units, and so for the purposes of this discussion, the capacity of the station in kilowatts will be taken as the station rating at 1.0 power factor. The kilowatt capacity reduction from this base rating caused by the flow of kilovars will be allocated to the kilovars flowing.

Representative data for steam and hydroelectric generators, giving their outputs at reduced power-factor loadings, were studied from which the kilowatt output for various power factors and hence the reductions from base kilowatt rating were determined. These results are shown plotted in Figures 3 and 4.

These data present a reasonable allocation of the portion of each class of equipment capacity actually being used by active and reactive kilovolt-amperes during peak load conditions for various power factors. It is interesting now to speculate as to what portion of the total system investment is used—within these assumptions—in serving kilovars for different power factors in a few chosen systems.

Table I is a summary of the classifications of system investments for various types of equipment as compiled by the Federal Power Commission in 1939, for three representative systems. From these data the fraction of the total system investment in each type of equipment considered in this study may be tabulated.

Weighting these percentages by the amounts of each type of equipment that is consumed by kilovars at various power factors as shown in the data of Figures 1 to 4, one can determine what fraction of the total system investment is being used by reactive component for the three types of systems listed in Table I. Part I of the appendix develops the method for consolidating these data.

Table I. Classification of System Investments

	Company A	Company B	Company C
Steam	0.104	0.335	0.452
Hydroelectric	0.334	0.011	0
Transmission (current limit)	0.099	0.169	0.005
Transmission (voltage-drop limit)	0.134	0.039	0
Distribution (current limit)	0.203	0.293	0.526
Distribution (voltage-drop limit)	0.126	0.153	0.017
Total	1.00	1.00	1.00

Data taken from book compiled by Federal Power Commission, 1939, "Statistics of the Electric Utilities in the United States."

Consolidating these figures as suggested in the foregoing and assuming an over-all figure for X/R of 1.0 for voltage-limited equipment and 0.8 power-factor generators, the interesting data on Figure 5 are obtained. The results are surprisingly similar for the three rather different types of systems chosen. These curves indicate that 20 to 25 per cent of the system investment can be charged reasonably to the kilovar requirements when kilovars are supplied from generators operating at 0.8 power factor.

II. Kilovars Supplied by Auxiliary Kilovar Generators at or Near the Load

Kilovar generators can always be located any place on any system at essentially a constant cost per kilovolt-ampere of something less than ten dollars. Since these generators cannot always be placed adjacent to the requirement, some system capacity should be charged against them so a figure of $15 will be added, making $25 as an unquestionable outside cost of kilovar generators. Numerically, the use of separate kilovar generators means that a much smaller fraction of the total system investment is chargeable to reactive kilovolt-amperes. Since the kilowatt investment in a system may readily vary around $400, the fraction of total system investment chargeable to kilovar—for correcting the loads to 1.0 power factor—over a range of system values—has been calculated and is shown plotted in Figure 6.

Figures 5 and 6 give at a glance the wide variation in the fraction of the total system investment chargeable to kilovar requirements for the two concepts. A value of two to six per cent of the system investment is chargeable to the reactive component when supplied near the load, as contrasted to 20 to 25 per cent when supplied by the kilowatt generators in the power stations for system loads around 80 per cent power factor.

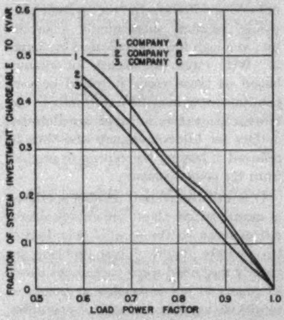

Figure 5. Fraction of system investment chargeable to kilovars when they are supplied by kilowatt generators and transmitted through the system to the load

Discussion

1. General

These figures indicate that a tremendous amount of relatively high-cost system kilowatt capacity is used by kilovars when it is supplied at a point distant from the load—presumably by kilowatt generators. This is a well-known and long-established fact as evidenced by the following extracts taken from the *Proceedings* of the 13th convention of National Electric Light Association, (held in Washington, D. C., June 1907) pages 81–83.

> "With the rapid increase in the industrial use of induction motors and in arc lighting by alternating current, the troubles incident to low power factor, in systems not designed for it, have become unfortunately familiar."
>
> "Low power factor has two serious results: It limits the capacity of the electrical part of the system by loading it up with unproductive current—current for which no revenue is obtained; and it means poor voltage regulation."
>
> "The rating of electrical apparatus depends primarily on the heating produced by the load, and this heating depends entirely on the current. The kilowatt load, therefore, unless the corresponding power factor is taken into account, is not a measure of the actual load on the apparatus."

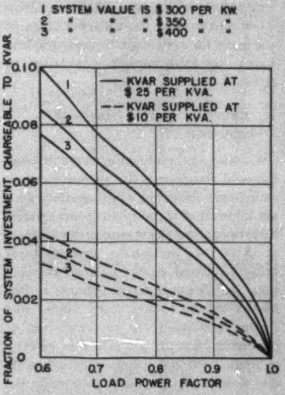

Figure 6. Fraction of system investment chargeable to kilovars when complete load kilovar requirement is supplied at or near the load

> "The second injurious effect of low power factor is poor voltage regulation. This effect is well known and requires no extended explanation."

These facts, however, although they have been known since the turn of the century, are just beginning to exert a real influence on the design and operation of power systems. Until the present period expansions were made beyond the immediate requirements to allow for normal expected growth and to cover new areas or to give necessary reliability. These practices provided margins therefore that could—for some time—be used by the kilovar requirements without impairing the kilowatt capacity.

The developments in the distribution field, such as load-center and unit-substation equipments and the advent of the low-priced kilovar generator—the capacitor—have changed this picture. Conditions are different now and should be so recognized. The expansion era is tapering off, and the war effort in particular is requiring that we get the most out of every bit of system capacity. The "injurious effects of low power factor" referred to in 1907 need not now be felt by the relatively high-cost kilowatt investment equipment. As outlined, the cost of kilovar generators is such that they can now be installed any place on a system at approximately ten dollars per kilovolt-ampere. In some cases the reactive component cannot be supplied right at the load, and so some additional system-investment allocation is required, but taking an outside figure of $25 per kilovolt-ampere still leaves plenty of margin to show the fallacy of attempting to use the system for transmitting kilovars to the loads from the generating stations.

2. Relation to the Immediate Objectives

Looking at the situation from a critical-material standpoint, the picture is about as follows: Assuming approximately $400 per kilowatt of capacity for power-system investment, and 25 per cent of this figure—or $100 as previously developed—as being allocated to the kilovar requirement at 0.8 power factor when it is furnished by the system as a whole, and further estimating the power system as averaging 50 cents per pound of electric equipment would mean approximately 200 pounds of equipment to provide 0.6 kilovar or approximately 330 pounds per kilovar. Contrast this with one kilovar being supplied by capacitors with approximately five pounds of material at most, including mounting, housings, dielectric liquid, paper, and so forth. We wondered why Japan has for several years installed capacitors in very large banks, such as one installation having 100,000 kva at a single location.[2] The answer is obvious when vital materials are really needed and are being used for other purposes, as apparently they were.

Our immediate concern in the war effort is to make available the maximum kilowatts from existing facilities. The possibilities in this respect through the use of auxiliary kilovar generators are readily apparent, and in many locations advantage is being taken of the indicated gains.[1-3] But it is generally accepted that still a majority of the kilovar requirements of the country are being supplied by kilowatt generators, which means valuable system generation, transmission, and substation capacity is being used by the reactive component that could be used in producing kilowatts. The relation between these facts and our immediate objectives is considered sufficiently self-evident to require no further amplification.

3. Effect on System Design and Operation During Normal Times

If a system is set up and operated on the basis of supplying the bulk of its reactive requirements from the power generators, and rates are instituted accordingly in an endeavor to encourage the consumer to generate his own kilovars, several interesting angles present themselves. System kilowatt investment costs are relatively stable, and so the cost of delivering kilovars by the system as a whole remains substantially constant, but at a high level as indicated by Figure 5. With rates established for kilovars based on these costs, it would be very profitable obviously for a consumer to buy kilovar generators for approximately ten dollars per kilovolt-ampere and thus be relieved of buying the expensive product from the power company.

It is well known that parallel operation of many electric plants presents problems not common to the parallel operation of two or three plants. These problems are greatly magnified when part of the parallel plants do not co-operate in a manner conducive to economy, ease of operation, and continuity of service of the system as a whole. The kilovar generators—and sometimes large ones—operating on the consumer's premises, are really parallel plants, over which the operating company has little or no control. And these plants are there, in principle, because the consumer could not afford to buy the expensive kilovars offered by the operating company. Further, as the price of kilovar generators such as capacitors goes down, more and more of them will be installed by the consumers. These may or may not be regulated reactive installations. Since economic system design and operation are so intimately related to kilovar requirements, it follows that the operating companies would lose control of this important factor.[4] It is well known that even today—when load factors are a maximum—it is the light load periods that really give voltage and even stability troubles through lack of sufficient reactive control, and so what it would mean in this respect if the price of capacitors were greatly reduced in the future can be imagined easily. In fact, system design and operation could become subject uncomfortably to the mercy of the consumer and the price of capacitors instead of remaining in the hands of the operating company.

From this viewpoint it appears that the two requirements of a consumer—namely, kilowatts and kilovars—may be supplied best by the power company. The relatively long-term basis on which the utilities are financed, in view of their stabilized nature, allowing them to accept a slightly lower return on their investment as compared to industrials, together with the fact that the utility has greater latitude in arrangement of capacitors because of diversity to obtain best efficiency, seems to bear out such an approach.

This is a somewhat different philosophy than has prevailed generally within the operating companies, in that kilovars have been looked upon as a nuisance, and the companies were willing to pay the consumer a certain price to keep the kilovars off their systems.

The two requirements are definite, and from the point of view developed the two requirements may best be dealt with as two independent commodities, using separate "generators" and possibly little system equipment in common. This suggests their metering may even be done separately. The confusing terms power-factor and kilovolt-amperes might even be abandoned to advantage. The large cost-to-supply ratio of kilowatts to kilovars—the two units which go to make up power factor and kilovolt-ampere—logically indicates divorcing them. This large cost ratio further indicates that kilovolt-ampere metering, which gives equal weight to kilovars and kilowatts, is somewhat inequitable. It appears that with this philosophy, that is, the operating company providing the kilovar requirements, the utility would be in the business of selling *complete* electric service, in terms of both kilowatts and kilovars, and in addition, of having *complete* control of system design and operation, as it should have—resulting in maximum economy and usage of materials that go into making up a system.

Appendix. Allocation of System Capacity to Active and Reactive Uses

I. Kilovars Supplied by Kilowatt Generators and Transmitted to the Load

1. Where Voltage Drop Is Limiting Factor

The expression for voltage drop

$$V_D = I(R\cos\theta + X\sin\theta)$$

gives an accurate relation beween voltage drop, current magnitude, power-factor angle, and the impedance of the circuit. For any specified condition, the fraction of the total drop caused by the active current is

$$V_P=\frac{R\cos\theta}{R\cos\theta+X\sin\theta}=\frac{\cos\theta}{\cos\theta+N\sin\theta}$$

and the fraction of the drop caused by the reactive current is

$$V_Q=\frac{N\sin\theta}{\cos\theta+N\sin\theta}$$

where N = ratio of reactance to resistance

θ = power-factor angle

On the basis that each component (active and reactive) consumes system capacity in proportion to its effect on voltage drop, the factors V_P and V_Q give directly the fraction of the total capacity in this type of equipment consumed by each, as a function of θ and N.

These relations are shown plotted in Figure 1 for power factors of 0.6 to 1.0 and for N's of 0.5, 1.0, 2.0, and 3.0.

An exact analysis may require an integrated value of V_P and V_Q for a particular system because of the variation in N. For example, one set of factors may be necessary for the transmission system, another for the distribution system.

2. Where Ampere Capacity Is Limiting Factor

For a given thermal limit (or ampere capacity) the decimal fraction of the capacity used by active current is

$$C_P=\cos\theta$$

and that used by the reactive is

$$C_Q=1-\cos\theta$$

This division of system capacity, or allocation of capacity consumed, is on the basis of the kilowatt capacity used by each. These relations are shown plotted in Figure 2, for various power factors.

3. Where Thermal Capacity in Generators Is Limiting Factor

Representative data for steam and hydroelectric generators, relative to their outputs at reduced power-factor loadings, were obtained, from which data for the kilowatt output for various power factors were determined. Letting K_P denote the fraction of maximum kilowatt output at reduced power factor, it follows that the fraction of station kilowatt capacity consumed by the reactive component at corresponding power factors is $1-K_P$, or

$$K_Q=1-K_P$$

The values of K_P and K_Q calculated from the data are shown plotted in Figures 3 and 4 respectively.

4. Combining the Three Types of Equipment

On the basis that active and reactive kilovolt-ampere requirements be allocated their share of the total system investment in proportion to their use of the system capacity, the relations derived above give directly the per-unit allocations for each particular type of equipment. And the fraction of the *total* system investment chargeable to the active and reactive components at any given power factor can be determined by evaluating T_P and T_Q as follows:

$$T_P=k_1V_P+k_2C_P+k_3K_P$$

= fraction of total investment chargeable to kilowatt load

$$T_Q=k_1V_Q+k_2C_Q+k_3K_Q$$

= fraction of total investment chargeable to kilovar load

where

k_1 = ratio of equipment investment having voltage drop as limiting factor to total system investment

k_2 = ratio of equipment investment having ampere capacity as limiting factor to total system investment

k_3 = ratio of generating-station investment to total investment

T = total system investment = $1.0=T_P+T_Q$

and V_P, V_Q, C_P, C_Q, K_P, K_Q are derived and defined above.

II. Kilovars Supplied by Auxiliary Kilovar Generators at or Near the Loads

The fractions of the system investment chargeable to the active and reactive components for this case are somewhat easier to develop, since the investments for each do not necessarily use common system equipment.

Therefore

$$T_P=\frac{\text{kilowatt investment}}{\text{total investment}}$$

$$T_Q=\frac{\text{kilovar investment}}{\text{total investment}}$$

where total investment is equal to the kilowatt investment plus the kilovar investment. Designating the kilowatt investment as 1.0, and, if sufficient kilovar is supplied always to improve the load power factor to 1.0, so that all the kilowatt capacity is available, the kilovar investment, as a fraction of the kilowatt investment is

$$\text{kilovar investment}=\frac{\sin\theta\text{ cost per kilovar}}{\cos\theta\text{ cost per kilowatt}}$$

$$=\tan\theta\frac{(\text{cost per kilovar})}{(\text{cost per kilowatt})}$$

Hence

$$T_P=\frac{1.0}{1.0+\tan\theta\dfrac{(\text{cost per kilovar})}{(\text{cost per kilowatt})}}$$

$$T_Q=\frac{\tan\theta\dfrac{(\text{cost per kilovar})}{(\text{cost per kilowatt})}}{1+\tan\theta\dfrac{(\text{cost per kilovar})}{(\text{cost per kilowatt})}}$$

and

$$T_P+T_Q=1.0=\text{total investment}$$

These relations are shown plotted in Figure 6 for system costs per kilowatt of \$300, \$350, and \$400 and for kilovar costs of \$10 and \$25, supplied near the load, to improve power factor of load to 1.0.

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Pulling Loads on Single- and Multiple-Conductor Impregnated-Paper Lead-Encased Cable, Solid Type

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LACK of factual information relative to the permissible pulling loads to which solid-type impregnated-paper lead-sheath power cable might be subjected by means of woven-wire pulling grips without damage to the sheath prompted the Insulated Power Cable Engineers Association to consider the subject and resulted in the laboratory tests described in this paper.

There seems to have been very little work done in recent years on this or related subjects that has appeared in the technical literature. Considerable authentic and valuable information is available relative to work done by the National Electric Light Association underground systems committee on cable installation. For convenience of reference some excerpts from literature have been included in Appendix I.

Because of a lack of field records or laboratory test results, it was considered desirable to carry out a test program involving a rather broad selection of samples with respect to conductor size and shape, thickness of insulation, and constructions that would be representative of the cable industry's production. All paper-cable manufacturers' products were represented in the selection of samples tested.

The question of permissible strain to which lead-sheathed oil-filled cable might be subjected without serious damage to the sheath from gouging had been before a committee of manufacturers' representatives since December 1936. The original suggestion, based upon the use of pulling eyes soldered to conductors and sheath and the results of field experience rather than laboratory tests, limited the pulling strain to a maximum of 5,000 pounds. This limitation was for the purpose of controlling the damage to the lead sheath by gouging to a depth of not more than 20 mils. A later recommendation was made to limit the pulling strain on single-conductor cable to 6,000 pounds and on three-conductor cable to 5,000 pounds.

By observing these values for maximum strain, it was thought the limit of sheath gouging could be reduced from 20 mils to 15 mils. This assumed adequate duct clearance, good duct construction clear of any harmful foreign matter, and good installation practice.

Conductor areas of oil-filled cable and of solid-type cable are sufficiently large in most cases to withstand pulling stress considerably in excess of the foregoing limitations without physical changes in the cable that will effect its electrical characteristics appreciably. If, in these cases, the maximum permissible pulling load on a cable is considered relative to the maximum permissible stress on conductor area, the tendency is to lose sight of the premise: that is, protection of the sheath against excessive gouging.

Conductor areas in solid-type paper cable are frequently insufficient for a pulling load of 5,000 or 6,000 pounds. In these cases it is necessary to limit the load to some lower value based upon a maximum permissible stress on the conductor or upon a combination of the conductor and sheath if pulling eyes are used.

If cable is pulled by means of woven-wire grips, the strength of the conductor is not involved unless the ends of the conductor are embedded into the sealing caps. Ordinarily this is not done, and the entire load is taken by the cable sheath.

A few simple rules that are easy for application in the field for the guidance of men charged with the safe handling of cable during installation with woven-wire grips are much to be desired. This approach to the problem, however, would broaden the consideration under the original premise: that is, of permissible stress on the cable sheath. There are many variable factors involved in the pulling of cable that are peculiar to particular installations. These factors cannot be contemplated under simple rules nor are they considered in connection with the simple formulas for maximum pulling stress and maximum pulling length proposed as a result of this investigation.

Table I. Sheath Dimensions, Ultimate Load, and Tensile Strength of Lead Sheaths

Sample	Measured Outside Diameter	Nominal Thickness of Sheath (By Manufacturers) Fraction	Nominal Thickness of Sheath (By Manufacturers) Decimal Equivalent	Measured Average Thickness Sheath	Calculated Area (Square Inches)	Load at Failure (Pounds)	Calculated Tensile Strength (Pounds Per Square Inch)
A. Single Conductor With Thin Wall Insulation Up to 5/32 Inch							
A-1 Specimen 1	1.21	6/64	0.094	0.115	0.3956	1,400	3,539
A-1 Specimen 2	1.18	6/64	0.094	0.1155	0.3863	1,200	3,106
A-3	1.25	1/8	0.125	0.087	0.3179	1,000	3,146
A-4	1.75	10/64	0.156	0.129	0.6569	2,410	3,669
B. Single Conductor With Heavy Wall Insulation Above 5/32 Inch							
B-1	1.25	7/64	0.109	0.102	0.3679	1,600	4,349
B-2	2.13	0.117	0.117	0.124	0.7815	1,930	2,470
B-3	1.18			0.090	0.3082	800	2,596
B-4	2.68			0.145	1.1548	2,500	2,165
C. Three Conductors With Thin Wall Insulation Up to 15 Kv							
C-1	1.73	7/64	0.109	0.124	0.6256	2,080	3,325
C-2	2.42	8/64	0.125	0.135	0.9691	2,750	2,838
C-3	2.22	9/64	0.141	0.127	0.8351	2,000	2,395
C-4	2.07	8/64	0.125	0.134	0.8150	2,500	3,067
C-5	1.29	6/64	0.094	0.105	0.3909	1,180	3,019
C-6	1.78	7/64	0.109	0.118	0.6161	1,820	2,954
D. Three Conductors With Heavy Wall Insulation 15 Kv and Above							
1	2.54	1/8	0.125	0.131	0.9910	2,500	2,523
2	2.56	1/8	0.125	0.142	1.0787	2,500	2,318
D-3	2.89	9/64	0.141	0.153	1.3156	4,450	3,382
D-4	2.27	1/8	0.125	0.136	0.9118	2,460	2,698
5	2.50	1/8	0.125	0.134	0.9960	2,500	2,510
D-5	3.22	9/64	0.141	0.158	1.5109	3,700	2,434
6	3.12	9/64	0.141	0.152	1.4173	4,500	3,175

The tensile strength of lead sheath was calculated on the basis of the tape-measured outside diameter near the end of the sample and the average measured thickness of lead sheath obtained from rings of sheath cut just back of the end seals subsequent to the pulling test. The sheath had not been damaged or stretched at these points because of protection by the reinforcing shields on the pulling grip.

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Table IA. Specimen Test Data

Sample *D*-3

Three-Conductor 800,000-Circular Mil Type-*H*, $^{13}/_{64}$ Paper, $^{9}/_{64}$ Lead

(a) Grips—Kellems *P* 250
(b) Gauge Length—20 Inches
(c) Fracture at 4,450 Pounds, $13^{1}/_{2}$ Inches Within Bottom Grip, 2 Feet $1^{1}/_{8}$ Inches From Bottom; Spiral in Form

Load (Pounds)	Interval (Minutes)	Lead Sheath Elongation (Inches Per Inch)	Vacuum Inches of Mercury		Outside Diameter (Inches)	
			Top	Bottom	Top	Bottom
500		0.0000	$^{1}/_{8}$	$1^{1}/_{8}$*	2.90	2.90
1,000		0.0010	$^{5}/_{8}$	$1^{3}/_{4}$*	2.90	2.90
	1	0.0015	$^{5}/_{8}$	2 *	2.90	2.89
	2	0.0015	$^{3}/_{4}$	$2^{1}/_{8}$*	2.90	2.89
	3	0.0015	$^{3}/_{4}$	$2^{1}/_{4}$*	2.90	2.89
	4	0.0015	$^{7}/_{8}$	$2^{3}/_{8}$*	2.89	2.89
	5	0.0015	1	$2^{3}/_{8}$*	2.90	2.89
1,500		0.0015	$1^{3}/_{8}$	2 *	2.89	2.89
	1	0.0015	$1^{1}/_{2}$	$2^{1}/_{4}$*	2.90	2.89
	2	0.0015	$1^{1}/_{2}$	$2^{1}/_{4}$*	2.90	2.90
	3	0.0015	$1^{1}/_{2}$	$2^{3}/_{8}$*	2.89	2.90
	4	0.0015	$1^{5}/_{8}$	$2^{1}/_{2}$*	2.90	2.90
	5	0.0015	$1^{5}/_{8}$	$2^{1}/_{2}$*	2.89	2.90
2,000		0.0015	$2^{3}/_{8}$	$1^{1}/_{2}$*	2.89	2.90
	1	0.0025	$2^{5}/_{8}$	$1^{1}/_{2}$*	2.89	2.88
	2	0.0030	$2^{5}/_{8}$	$1^{1}/_{2}$*	2.89	2.88
	3	0.0030	$2^{5}/_{8}$	$1^{1}/_{4}$*	2.89	2.90
	4	0.0030	$2^{5}/_{8}$	$1^{3}/_{8}$*	2.80	2.89
	5	0.0030	$2^{5}/_{8}$	$1^{1}/_{2}$*	2.89	2.88
2,500		0.0030	$4^{1}/_{8}$	$^{7}/_{8}$**	2.90	2.90
	1	0.0030	$4^{1}/_{4}$	$^{7}/_{8}$**	2.80	2.90
	2	0.0035	$4^{1}/_{4}$	$^{3}/_{4}$**	2.90	2.88
	3	0.0035	$4^{1}/_{4}$	$^{5}/_{8}$**	2.80	2.90
	4	0.0035	$4^{1}/_{4}$	$^{1}/_{2}$**	2.90	2.90
	5	0.0035	$4^{1}/_{4}$	$^{3}/_{8}$**	2.80	2.89
3,000		0.0075	$7^{3}/_{4}$	4 **	2.89	2.89
	1	0.0100	9	$4^{3}/_{8}$**	2.80	2.89
	2	0.0100	$9^{1}/_{4}$	$4^{7}/_{8}$**	2.80	2.89
	3	0.0100	$9^{3}/_{8}$	5 **	2.88	2.89
	4	0.0100	$9^{1}/_{2}$	$5^{1}/_{8}$**	2.89	2.88
	5	0.0100	$9^{5}/_{8}$	$5^{1}/_{4}$**	2.89	2.88
3,500		0.0185	$17^{1}/_{2}$	$11^{5}/_{8}$**	2.88	2.87
	1	0.0250	$19^{1}/_{2}$	$13^{1}/_{2}$**	2.88	2.87
	2	0.0265	21	$14^{1}/_{4}$**	2.88	2.87
	3	0.0300	$22^{1}/_{8}$	$14^{5}/_{8}$**	2.88	2.87
	4	0.0320	$22^{1}/_{4}$	$14^{7}/_{8}$**	2.88	2.87
	5	0.0320	$22^{1}/_{4}$	15	2.87	2.87
4,000		0.0410	24	$23^{1}/_{4}$**	2.87	2.86
	1	0.0435	$25^{1}/_{4}$	$23^{7}/_{8}$**	2.87	2.85
	2	0.0450	$25^{7}/_{8}$	24 **	2.87	2.85
	3	0.0450	26	$24^{1}/_{2}$**	2.87	2.85
	4	0.0450	26	$24^{1}/_{2}$**	2.87	2.85
	5	0.0470	26	$24^{1}/_{2}$**	2.85	2.83
4,450		0.0525	$26^{1}/_{4}$	$26^{7}/_{8}$**	2.85	2.81

Extension of lead sheath beyond end of copper conductors: Top—$4^{1}/_{2}$ inches; bottom—9 inches.

* Positive pressure.

** Negative pressure.

Table II. Comparison of Tensile-Stress Values Under the Respective Classifications for Cable Construction

Pounds Per Square Inch

Group A. Single Conductor With Thin Wall Insulation up to $^{5}/_{32}$ Inch

A-1 Specimen 1	3,539	Average	3,365
A-1 Specimen 2	3,106	Minimum	3,106
A-3	3,146	Maximum	3,669
A-4	3,669		

Group B. Single Conductor With Heavy Wall Insulation Above $^{5}/_{32}$ Inch

B-1	4,349	Average	2,895
B-2	2,470	Minimum	2,165
B-3	2,596	Maximum	4,349
B-4	2,165		

Group C. Three Conductors With Thin Wall Insulation Up to 15 Kv—Comparison Between Belted and Type-H Cables

	Belted	Type H
C-1	3,325	
C-2	2,838	
C-3	2,395	
C-4	3,067	
C-5	3,019	
C-6		2,954
Average	2,929	
Minimum	2,395	
Maximum	3,325	

Group D. Three Conductors With Heavy Wall Insulation 15 Kv and Above

	Belted	Type H
1	2,523	
2	2,318	
D-3		3,382
D-4	2,698	
5		2,510
D-5		2,434
6		3,175
Average	2,513	2,875
Minimum	2,318	2,434
Maximum	2,698	3,382

The recommendations of a committee which were before the Insulated Power Cable Engineers Association for consideration and approval at the time this test program was decided upon were:

> "In the case of cables in which the pulling eye is attached to the end of the cable, the maximum pulling strain shall be based on 0.008 pound, per circular mil of copper with a maximum of 6,000 pounds.
>
> "Where a cable grip is used over the lead sheath for pulling, the maximum pulling strain shall be taken as 1,500 pounds, for each one-inch diameter of cable. For example: A three-inch diameter of cable would call for 4,500 pounds. This suggestion does not take into consideration what is inside the lead sheath; that is, we may have large copper and light walls of insulation or small copper and heavy walls of insulation for a given diameter.
>
> "These pulling strains are based on a maximum of 750-foot pull with no bends. Where bends occur, it has been suggested that the permissible pulling strain be reduced ten per cent for each bend."

The proposed rule applicable to cable fitted with pulling eyes received rather general acceptance. However, there were expressions of preference for the 5,000-pound limit, particularly with respect to single-conductor cable.

The proposed rule applicable to cable pulled with a woven-wire grip was judged unacceptable. It was considered preferable to base the permissible pull upon the area of the sheath and the permissible stress to which the sheath material could be subjected without damage from stretching. As a result of this investigation which undertakes to determine the ultimate stress values and extension of lead sheath, it appears that the maximum permissible pulling loads calculated by the foregoing proposed rule would be in excess of the strength of the cable sheath.

Test Specimens

It was directed that samples should be selected from the lists submitted by the manufacturers on the basis of their being as representative as possible as to conductor size under each of the following groupings:

Single-conductor cable:
A with thin wall of insulation up to $^{5}/_{32}$ inch.
B with heavy wall of insulation about $^{5}/_{32}$ inch and heavier.

Three-conductor cable:
C with thin wall insulation rated up to 15 kv.
D with heavy wall insulation rated at 15 kv. and over.

Classification of the samples selected

Table IIA. Tensile Strength of Lead Sheath From Table II

Group	Average	Minimum	Maximum
A	3,365	3,106	3,669
B	2,895	2,165	4,349
C Belted	2,929	2,395	3,325
D Belted	2,513	2,318	2.698
C Type *H*	2,954	One sample only	
D Type *H*	2,875	2,434	3,382

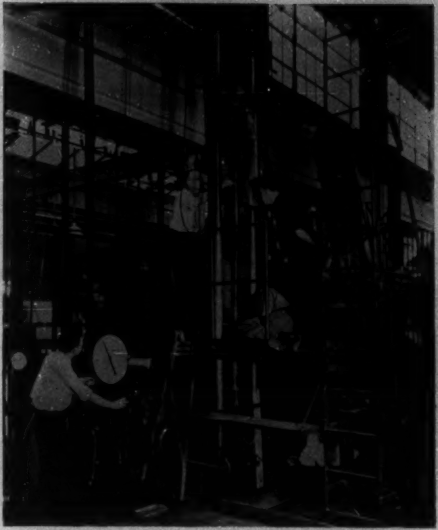

Figure 1. Specimen under test, showing primarily points of observation

under the foregoing groupings, as well as dimensional characteristics, are shown in Table I.

Testing Equipment and Procedure

The pulling grips used were furnished by Kellems Company for all except a few preliminary samples. The manufacturers' statement relative to the design, selection, and performance of pulling grips has been incorporated with this text as a matter having direct bearing upon this work and also because of its potential value in the selection of grips for application in the field.

Tests were conducted in the John A. Roebling's Sons Company laboratory. An Amsler testing machine of 40,000 pounds capacity was used. Samples tested with pulling grips were under the 10,000 pound range, and samples fitted with pulling eyes were under the 40,000 pound range.

The original end seals were removed from the samples as received. These were replaced with new end seals, each with a bicycle-valve insert.

A *U*-tube mercury manometer was connected to the bicycle-valve stem at each end of the sample by means of a flexible rubber hose. The object of this attachment was to use the manometer as an indicator of the rate and extent to which vacuums would develop as the sheath stretched and pulled away from the end of the cable under increased load. Also, loss of vacuum was to be used as the criterion for failure of the sheath. The thought was that the end seals might not be tight, that the pinching action of the pulling grips may be responsible in some cases for opening up cable end seals, and that faults of this nature could not be detected by observation.

The manometers were connected to the valves in the end seals before any load was applied. Connection of the flexible rubber tube to some of the early samples tested produced a slight pressure indication on the manometers. A slight drainage of compound to the lower end of the sample when it was mounted vertically in the testing machine also produced slight positive pressure readings. As the sheath stretched, this pressure was dissipated, and vacuum developed. In these instances pressure is indicated on the test sheets by a plus sign. As the load was applied, the vacuum in inches of mercury was read to the nearest $^1/_{16}$ inch on each manometer scale.

Measurements of extension of sheath between grips were taken between 20-inch and 40-inch bench marks on the sheath, each mark being at least six inches from the ends of the pulling grips. The variation in distance between bench marks was due to the variation in the length of grips used. As long a gauge length as possible was used on all cables, at the same time using a gauge length that was a multiple of ten inches. The gauge points on the cable moved with reference to two fixed 18-inch scales mounted on the frame of the testing machine. The scales were graduated in hundredths of an inch. All readings on the scales were taken to the nearest $^1/_{100}$ of an inch.

The bottom head of the testing machine was stationary—the top head was movable. The bench marks on the sheath therefore moved upward; hence, the total elongation for any load was equal to the difference between the scale readings at that load and at the initial load. This divided by the gauge length gave the unit elongation in inches per inch. As the load was applied, the movement of these gauge points was determined to the nearest 0.01 inch, read on a steel scale.

Outside diameters of the cable were measured by diameter tape during tests. These were taken at the top and bottom bench marks on the cable sheath unless otherwise indicated.

For the originally planned tests the initial loads applied, as well as the subsequent load increases, were varied somewhat, depending upon the size of the cable. Each load value was held for an interval of five minutes. Sheath elongation and manometer readings were taken at each one-minute interval. The five-minute holding interval for each loading was chosen as a minimum time that a cable, of appreciable size and length fitted with a woven-wire grip, would be subjected to pulling stress during installation. This is estimated as approximately equivalent to pulling a 300-foot length of cable into a conduit at a rate of 60 feet per minute.

The ultimate strength of the sheath was taken as the maximum load indicated on the dial prior to failure.

Chemical analyses of the sheaths were not made. It was presumed that they represented a cross section of commercially pure leads.

All samples were tested at room temperature, approximately 75 degrees Fahrenheit.

The accompanying illustrations (Figures 1 to 4) show the method of mounting

A-1
A-2
A-3
A-4
B-1
B-2
B-3
B-4

Figure 2. Fractures of cables *A*-1 to *B*-4 inclusive

the eight-foot samples, the type of testing equipment used, types of end terminations, the nature of the sheath fractures with both woven-wire grips and socket-type pulling eyes.

Pulling Grips

Experience with miscellaneous stock pulling grips demonstrated that the proper selection of size, type, and application of the grips to the ends of the cable was necessary if tests of the samples to destruction were to be realized.

The Kellems Company's representative was present to observe the performance of the pulling grips under load during the planned series of tests and contributed the following statement:

> Two types of grips were used, one the old type, constant weave, in which every mesh was exactly the same size as every other mesh in the grip. In other words, all wires were crossed at exactly the same angle throughout the length of the grip. The other type was woven with a variation in the size of the mesh; that is, the angle at which the wires were crossed was varied, decreasing gradually from the receiving end to the pulling end of the grip.
>
> It was found that the variable-mesh grip is much more effective than the constant-mesh grip, as the strain is evenly distributed over a greater area of the cable. In the constant-mesh grip, the strain is concentrated near the pulling end of the grip, and there is a tendency to localize the bite of the grip and cut into the sheath at this point.
>
> Also, the flexible variable-mesh grip held, whereas the constant-mesh stiff grip had to be served down with wire to the cable to keep it from slipping under heavy loading.
>
> As a result of these tests, certain improvements in design were made to prevent slippage and to improve distribution of strain. These improvements are now standard in current manufacture.
>
> In the past the tendency has been to use grips with too high a breaking strength; the strength of the grip is many times that of the cable sheath. When the grip is sufficiently strong but not far in excess of the cable sheath strength, the result is a more flexible grip and a more even distribution of the strain on the cable. This increases the effectiveness of the grip and also the holding power of the grip to the cable under it.
>
> There is a definite relationship between diameter and length of grip—the greater the diameter, the greater the length required. This is true only to a limited extent with the old-type constant-mesh grips, since the strain is concentrated near the pulling end of the grip. In the variable-mesh pulling grips this relationship between diameter and length has been taken into consideration, and larger diameter grips have been made proportionately longer. Throughout each set of cable size ranges, the length of the grip is increased with the load rating.

A specimen test-data sheet has been reproduced in Table I*A*. This shows how gradually the deformation of the sheath takes place under an increasing static load applied for the time intervals shown.

Stress-strain relations for a selection of samples representative of each classification of cable as given in Table I are shown in Figure 5.

Stress-vacuum relations for these same cables are shown in Figure 6. The shift in the relative position of the curves for the respective samples is due to using the average of the manometer readings from the two ends of the sample.

The summary of information pertaining to all but a few preliminary and special test samples is given in Table III which gives some idea of the dimensions and relative performance of the different cables. The tensile stress values from Table I are regrouped in Table II in order to get a comparison of average, maximum, and minimum values obtained for the types of cable under the respective classifications.

As a result of the work shown in Tables I, II, and III it is recommended that the values of permissible pulling load and the maximum length of lead-sheathed impregnated-paper cable solid type, that should be pulled with a woven-wire grip without damage to the cable, given in Table IV, be used as a first approximation.

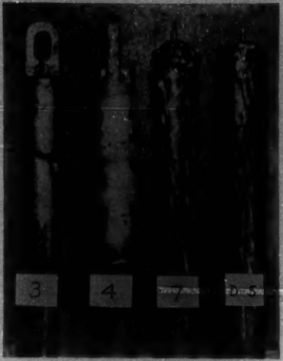

Figure 3. Samples showing typical valve inserts

Values given in Table IV and equation 1 are based upon a maximum permissible stress for commercially pure lead sheath of 1,500 pounds per square inch and a conduit friction coefficient of 0.4.

Comparable values to those given in Table IV for a cable of other sheath dimensions may be calculated by using the formula.

$$S_m = 4{,}712t(D-t) \text{ pounds} \qquad (1)$$

The recommended maximum permissible pulling length of a given cable may

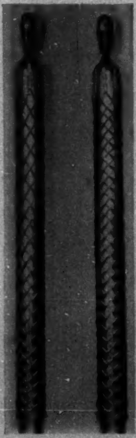

Figure 4. Kellems' pulling grips

Left—Variable mesh
Right—Constant mesh

Table III. Outside-Diameter Measurements Along Sample After Break

Diameter Tape—Inches

Distance From Bottom	A-1 Specimen 1	A-1 Specimen 2	A-3	A-4	B-1	B-2	B-3	B-4	C-1	C-2	C-3	C-4	C-5	C-6	D-3	D-4	D-5
0′ 6″	1.210	1.215	1.255	1.735	1.245	2.20	1.19	2.70	1.745	2.42	2.21	2.09	1.29	1.80	2.90	2.27	3.24
1′ 0″	1.210	1.210	1.250	1.730	1.255	2.205	1.20	2.71	1.745	2.415	2.22	2.07	1.285	1.795	2.89	2.265	3.235
1′ 6″	1.205	1.205	1.235	1.705	1.255	2.185	1.195	2.695	1.735	2.405	2.205	2.07	1.28	1.795	2.865	2.26	3.21
2′ 0″	1.180	1.125	1.225	1.635	1.225	2.15	1.185	2.685	1.735	2.37	2.20	2.035	1.255	1.77	2.805	2.255	3.185
2′ 6″	1.160	1.160	1.225	1.695	1.235	2.095	1.17	2.66	Fracture	2.375	2.165	2.04	1.255	1.745	2.79	2.235	3.19
3′ 0″	1.185	1.185	1.240	1.705	1.245	2.215	1.165	2.665	1.72	2.38	2.135	2.025	1.265	1.76	2.825	2.24	Fracture
3′ 6″	1.195	1.195	1.245	1.725	1.25	2.115	1.165	2.635	1.725	2.39	2.125	2.05	1.28	1.77	2.855	2.245	3.195
4′ 0″	1.205	1.200	1.255	1.725	1.245	2.105	1.175	2.67	1.735	2.395	2.15	2.05	1.28	1.78	2.875	2.25	3.185
4′ 6″	1.200	1.200	1.255	1.735	1.25	2.125	1.17	2.67	1.735	2.395	2.14	2.055	1.28	1.80	2.885	2.23	3.20
5′ 0″	1.200	1.195	1.245	1.74	1.25	2.145	1.18	2.67	1.72	2.385	2.14	2.04	1.28	1.79	2.90	2.22	3.21
5′ 6″	1.190	1.180	1.240	1.735	1.24	2.135	1.18	2.685	1.72	2.38	2.125	2.025	1.28	1.775	2.885	2.20	3.19
6′ 0″	1.185	1.170	1.225	1.725	1.24	2.115	1.175	2.665	1.70	2.37	2.165	2.015	1.275	1.775	2.84	2.175	3.185
6′ 6″	1.130	1.145	1.210	1.705	1.205	2.105	1.17	2.655	1.72	2.335	2.195	2.00	1.265	1.77	2.785	2.26	3.20
7′ 0″	1.200	1.150	1.250	1.69	1.235	2.10	1.175	2.59	1.725	2.405	2.20	2.025	1.265	1.755	2.85	2.275	3.215
7′ 6″	1.210	1.205	1.255	1.72	1.25	2.105	1.175	2.625	1.735	2.41	2.20	2.06	1.245	1.73	2.885	2.265	3.225
8′ 0″	1.210	1.210	1.260	1.745	1.25	2.13	1.185	2.68	1.74	2.42	2.20	2.075	1.22	1.73	2.895	2.27	3.245
8′ 6″	1.210	1.215		1.745		2.16	1.175	2.70			2.21	2.085	1.27	1.78	2.885		3.24
9′ 0″						2.17	1.195	2.715					1.285	1.79	2.885		
9′ 6″						2.18		2.71					1.295	1.79			
10′ 0″								2.52					1.30	1.795			
10′ 6″								2.725									
Failed at	2′ 2″	6′ 8½″	6′ 4″	2′ 0″	6′ 9″	2′ 7½″	2′ 5″	3′ 1½″	2′ 6″	6′ 4½″	3′ 3½″	6′ 7½″	7′ 9″	7′ 8″	2′ 1½″	5′ 9″	3′ 0″
Outside diameter*	1.120	1.120	1.195	1.630	1.170	2.055	1.155	2.52	1.66	2.32	2.125	1.985	1.230	1.720	2.775	2.150	3.13
Stress	3539	3106	3146	3669	4349	2470	2596	2165	3325	2838	2395	3067	3019	2954	3382	2698	2434
Elongation, inch per inch	0.0717	0.1220	0.0677	0.0577	0.0203	0.2037	0.0650	0.1057	0.0623	0.0837	0.1890	0.0875	0.0483	0.0662	0.0525	0.1335	0.0827
(a). Location of Sheath Failure Relative to End of Grip																	
Under																	
Upper grip		2″	4½″		10″		1½″†			5″	3″†	10″	3″	4½″		3″	
Lower grip	3″			6″		3″		1½″	3″						13½″		1″
(b). Approximate Weight of Cable—Pounds Per Foot																	
Approximately pounds per foot	3.2	3.2	2.8	6.7	3.7	5.2	2.4	9.9	5.6	10.8	11.0	6.7	2.6	4.8	15.4	8.6	14.8
(c). Vacuum Developed—Inches of Mercury																	
Top end	13.5	17.75	14.25	11.25	0.5	23.0	8.0	25.5	11.25	24.0	19.88	22.75	18.38	16.38	26.25	16.0	19.5
Bottom end	12.5	11.75	15.75	11.625	0.0	22.88	14.75	23.5	11.5	10.0	19.38	22.88	15.5	18.88	26.88	16.13	25.0

* Diameter tape measurements adjacent to the sheath failure.

† Exception—failure outside the grip.

Table IV. Maximum Pulling Loads and Calculated Length of Paper Cable Permissible When Pulled With a Woven-Wire Grip

Core Diameter Range (Mils)		Nominal Thickness of Sheath (Inches)	Outside Diameter Range (Inches)		Recommended Maximum Permissible Pulling Load (Pounds)		Recommended Maximum Permissible Pulling Length (Feet)	
Minimum	Maximum		D_1	D_2	P_1	P_2	L_1	L_2
251 –		0.078	0.407		120.9		302	
	– 600	0.078		0.756		249.2		623
601 –		0.086	0.773		278.4		696	
	– 900	0.086		1.072		399.6		999
901 –		0.094	1.089		440.8		1,102	
	–1,200	0.094		1.388		573.2		1,433
1,201 –		0.101	1.403		618.4		1,546	
	–1,500	0.101		1.702		761.9		1,905
1,501 –		0.109	1.719		827.0		2,068	
	–1,800	0.109		2.018		980.6		2,452
1,801 –		0.117	2.035		1,058.0		2,645	
	–2,100	0.117		2.334		1,222.0		3,055
2,101 –		0.125	2.351		1,311.0		3,278	
	–2,400	0.125		2.650		1,487.0		3,718
2,401 –		0.133	2.667		1,589.0		3,973	
	–2,700	0.133		2.966		1,776.0		4,440
2,701 –		0.141	2.983		1,889.0		4,723	
	–3,000	0.141		3.282		2,087.0		5,218
3,001 –		0.156	3.313		2,320.0		5,800	
	–3,300	0.156		3.612		2,541.0		6,353
3,301 –		0.164	3.629		2,678.0		6,695	
	–3,600	0.164		3.928		2,909.0		7,273
3,601 –		0.172	3.945		3,058.0		7,645	
	–3,900	0.172		4.244		3,300.0		8,250

be calculated on a comparable basis by using the formula

$$L_m = \frac{S_m}{cw} \text{ feet} \qquad (2)$$

The thickness t and outside diameter D of the cable sheath are in inches. The recommended maximum pulling load S_m is expressed in pounds, the weight of cable w is in pounds per foot, and the pulling length L_m in feet.

The use of these formulas requires but a limited amount of information including the weight per foot, outside diameter, and thickness of sheath, all of which should be available from the reel marking.

Interpolation of permissible pulling loads for standard thicknesses of sheath of intermediate diameters may be made from Table IV or taken from Figure 7.

The maximum recommended pulling-length values, Table IV are for cable having a weight of one pound per foot. These should be divided by the weight per foot of the cable to be pulled.

In instances where the use of a high-strength brand of commercially pure lead or an alloy sheath is required, a higher maximum stress value than 1,500 pounds per square inch could be justified.

A modification of the maximum permissible pulling length could be justified likewise, if facts are available as to prevailing coefficient of friction, duct condition, grade, bends, clearance, lubrication, and snubbing action to be expected at the entrance manhole.

Observations From Test Procedure and Results

None of the sheath failures under the original series of tests were between bench marks. With two exceptions the failures were under the grip and within ten inches of the back end. The two failures beyond the grips were $1^1/_2$ and 3 inches from the back end as shown in section (*a*) of Table III.

The recorded elongations do not represent the maximum strain values, because they do not involve that portion of the sheath including the failure.

Under the grip where the great majority of the failures occur, the resultant load is believed to be a combination of lateral compression and longitudinal tension in contrast to substantially tension stress only on that part of the sheath between the bench marks.

In general, the angularity of the sheath failure corresponded with that of the underlying paper insulation or shielding tape. This indicates a binding of the sheath to the core.

The paper tapes or the shielding tape and paper tape underlying the lead sheath at the point of failure showed appreciable change in mechanical uniformity. It is not known whether this change in position takes place gradually at these locations of greatest extension of the sheath or whether the change occurred when the sheath parted.

There did not seem to be serious abrasion of the sheath because of the grips, as slippage was practically eliminated in most cases.

Sample 7, Figure 3, which had the conductors soldered into the end seal, could not be tested to destruction, as the sheath bulged just back of the end seal which caused the grips to slip.

If cable end seals of usual design are properly made at the factory, there is little prospect of damaging them at any permissible stress to which the cable sheath should be subjected, especially if pulling grips of the reinforced type are used. There was but one instance where vacuum did not develop at the end of the sample.

Examination of the samples showed that there was appreciable coning of the

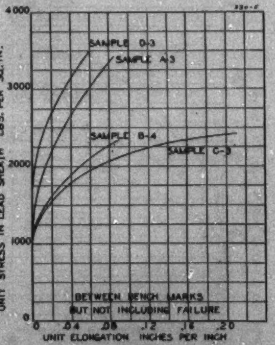

Figure 5. Stress strain relation—lead cable sheath

Table VA

A. First Set

Section No.	Top 1	2	3	4	5	6	Bottom 7
Bench Marks (Inches)	10	10	20	10	20	10	10
Sample A	Elongation in inches per inch at initial maximum stress of 2,180 pounds per square inch						
	000	0.013	0.066	0.037	0.054	0.012	000
	After 46 minutes failure occurred—ultimate elongations						
	000	0.036	0.180	0.088	0.098	0.030	000
	Failure occurred 3 inches within the back end of upper grip						
Sample B	Elongation in inches per inch at initial maximum stress of 2,400 pounds per square inch						
	000	0.018	0.111	0.076	0.044	0.018	0.002
	After 7 minutes failure occurred—ultimate elongations						
	0.001	0.015	0.129	0.097	0.096	0.019	0.003
	Failure occurred just below the back end of the upper grip						

Table VB. Summary of Application of Load and Test Results

B. Second Set

	Specimen			
	8g	8i	8h	8f
Time of loading (seconds)	80	155	350	420
Time of loading (minutes)	1.33	2.58	5.83	7.0
Area of sheath (square inches)	1.586	1.609	1.604	1.641
Stress at failure (pounds per square inches)	3,000	2,870	2,760	2,570
Rate of stress application (pounds per square inch per minute)	2,250	1,110	473	367
Load at failure (pounds)	4,800	4,580	4,410	4,120
Rate of loading (pounds per minute)	3,600	1,775	757	588
Elongation 10 inches (inches per inch) measured at center of specimen	0.218	0.144	0.180	0.165
Vacuum (inches of mercury)				
Top end	−26	−29	$-28^{1}/_{8}$	$-28^{1}/_{8}$
Bottom end	$-22^{1}/_{4}$	$-24^{7}/_{8}$	$-24^{3}/_{8}$	$-19^{5}/_{8}$
Outside diameter (inches)				
Before test	3.27	3.28	3.27	3.27
After test	3.20	3.23	3.22	3.20
Elongation 10-inch gauge length (inches per inch)				
Location along sample				
Top 1	0.000	0.000	0.000	0.000
2	0.030	0.015	0.010	0.031
3	0.190	0.130	0.080	0.136
4	0.215	0.145*	0.267*	0.173
5	0.210	0.142	0.180	0.180
6	0.295	0.145	0.187	0.244*
7	0.287*	0.080	0.112	0.037
8	0.053	0.050	0.015	0.000
Bottom 9	0.000	0.000	0.000	0.000
End displacement (inches)				
Top	5.4	7.7	7.2	0.9
Bottom	9.5	3.5	3.9	5.3

* Nearest to point of failure.

insulation off the end of the conductors of single-conductor cable, not so much on belted cable, and very little of this at the ends of type-*H* cable.

A maintained load of 1,000 pounds per square inch of sheath causes the sheath to stretch and start to pull off the end of the cable.

A load of 1,500 pounds per square inch produces measurable elongation of the sheath between grips almost as soon as the load is applied.

There is a very appreciable, if not destructively excessive, increase in the rate of elongation of the sheath at a stress of 2,000 pounds per square inch.

With the rate of loading used, the sheaths ruptured at apparent stresses of 2,300 to 4,300 pounds per square inch, calculated on the basis of the average area of rings taken from the ends of the cable sample after test.

The indicated tensile strength of lead sheath recorded in Table II shows higher average and maximum values for single-conductor cable than for multiple-conductor cable. This is likewise true of minimum values with but one exception. The tensile strength indicated for three-conductor cable sheath over light wall insulation is about the same for belted and type-*H* cable; however, one unusually low value was obtained on a sample of belted-type cable. Approximately a 15 per cent higher average tensile-strength value of sheath was obtained from three-conductor type-*H* cable with heavy wall insulation than for belted cable. The relatively high apparent stress values indicate that part of the load applied was transmitted to the insulation and conductor.

The apparent tensile strengths of lead sheath given in Table II are appreciably higher than should be expected from tensile tests on samples of commercially pure lead sheath as reported by Moore and Allerman in Engineering Experiment

Table VC. Summary of Application of Load and Test Results

C. Third Set

	Specimen			
	9a	9b	9c	9d
Time of loading (seconds)	90	180	300	450
Time of loading (minutes)	$1^{1}/_{2}$	3	5	7
Area of sheath (square inches)	1.327	1.319	1.289	1.306
Stress at failure (pounds per square inch)	3,310	3,110	3,080	3,390
Rate of stress application (pounds per square inch per minute)	2,210	1,035	615	485
Load at failure (pounds)	4,300	4,050	3,800	4,420
Rate of loading (pounds per minute)	2,870	1,350	760	605
Elongation 10 inches (inches per inch) measured back of the grip	0.090	0.073	0.072	0.062
Vacuum (inches of mercury)				
Top end	$+2^{1}/_{2}$	$-^{1}/_{2}$	+4	−7
Bottom end	$-26^{3}/_{8}$	−24	$-27^{3}/_{4}$	$-20^{3}/_{4}$
Outside diameter, inches				
Before test	3.03	3.05	3.05	3.05
After test	2.98	3.00	2.99	2.99
Elongation 10-inch gauge lengths (inches per inch)				
Top 1	0.027	0.035	0.035	0.037
2	0.043	0.048	0.046	0.032
3	0.080	0.067	0.077	0.035
4	0.148	0.135	0.108	0.067
5	0.304*	0.263*	0.233*	0.165
6	0.165	0.218	0.158	0.317*
7	0.035	0.055	0.032	0.049
8	0.000	0.010	0.000	0.007
Bottom 9	End seal	End seal	End seal	End seal
End displacement (inches)				
Before loading	$1^{1}/_{2}$	$^{5}/_{8}$	$^{1}/_{2}$	$1^{1}/_{8}$
After loading	10	$9^{3}/_{4}$	$8^{1}/_{4}$	$8^{5}/_{8}$
Failure occurred				
Within grip				
From back end	1 inch	3 inches	2 inches	9 inches
Circumferential twist				
Bench marks (feet)	5	5	5	5
Rotation (degrees)	17	17	None	None

* Nearest the point of failure.
Elongation for 9*d* at 6 large owing to double necking down.

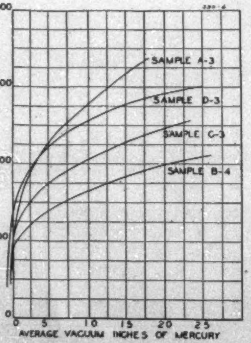

Figure 6. Stress-vacuum relation—lead cable sheath

Table VI

100 Per Cent Lead	Pounds Per Square Inch	
	Longitudinal	Transverse
G	2,010	2,200
H	1,590	1,610
I	1,530	1,570
J	1,910	1,965
Average	1,760	1,838
Deviation from average (per cent)—plus	14	20
minus	13	14

Table VIII. Lead Bend Tests

Test Number	Simplex Method (Cycles)	Standard Method 90-Degree Bends			Description at Bends
		Maximum	Minimum	Average	
0	$11^1/_2$–12	63	45	53	Knife edges
1	$10^1/_2$–12	79	39	53	
2	10 – 9	63	45	51	
3	11 –11	69	49	56	
4	7 –$11^1/_2$	73	43	54	
5	12 –10	57	43	48	
6	$9^1/_2$– 9	71	43	50	
7	10 –10	71	51	58	
8	9 –12	69	43	52	
Range		57–79	39–51	48–58	
Over-all range	7–12		39–79		

The test numbers indicate the number of feet from the top end of the test length to where the rings were cut. The sheath was pulled apart between six and seven feet from the end. Two tests were made by the Simplex method at each location.

Station, University of Illinois, *Bulletin* 243, "The Creep of Lead and Lead Alloys Used for Cable Sheathing,"[6] and Insulated Power Cable Engineers Association "Commercially Pure Lead Sheath for Power Cables," technical report 2, September 1936.[7]

An indication of the effect of loading upon thickness of sheath throughout the length of preliminary sample 1 and in the immediate proximity of the failure may be had from Table VII, discussed in Appendix III.

Determination of the uniformity in quality of commercially pure lead sheath from so representative an assortment of samples as source, diameter, and thickness of sheath seemed desirable. Two methods of bend test were used in order to find out whether results would be equally uniform as to the magnitude of values and variation range. The results of this investigation are reported in Appendix IV.

Special Investigation

It was apparent from the test results under the original investigation that answers to several important questions were lacking.

It was desired to know how the stretch throughout the length of the sample was distributed and to evaluate these incremental elongation values with that of the unfractured sheath recorded in Table III.

It was thought that a supplemental investigation of a series of samples having a common quality of commercial lead sheath and tested under different loading conditions would develop some of the answers.

SPECIAL TESTS

The results of these special tests are reported here as a supplement to the original report.

It was thought that a longitudinal reinforcement of friction tape with a helical wrapping of like material over the sheath which extended from ten inches inside to two inches beyond the back end of the grip would shift the sheath fracture to occur between the regular bench marks. This it failed to do, but it did eliminate any appreciable tendency of the grips to slip. All special test samples were so reinforced.

The three sets of special test results are reported individually because of variations in procedure. (Tables V*A*, V*B*, and V*C*.)

A. First Set. The purpose was to determine the extent of the elongation that occurred in various sections of the sample lengths and to compare the strain on the sheath between bench marks at the center of the sample with that in the section under the grip which includes the failure.

The sheaths of two specimens were graduated by 10-inch and 20-inch bench marks. Wire pulling grips were used on both ends of the samples. The load was applied gradually, and readings were taken at each increment increase of 1,500 pounds, allowing only time necessary to take manometer reading and to measure the elongation and diameter over the sheath between the bench marks at the center of the specimen.

Samples used were three-conductor 500,000-circular mil Compack $^{33}/_{64}$-inch paper, $^9/_{64}$-inch lead.

B. Second Set. The purpose was to determine the time-ultimate stress relation, the variation in elongations between ten-inch bench marks, throughout the length of the samples, and the variation in strain at the center of the sample and in the location containing the failure. Wire grips were used on both ends of the samples.

The ultimate stress was estimated as 3,000 pounds per square inch for all rates of loading. The time intervals of loading were set to approximately $1^1/_2$, $2^1/_2$, 5, and 7 minutes. These rates of loading were reasonably well realized by the use of a stop watch and the testing-machine loading valve.

The same cable was used as that for "*A*. First Set."

C. Third Set. The first and second sets of special tests show that stretching of the sheath takes place more or less

Table VII

Test Number	Lead Thickness (Mils)		
	Maximum	Minimum	Average
0	140	123	131
1	122	107	113
2	128	108	117
3	124	108	113
4	125	113	118
5	125	112	117
6	114	102	110
7	131	110	120
8	147	119	130
Range	114–147	102–123	110–131
Over-all range		102–147	

Sheath fractured between the sixth and seventh foot.

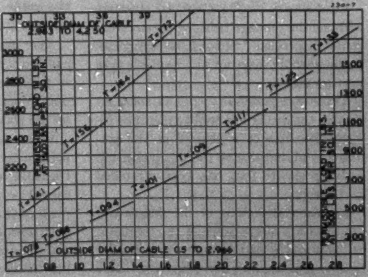

Figure 7. Permissible load in pounds on lead-encased cable

evenly at both ends of the samples when pulled with woven-wire grips.

The purpose of this set of tests is to determine how the elongation of the sheath takes place when the relative effective length of the stretched sheath is doubled by placing a pulling eye on one end and a woven-wire grip on the other end of the sample.

An ultimate stress was estimated as 3,000 pounds per square inch for loading intervals of $1^{1}/_{3}$, $2^{1}/_{2}$, 5, and 7 minutes respectively.

The sheath was graduated by ten-inch bench marks. The increment laid off just outside of the grip was used for elongation measurement readings while the sample was being loaded. The outside diameter recorded was taken halfway between these bench marks. The lower of these bench marks on the grip end was located about four inches above the back end of the grip on account of the reinforcing tape over the sheath.

A line was drawn from end to end of each sample to be used as a reference in determining whether or not the cable twisted while being pulled.

The samples were mounted in the machine with the wire pulling grips at the bottom.

The end displacement is an average of several probe depth measurements through the valve stem in the end seal, minus the length of the valve stem.

Samples used were three-conductor 500,000-circular mil Compack $^{19}/_{64}$-inch paper, $^{9}/_{64}$-inch lead.

Elongation of the sheath extends back to the wipe at the pulling-eye end. It is expected that elongation of the sheath would take place over a greater distance if longer samples could have been used.

Conclusions

If stresses in commercially pure lead sheath are in excess of 1,500 pounds per square inch, vacuums of high magnitude are quite likely to develop.

Lead sheath can be stretched seriously without the damage being apparent from visual inspection. Serious stretching can be detected as variations in diameter tape measurements.

There is a possibility that in some instances the insulation quality might be reduced because of a change in the mechanical uniformity of the insulation underlying sheath that has been stretched excessively.

Excessive stretching of the sheath and the resulting disturbance of the mechanical uniformity of the underlying insulation could account for excessive cable heating and many manhole and joint failures of the past.

There is need for extreme caution, when luffing or ratcheting cable for slack in manholes, or in removing cable to be reinstalled, not to damage it by stretching the sheath excessively.

The extent of stretching occurring during pulling or as a result of adjustment of the cable in the duct by luffing can be determined readily by placing ten-inch bench marks along the sheath under and just beyond the back of the grip.

Excessive stretching of the lead sheath off the end of the cable produces a high degree of vacuum which may be responsible for the creation of voids within the insulation some distance back from the end of the cable through the transfer of compound from the insulation into the space developed at the pulling end of the section.

It is possible that the effect of this migration of compound may have accounted for an appreciable percentage of cable failures in the pulling end of section lengths within the manhole or adjacent to the conduit entrance.

There is appreciable cold flow of commercially pure lead sheath if a stress of 1,500 pounds per square inch is exceeded, which not only reduces its effective thickness but may reduce its flexure fatigue resistance.

Overstraining of the cable sheath might be avoided in many cases by checking experienced judgment against a precalculated value of maximum recommended load or cable length by means of the proposed formulas.

Calculation of the maximum permissible load to which a cable should be subjected is of questionable value for marginal cases, unless some practical device is available that will indicate continuously the actual load to which the cable is subjected or limit the load at the chosen maximum permissible value during installation. If such facilities are not available, there is no way of telling whether the cable sheath may have been overstressed during installation.

The use of woven-wire pulling grips can be justified with practically any size or type of cable where there is adequate duct room and the safe maximum stress on the sheath is not exceeded.

When deciding upon the type of connection to be used on the pulling end of the cable in marginal cases, consideration should be given to the value of the cable per foot that may be lost through damage from excessive stretching of the sheath and the potential saving in reducing manhole slack allowances if pulling eyes are used. In case of doubt, as with borderline cases, it is always safer to require the attachment of pulling eyes.

Factual field information gathered from installation experience through the use of the simple device of placing ten-inch bench marks along the pulling end of the cable would prove valuable in establishing some limit of elongation of sheath beyond which it could be considered quite definitely that the quality of the cable had been impaired.

Samples of cable can be prestressed by the use of pulling grips to a point where the sheath has been stretched excessively. These samples could be subjected to cyclic loading tests to determine what effect the disturbance of mechanical uniformity caused by excessive stretching of the sheath has on the performance in comparison to that of an adjacent nonprestressed sample of the same cable.

In the case of small conductors where the permissible load would be limited by the formula 0.008 × conductor circular mils, the permissible load on the lead sheath may be added to that permitted on the conductor if the conductor and lead sheath are joined securely to a pulling eye.

It is hoped that a study of the data presented will stimulate discussion of an old subject and lead to the development of more adequate control facilities for field use and to the adoption of a few practical rules for the guidance of men in the field, who, after all, carry the responsibility for the proper handling of cable installations.

If, as a result of this work, there is any contribution that will make it possible for cable engineers of the utilities and the manufacturers to discriminate as to when pulling eyes or socketing of conductors into the end seals should be specified in preference to the conventional type of end seal, the expense and effort will have been justified.

Appendix I. Excerpts From Literature

The National Electric Light Association's underground systems committee reports on cable installation records current practice among utilities as of 1925.[1] The 1926 report[2] summarizes opinion as to the effect of stress upon the cable and states the means of judging damage as follows:

> "Information obtained—principally from the cable manufacturers—leads to the conclusion that the electrical characteristics will not be effected if the cable physical characteristics be not disturbed.
>
> "In summarizing the reports from the manufacturers and from the operating companies, it would appear that in the ordinary installations of cable no serious harm can be done the cable unless there be visible mechanical injury, which has led one of our members to inject the thought that it is 'highly undesirable to install such long lengths of cable as

would overstress the lead sheath either in installation or removal, having in mind the fact that in many cases it is necessary to take extra hitches on the cable in order to secure sufficient slack in the manhole into which the cable is pulled."

There is available extensive information relative to stresses on cables fitted with pulling eyes as a result of the field experiments conducted by Carroll H. Shaw.[3]

Factors influencing the coefficient of friction found in connection with cable pulls are presented in the NELA underground systems report on "Cable Installation for 1930."[4] The committee's conclusion as to available material relative to coefficient of friction range to be considered in the pulling of cable is quoted from NELA underground systems Reference Book.[5]

"A conservative rule of thumb that has been used allows a pulling tension of 0.75 times the total weight of cable being pulled. If this limit is greatly exceeded, it would appear advisable to look for possible damage to the cable sheath such as scoring, gouging, or deformation. Certainly under no conditions should the stress be large enough to produce permanent elongation of the copper. The factor 0.75 mentioned above is the coefficient of friction between cable and duct. This varies widely depending upon conditions.

"For well-lubricated well-constructed straight ducts with ample clearance between cable and duct the coefficient of friction will average about 0.40."

The NELA underground systems committee, in its 1930 report on cable installation,[4] summarized its conclusions on this phase of their report as follows:

"The problem has been considered by the subcommittee this year on the basis that:

"1. No limit to the pulling stress has been set by the manufacturers or users of power cable, and it is therefore one of the duties of the committee to render a decision on this point.

"2. The maximum stress to which the cable is subjected is the only figure of value in determining whether

"(*a*). The cable has been injured during installation.

"(*b*). A value for the safe maximum stress to which a cable may be subjected.

"In discussion of the possibility of setting a safe value for the maximum pulling stress, it will be found in all cases that the injury to the cable is evident on the sheath of the leading end, except in those cases where, through oversight, the cable has jammed at the entrance to the duct. In the latter circumstance the dynamometer reading will not indicate necessarily that damage has been done. If, however, the cable has been overstressed mechanically, the leading end is the only portion of the cable which has sustained the maximum stress, and this is the stress recorded by the dynamometer. The stresses which exist in the cable behind the leading end are unknown and indeterminate by any practical measuring device but of a lower magnitude than the stress on the leading end which has already been determined. The leading end, however, is brought out for visual inspection at the end of the pull, and, if the conductors are not pulled away from the sheath, and the lead is not damaged, there is no possibility that dynamometer readings could indicate other damage.

"This accessory is important, however, for improving installation methods and devices, especially in connection with new or unusual cable types, and a great deal of experimental work has been done by the larger companies along this line."

Appendix II. Short-Time Tensile Strength of Lead Sheath

The apparent stress values obtained from the samples of cable tested and shown in Table II are appreciably higher than reported tensile-strength values for commercial lead cable sheath.

Short-time tensile test results reported by Moore and Allerman[6] on sections taken from samples of cable sheath in both longitudinal and transverse directions to the axis of the sheath and tested at a head speed of 0.02 inch per minute are given in Table VI.

Results of short-time tests on circumferential strips of lead sheath reported in connection with the joint Association of Edison Illuminating Companies and Insulated Power Cable Engineers Association research technical report[7] show for commercial cable sheath without reference to quality of lead:

Weighted average of all..	average value.....	2,362 psi
	minimum value...	1,569 psi
	maximum value ..	2,660 psi

These results cover tests made at 11 different laboratories on samples taken from the same sheath lengths. Testing speeds varied from 0.2 to 1.25 inches per minute.

Appendix III. Physical Test Results from Lead Sheath After Being Subjected to a Loading Test

It was thought desirable to get some indication of the effect of stretching on thickness and physical characteristics of the sheath throughout the length of a sample, as indicated by the IPCEA and the Simplex equipments and procedures for bend tests. (See Tables VII and VIII.)

One of the samples used for preliminary tests in setting up the test procedure was ringed at the ends and at one-foot intervals. One two-inch ring and four one-half-inch rings taken at each location gave the results shown in Table VII.

Figure 8 shows the location and numbering of test rings in the immediate proximity of the failure of preliminary sample 1.

A one-half-inch ring was taken for standard bend test as close as possible to the sheath fracture (1*B*), one above (1*T*), and the other below (1*B*). Adjacent to the lower ring 1*B*, successive rings 2*B*, 3*B*, and 4*B* were examined with the results indicated in Table IX.

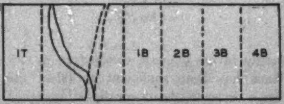

Figure 8. Diagram showing the location and numbering of test rings adjacent to failure

T—Top end during pulling strain test
B—Bottom end during pulling strain test

Table IX

Sample	Thickness—Mils			IPCEA Bends 90 Degrees			Cycles (Simplex)	
	Maximum	Minimum	Average	Maximum	Minimum	Average	1	2
1*T*	106	95	102	57	47	50*		
1*B*	121	86	102	55	45	51*		
2*B*	120	96	108					
3*B*	113	102	107				$7\frac{1}{2}$	$7\frac{1}{2}$
4*B*	114	104	107	50	45	51*		

* All knife edges.

The fractures follow the helices of the paper tape directly under the sheath. The lead thins down in a long taper to a knife edge at the fracture as expected. Stretching of the sheath apparently did not change the bending-test results seriously.

Appendix IV. Bending Tests on Samples of Lead Sheath

The standard bend-test procedure and equipment required under the Specification for Impregnated Paper Insulated Cable, Solid Type (sixth edition, November 1937), high tension cable committee, Association of Edison Illuminating Companies, and an alternate known as the Simplex method were used. The latter has been under consideration as a possible standard for procedure in manufacturing control work. It is made quickly and involves the entire circumference of the sheath in a single test.

The rings for test were taken about four inches back of the seals from each end of the test length. Adjacent rings from each end were used for the respective tests. The sheath had not been abraded because of the protection by the pulling grip reinforcement.

Table XA

Cycles	Set 1	Set 2
Average	11.7	11.4
Minimum	9	7
Maximum	14.5	14

Table XB

	All Top Samples	All Bottom Samples
Average	11.5	11.7
Average—all samples	11.6	
Maximum spread—all samples	7 to 14.5	

The arithmetical averages of the number of 90-degree bends and the range of values under the standard method are as follows:

Maximum 69.3 with spread 57–83
Minimum 47.1 with spread 31–61
Average 57.7 with spread 48–67
Over-all range 31 to 83 bends

The arithmetical average of the performance tests made on the Simplex tester showed for two sets of samples, each set consisting of a ring from the top end and from the bottom end of the cable length, the results shown in Table X*A*.

If tests of all top samples and tests of all bottom samples are averaged, they show the results given in Table X*B*.

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Ampere Load Limits for Copper in Overhead Lines

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Synopsis: The paper presents a method of determining approximately the maximum continuous current-carrying capacity of conductors in overhead lines, as fixed by certain operating limits of temperature and time, beyond which it is expected that the physical characteristics of copper conductors might be materially impaired. The paper deals illustratively with conditions in the Philadelphia area, but the method is equally adaptable to any situation.

The design limits of ampere ratings determined herein, are from 10 to 35 per cent higher for bare conductors, and from 10 to 20 per cent higher for covered conductors, than those previously used in the Philadelphia area. Operation at load equal to the design limit will, during 99.93 per cent of the hours in the average year, maintain copper temperatures below 100 degrees centigrade in normal operations and below 135 degrees centigrade in emergency operations.

UNDER ordinary conditions and particularly in the design of new single-purpose lines, there is little doubt that some sort of cost balance akin to Kelvin's law favors conductor sizes sufficiently large to give operating-temperature limitations a place of rather remote interest. Considerations of voltage variation and the advance provision for expected load growth in general-purpose distribution lines also favor the initial installation of conductor sizes considerably larger than the minimum that could be used if necessary.

The present emergency naturally compels a reconsideration of the design limits of conductor size, but not without a possibility of substantial future advantage to those central-station systems which will utilize the information thus gained to make the maximum use of an installed line-conductor size, rather than to abandon it in favor of an avoidable change to a larger conductor size. In the authors' experience there has been no instance where the natural cost balance that normally favors oversize conductors in new lines has been sufficient to offset the large expense that must be accepted when an otherwise adequate line conductor size must be abandoned in order to provide a larger size.

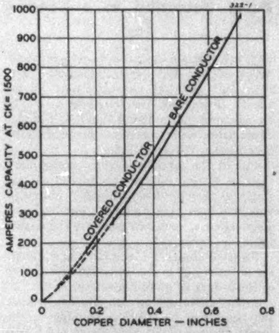

Figure 1. Principal effect of conductor diameter on ampere capacity of overhead lines

$$I = CKd\sqrt[4]{d+2s}$$

Line accessories such as conductor clamps and disconnects can have limitations that will become critical. Likewise, it is always possible that conductor clearances will need to be reconsidered at critical points, along lines which possibly might be required to operate at abnormally high conductor temperatures. Such limitations may be removed by thoroughly normal procedure and, usually, at much lower cost than to increase the conductor size. The working limits of the conductor material in its cooling medium must be known, however, before the other factors can be considered appropriately. This paper, therefore, centers its attention upon an approximate appraisal of present knowledge about the physical limitations of copper and a reconsideration of the natural heat-dissipating properties of conductors in overhead lines, as related to their effect upon the practical design limits of ampere loading for overhead lines.

Mechanical Limitations of Bare and Covered Copper

To the extent that copper may be said to have elasticity, then elasticity is the property of overhead line conductors which is most affected by temperature and time. Soft-drawn copper has very little and hard-drawn copper has the most but whatever elasticity is possessed by any conductor will be impaired to the extent that temperature and time may anneal the copper. The natural drawing effect which accompanies the mechanical stretching of copper imparts some measure of elasticity to it which it did not possess previously. Hence, mechanical tests of copper conductors, after partial annealing without mechanical stress may not be directly comparable to similar tests of identical conductors similarly heated, while kept under normal stringing tension. All of these factors contribute to a natural uncertainty that makes the mechanical characteristics of copper at comparatively low temperatures appear to be almost akin to the mechanical characteristics of alloy steels at much higher temperatures.

There appears to be considerable uncertainty, for instance, whether copper begins to anneal at 100 degrees centigrade or at 130 degrees centigrade. The range of uncertainty suggests, however, a probability that operating temperatures may be permitted to exceed 100 degrees centigrade on occasion, without material effect upon the mechanical properties of overhead conductors, provided the temperature will seldom exceed, say, 130 degrees centigrade.

There are occasions, happily very infrequent in well-managed distribution-system operations, however, when the

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rare coincidence of heavy loads with the failure of supply plant places the operator in a dilemma. His alternatives in such a circumstance are to mistreat certain surviving supply plants, to cut off certain customers, or to delay restoration of interrupted service. His choice naturally favors the customer, up to the point where the imminence of surviving plant failure seriously threatens inconvenience to other customers as well. The customer is pretty reasonable about a service interruption, so long as he knows that the interruption was unavoidable. If he were a metallurgist, however, he probably would be reluctant to agree that literal observance of a 130 degree centigrade line-conductor temperature limit was sufficient reason for a service interruption. He might have some reason to suspect that temperatures as high as 175 degrees centigrade to 200 degrees centigrade might be accepted for an hour or two without material impairment to the line conductor, because that is the range in which the annealing time is known to be in the order of days rather than weeks or years.

A test was made, therefore, to determine approximately what damage the conductor and its automatic line splices might sustain during operation for a few hours at 175 degrees centigrade to 200 degrees centigrade. The test was made in a 12-foot frame, in which four short sections of bare medium hard-drawn, number 00 stranded copper conductors and three automatic line splices were connected in series and stressed to a constant tension of 1,000 pounds—the approximate stringing tension. Thermocouples were placed on two sections of the conductors and in each of the line splices. A temperature of 200 degrees centigrade was maintained essentially constant at a control point on one section of the conductor for about eight hours a day on six successive days for an aggregate of 44 hours. During the test, the other section of conductor maintained an essentially constant temperature of 180 degrees centigrade. The line splices were about 20 degrees cooler than the conductors and gave no evidence of any change in their contact resistance.

After the temperature tests were completed, approximate (eight-inch extensometer) stress-strain tests were made on each of the test samples and also on an unheated sample of the original conductor from which the test samples had been cut. The unheated sample of number 00 conductor broke at 5,500 pounds tension after about 0.9 per cent elongation. The 180 degrees centigrade sample broke at 4,600 pounds tension after 1.7 per cent elongation, whereas the 200 degrees centigrade sample had 30 per cent elongation without breaking at 3,600 pounds tension. In each test, the strength of the splices was superior to that of the conductor.

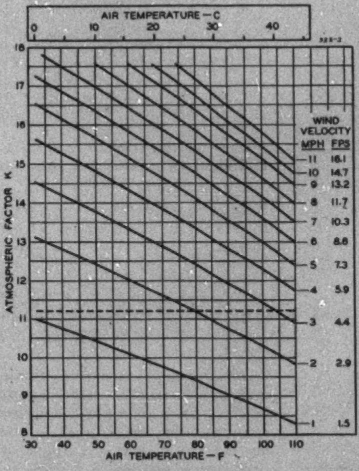

Figure 2 (left). Effect of air temperature and wind velocity upon atmospheric factor *K*

100 degrees centigrade surface—overhead

$K = \sqrt[4]{(t_s - A)^5 v}$

where

t_s = temperature of conductor surface—100 degrees centigrade

A = ambient temperature of air—degrees centigrade

v = wind velocity—feet per second

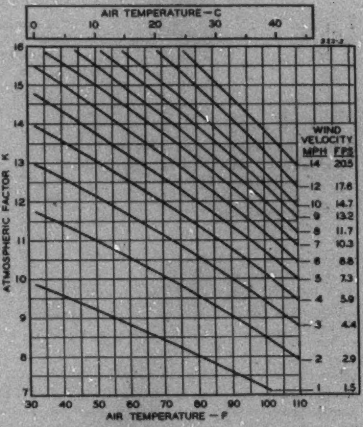

Figure 3 (right). Effect of air temperature and wind velocity upon atmospheric factor *K*

80 degrees centigrade surface—overhead

$K = \sqrt[4]{(t_s - A)^5 v}$

where

t_s = temperature of conductor surface—80 degrees centigrade

A = ambient temperature of air—degrees centigrade

v = wind velocity—feet per second

Thus, it was concluded that the physical characteristics of the copper conductor and its automatic line splices would not be altered appreciably by short-time operations up to 175 degrees centigrade on rare occasions, but that 200 degrees centigrade may cause serious annealing of the copper. The region between 175 degrees centigrade and 200 degrees centigrade appears to be a very critical one. Therefore, 175 degrees centigrade was accepted as the reasonable operating temperature ceiling for overhead copper conductors during the very rare emergencies in the life of a line when there might be a one- or two-hour coincidence of plant failure, maximum load, high ambient temperature, and low wind velocity.

The condition of the cover is a subordinate consideration. Hence the temperature of the cover, if present, should be correlated with the copper-temperature limits. Approximate calculations had indicated that 175 degrees centigrade copper would make about a 130 degrees centigrade maximum outside surface temperature for triple-braid weatherproof covering. This limit is severe but is still well below the 182 degrees centigrade (360 degrees Fahrenheit) minimum impregnating temperature recommended in the Purdue University *Engineering Bulletin* 43, November 1932. A drip test made on six standard samples of number 6 triple-braid weatherproof wire, ovenheated to 82 degrees centigrade for one hour, then to 130 degrees centigrade for two hours, developed bubbles on four samples after the first half-hour at 130 degrees centigrade. Two samples showed no ill effects, and no compound dripped from any of the samples so tested. Time is a factor, hence the over-all effect of temperature and time is expected to be slight during a few nonconsecutive hours in the life of the cover, when there might be a rare coincidence of critical atmospheric conditions and emergency load. With 100 degrees centigrade copper, the outside-cover-surface temperature is estimated to be 80 degrees centigrade, or just below the 82 degrees centigrade standard acceptance specification for triple-braid weatherproof wire. With the design limits of conductor temperature thus approximately determined, it becomes appropriate to review the effects of ampere loading upon temperature.

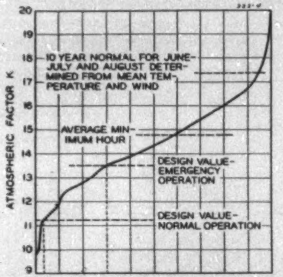

Figure 4. Minimum value of K in any hour for Philadelphia by days in June, July, and August, 1936–39

100 degrees centigrade surface
$K=\sqrt[4]{(t_s-A)^2V}$
where
t_s = temperature of conductor surface—100 degrees centigrade
A = ambient temperature of air—degrees centigrade
V = wind velocity—feet per second

Conductor Heating

The conductor heating effects are three and need little elaboration. At a given ampere loading, the power losses increase as the conductor resistance increases with temperature. Part of the power losses is dissipated by radiation according to the Stefan and Boltzman law. The remainder of the losses in outdoor conductors is dissipated by forced convection, for which an approximate formula has been developed by Schurig and Frick,[5] who have also shown that the effect of sunshine becomes very small in heavily loaded conductors. The natural heat balance is quite simple. The complete algebraic expression for the relation between amperes load and temperature is somewhat unwieldy because of the dissimilarity between the separate effects that must be correlated. By the method detailed in the appendix, however, it will be seen that the principal effects of conductor diameter, conductor-surface temperature rise above ambient, and wind velocity, may be factored out so as to give the following convenient expression for ampere carrying capacity:

$$I=CKd\sqrt[4]{d+2s}$$

where d is the conductor diameter, s is the cover thickness, K is the atmospheric factor, and C is a slow-moving residual coefficient which is essentially a constant for any given set of design limitations.

Table I

		Normal Rating	Emergency Limit
Bare-Stranded Conductors			
Circular mils	500,000	1,090	1,350
	450,000	1,030	1,270
	400,000	950	1,160
	350,000	880	1,070
	300,000	800	970
	250,000	710	850
Number	0000	640	760
	000	550	650
	00	470	560
	0	410	480
	1	360	410
	2	300	350
Bare-Solid Conductors			
Number	0000	600	720
	000	520	620
	00	450	530
	0	390	460
	1	330	390
	2	290	340
Covered Conductors			
Number	0000	570	710
	000	500	620
	00	440	530
	0	380	460
	1	330	400
	2	280	340
	4	220	250
	6	160	180

The principal effect of the conductor diameter and its cover, if present, may be seen in Figure 1, for the illustrative condition ($CK=1{,}500$). The position of the curve for covered wire above that for bare wire, is not particularly significant. In Philadelphia, for instance, the apparent advantage of covered wire in Figure 1, is offset by an 11 per cent lower design limit of CK.

The atmospheric factor ($K=\sqrt[4]{(t_s-A)^2v}$) may be evaluated directly from United States Weather Bureau data by the use of the chart in Figure 2 for bare conductors, or that of Figure 3 for covered ones, as the case may be. The results of such a survey may then be summarized, as in Figures 4 and 5. Maximum ambient temperature is not coincident with minimum wind velocity in the Philadelphia area. The 15-year maximum ambient of 40 degrees centigrade was accompanied by an 11-miles-per-hour wind. Other maximum ambients ranged from 35 degrees centigrade at five miles per hour to 39 degrees centigrade at 13 miles per hour. The most critical coincidence, 23 degrees centigrade at one mile per hour, made the lowest values of K shown in Figures 4 and 5. The condition did not persist for more than one clock hour in any day and occurred on less than six days in the average year.

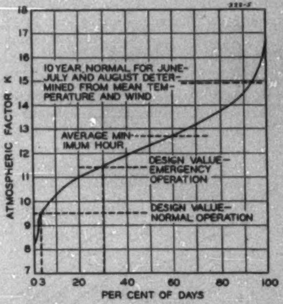

Figure 5. Minimum value of K in any hour for Philadelphia by days in June, July, and August, 1936–39

80 degrees centigrade surface
$K=\sqrt[4]{(t_s-A)^2V}$
where
t_s = temperature of conductor surface—80 degrees centigrade
A = ambient temperature of air—degrees centigrade
V = wind velocity—feet per second

It was considered reasonable therefore to permit copper temperatures to reach values in the neighborhood of 130 degrees centigrade with normal load or 175 degrees centigrade for any emergency load that might on a few rare occasions be coincident with so critical an atmospheric condition. The design values of K that are indicated in Figures 4 and 5 were selected so as to observe 130 degrees centigrade as the approximate ceiling for normal operations and 175 degrees centigrade as the ceiling for emergency operations. A summary of the resulting approximate maximum hour conductor temperatures during the three hottest months of the year is given in Figure 6, which was developed by using arbitrarily the value of C that would give the maximum estimate of copper temperature.

As should be expected, the residual factor C does not vary through nearly the range of values that is characteristic of K. For normal design-limiting conditions in the Philadelphia area, the value is within 4.5 per cent of $C=126$ for bare-stranded, within three per cent of $C=142$ for bare-solid, and within 5 per cent of $C=148$ for covered conductors. For emergency operations in Philadelphia the most critical values occur in the smallest conductor size and are respectively: $C=119$ for bare-stranded, $C=135$ for bare-solid, and $C=144$ for covered wire.

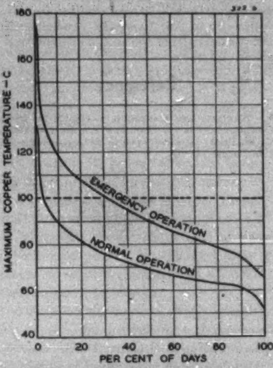

Figure 6. Approximate maximum-hour conductor temperature at ampere load limits in Philadelphia by days in June, July, and August

Ampere Design Limits in Philadelphia

Table I summarizes the ampere design limits for overhead copper lines in Philadelphia, as developed by the method described in this paper.

Winter load limits will be somewhat higher than given in the table. Tentative indications are that normal ratings may be increased ten per cent and emergency limits five per cent from November through March. The limiting atmospheric conditions are not much less severe than in the warmer months. However, about one half of the winter hours with critical ambient-temperature–wind combinations are accompanied by rain.

Conclusions

1. The paper deals with the carrying capacity of conductors in overhead lines, as determined by the effect of temperature and time upon the physical characteristics of the conductor. The paper does not deal with considerations of voltage variation, power loss, nor other factors that may favor larger conductors than are physically adequate for the immediate purpose.

2. The common assumption that maximum ambient temperature and minimum wind velocity are coincident is not supported by a study of United States Weather Bureau records for the past 15 years in Philadelphia.

3. The ampere load limits for overhead conductors in any situation can be evaluated directly from local records of critical ambient temperature and wind by the method given in this paper. The cooling effect of rain is not included, because it is absent during otherwise critical hours in summer and during one half of such hours in winter.

4. Coincidence of load-limit amperes with an hour of critical atmospheric conditions seems to be a contingency so remote that the resulting copper-temperature ceiling need be observed only as required in order to avoid weakening or oversagging the conductor.

5. Conductor temperature ceilings of about 130 degrees centigrade normal and 175 degrees centigrade emergency maximum appear permissible if of short duration and sufficiently infrequent. A 44-hour test has indicated that a few hours at 175 degrees centigrade should not change materially the physical characteristics of medium-hard-drawn copper wire or of automatic line splices, but that 200 degrees centigrade might cause serious annealing of the wire.

6. To recognize for the present at least the need for caution in utilizing temperatures approaching a critical zone for copper wires, the ampere load limits for Philadelphia are designed to keep copper temperatures regularly below 100 degrees centigrade normal or 135 degrees centigrade emergency. During the past 15 years, however, there has been an average of six nonconsecutive hours per year when a coincidence of load-limit amperes with critical atmospheric condition might make the copper temperature in some part of the line approach a ceiling of about 130 degrees centigrade normal or 175 degrees centigrade emergency maximum.

Appendix. General Expression for Current Ratings of Overhead Conductors

The current-carrying capacity of medium-hard-drawn copper conductors in outdoor overhead service was calculated for steady-state conditions. In the steady-state condition, the heat dissipation is equal to the I^2R loss in the conductor, or

$$I^2R=WS \text{ watts} \qquad (1)$$

where S is the surface area of the conductor, W is the sum of the watts radiated W_r and the watts dissipated by convection W_c, so that

$$W=W_r+W_c \text{ watts per square inch} \qquad (2)$$

In surroundings of temperature T_o degrees Kelvin, the heat dissipation by radiation from long horizontal cylinders, with surface of E relative emissivity and surface temperature of T_s degrees Kelvin, is

$$W_r=\frac{36.8E}{10^{12}}(T_s^4-T_o^4) \text{ watts per square inch} \qquad (3)$$

The approximate formula of Schurig and Frick[1] for heat dissipated at sea level by forced convection in air moving v feet per second crosswise to horizontal cylinder with outside diameters $(d+2s)$ ranging from 0.3 to 5.0 inches is

$$W_c=\frac{0.0128}{T_a^{0.123}}\sqrt{\frac{v}{d+2s}}(T_s-T_o) \text{ watts per square inch} \qquad (4)$$

where

$T_a=(T_s+T_o)/2$
d = copper diameter in inches
s = thickness of conductor cover in inches

Substituting equations 3 and 4 in equation 2

$$W=\frac{36.8E}{10^{12}}(T_s^4-T_o^4)+\frac{0.0128}{T_a^{0.123}}\sqrt{\frac{v}{d+2s}}\times(T_s-T_o) \qquad (5)$$

$$=(T_s-T_o)\left[\frac{36.8E}{10^{12}}(T_s+T_o)(T_s^2+T_o^2)+\frac{0.0128}{T_a^{0.123}}\sqrt{\frac{v}{d+2s}}\right] \qquad (6)$$

For convenience let

$$C_r=\frac{36.8E}{10^{12}}(T_s+T_o)(T_s^2+T_o^2)$$

and

$$C_c=\frac{0.0128}{T_a^{0.123}}$$

then

$$W=(T_s-T_o)\left[C_r+C_c\sqrt{\frac{v}{d+2s}}\right] \text{ watts per square inch} \qquad (7)$$

If it is assumed that about 75 per cent of the surface area of bare-stranded conductor is effective in heat dissipation, as is approximately true for radiation, then the effective surface area S of stranded copper is equal to that of a smooth cylindrical envelope and the heat dissipated per foot of conductor is

$$WS=12\pi(d+2s)(T_s-T_o)\times\left[C_r+C_c\sqrt{\frac{v}{d+2s}}\right] \text{ watts} \qquad (8)$$

The approximate average resistance per foot of medium-hard-drawn (97.5 per cent conductivity) copper of diameter d at T_c degrees Kelvin is

$$R=10^{-6}T_c/26.7(\pi d)^2, \text{ or } 13.97 \text{ ohms per circular-mil foot at 100 degrees centigrade} \qquad (9)$$

where r equals 1.00 for solid conductors, or approximately 0.87 for stranded conductor. Thus, from equations 1, 8, and 9

$$I^2 = \frac{WS}{R} = \frac{320\pi(rd)^2(d+2s)\times 10^6}{T_c}(T_s - T_o)\times \left[C_r + C_c\sqrt{\frac{v}{d+2s}}\right] \quad (10)$$

$$I^2 = \frac{320\pi r^2\times 10^6}{T_c}(F_s - T_o)d^2(d+2s)\times \sqrt{\frac{v}{d+2s}}\left[C_r\sqrt{\frac{d+2s}{v}} + C_c\right] \quad (11)$$

$$I^2 = \frac{10^9 r^2}{T_c}\left[C_r\sqrt{\frac{d+2s}{v}} + C_c\right]\times \sqrt{(T_s - T_o)^2)v}\;[d^2\sqrt{d+2s}] \quad (12)$$

or

$$I = CKd\sqrt[4]{d+2s}\text{ amperes} \quad (13)$$

where

$$K = \sqrt[4]{(T_s - T_o)^2 v} = \sqrt[4]{(t_s - A)^2 v}$$

$$C = 31{,}600r\sqrt{\frac{1}{T_c}\left[C_r\sqrt{\frac{d+2s}{v}} + C_c\right]}$$

$$= 31{,}600r\sqrt{\frac{1}{T_c}\left[\frac{36.8E}{10^{12}}(T_s + T_o)(T_s^2 + T_o^2)\times \sqrt{\frac{d+2s}{v} + \frac{0.0128}{T_a^{0.123}}}\right]}$$

A = ambient temperature in degrees centigrade

d = copper diameter in inches

E = relative emissivity of the surface

r = 1.00 for solid, or 0.87 for stranded conductor

s = cover thickness in inches

t_s = outside surface temperature in degrees centigrade

v = wind velocity in feet per second

T_o = ambient temperature in degrees Kelvin

$T_a = (T_o + T_s)/2$

T_c = copper temperature in degrees Kelvin

T_s = outside surface temperature in degrees Kelvin

Upon analyzing the factors in the expression for current, equation 13, it will be seen that:

K may be called an atmospheric factor and evaluated from Weather Bureau observations for any given design temperature t_s of conductor surface, as in Figure 2 for 100 degrees centigrade, or Figure 3 for 80 degrees centigrade.

C is a coefficient which compensates also for the residuals left after accounting for the major effects of d and K. C is practically a constant for all the usual overhead conductor sizes and combinations of high ambient temperatures with critically low wind velocities. For instance, all values of C lie within five per cent of 142 with the numbers 2 to 0000 sizes of oxidized (relative emissivity of 0.6) bare-solid conductors at temperatures from 100 to 175 degrees centigrade.

Reference

1. HEATING AND CARRYING CAPACITY OF BARE CONDUCTORS FOR OUTDOOR SERVICE, O. R. Schurig, C. W. Frick. *General Electric Review*, volume 33, 1930, pages 141–57.

Intrasystem Transmission Losses

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THIS paper describes a new method of calculating transmission losses within power systems. Reasonable accuracy is obtained with longhand calculation, although the same general scheme with the load-flow determinations carried out on a d-c calculating board would provide greater accuracy with less work. The use of an a-c calculating board with separate tabulations of kilowatt and kilovar flows would be even more advantageous. The method is based on the principle of superimposing the load distribution from each source, determining the current in each line as a sum of the individual load flows (with each in its proper direction), squaring this expression for current, and setting up an equation for losses in terms of megawatt generation at the various plants and of megawatt flows (in or out) at each interchange point. An outward flow of power at an interchange point is considered negative generation.

The procedure consists chiefly in deriving a special loss formula for a given power system. The formula is suitable for any condition of generation, load, and interchange. The formula involves only multiplication and addition, and it may, therefore, be used by semiskilled office personnel. Loss determinations for a large number of operating conditions may be made in an hour, thus making practicable the determination of losses for hour-by-hour readings on a dispatching log.

The loss formula may be used for determination of losses, either total or incremental, for billing, forecasting, or system-planning purposes. It has been used by several power companies for hour-by-hour determination of incremental losses. Other power companies have used the formula to derive a typical or average per cent loss to use over a month or longer period of time. The average per cent loss has been derived by calculating the losses for 10 to 50 or more representative hourly operating conditions and selecting an average value. Such determinations are reviewed monthly, or less often if system operating conditions are reasonably uniform from month to month.

Previous Methods

Heretofore it has been necessary to determine losses by detailed calculation of each individual line or by detailed measurement of each individual line on an a-c calculating board (network analyzer).

The determinination of losses by calculating each line separately is slow, even if applied to past operations where the actual load on each line has been measured. It is of little service for studies of future conditions in complicated networks where the load flows cannot be calculated easily. The longhand method of line-by-line calculation is too slow to use for hour-by-hour readings.

The a-c calculating-board method is of great value for system planning or for other purposes where only a few representative operating conditions are of interest. The time and cost of this method prevent its application to hour-by-hour readings.

Procedure for Determining Loss Formula

1. Secure estimated megawatt (three-phase) loads on primary substations for a heavy load hour (preferable annual maximum hour, when system losses are presumably the highest). Neglect transmission losses for the time being. On most systems the load distribution on a percentage basis changes very little from peak to off-peak, and the effect of power-factor variation is unimportant (except at off-peak on systems with high charging current). Loads at small stations may be prorated at terminal stations in inverse ratio to line impedance, without much effect on accuracy of the loss calculations.

2. Secure an impedance diagram of the high-voltage transmission system. Intermediate-voltage lines may be omitted without much effect on accuracy of the loss calculations.

3. Total the loads in item 1 and prorate in per cent (of 100 megawatts).

4. Determine Z for the $R+JX$ line values in item 2. (Disregarding power factor, the flow of current will divide inversely as Z in each circuit.) Convert Z to ohms at the major transmission voltage of the system. Draw a one-line diagram of the transmission system with the proper Z value (phase-to-neutral) marked on each line. (Use only the scalar value of Z, without regard to angle.)

5. Determine the location of generating sources and points of interconnection, existing or proposed, for the system. Very small plants may be netted with local loads before tabulating these loads in item 1. Any heavy load concentrated at one point and not following closely the system daily load curve may be considered as an interconnection point and handled as a power source (negative).

6. Draw up a hectograph or stencil one-line diagram with the lines in item 2 (omitting impedance values), with the substation loads in item 3, and with the sources in item 5. Consider each interconnection as a generating source (positive or negative). Since these one-line diagrams are to be used as work sheets in making up flow diagrams on a cut-and-try basis, 25 or more copies may be needed (unless a calculating board is available for the operation in item 7).

7. Make up a synthetic megawatt load-flow diagram for each separate source on a separate sheet, using the forms prepared in item 6. Consider each interconnection as feeding into the system like a generator, regardless of the normal flow of power at the interconnection point. Observe the direction of flow carefully and consistently as between diagrams, so that the algebraic sum of the megawatt flows in any line will be the correct one. Unless a d-c or a-c calculating board is available, the synthetic load-flow diagrams will have to be developed by a cut-and-try method, making the total of the products of impedance times proper flow around any loop equal to zero. Omit consideration of losses in making these flow diagrams, unless a calculating board is used. In any case the total output from any source should be 100 megawatts at the source. Omit consideration of load and generator power factor for the time being, unless an a-c calculating board is used, on the assumption that the proper load flow is secured when the sum of the *zmw* products around every loop in the system is equal to zero or approximately so. (Neglect fractions of megawatts.)

8. Set up a tabulation listing in the first column each line by the names of the two terminals, followed in the second column by the resistance of the line *in ohms at the major transmission voltage.*

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The author acknowledges the assistance of several operating engineers in working out the superposition method of loss determination and in applying it successfully to a variety of systems and conditions. Special mention is made of H. H. Deloney, Louisiana Power and Light Company; C. H. Frier, Oklahoma Gas and Electric Company; W. J. Googe, Southwestern Gas and Electric Company; R. B. Gow, Kansas Gas and Electric Company, and E. C. Stewart, Arkansas Power and Light Company.

Table I

Sources	Square Terms	Product Terms	Total Terms
1	1	0	1
2	2	1	3
3	3	3	6
4	4	6	10
5	5	10	15
6	6	15	21
7	7	21	28
8	8	28	36
9	9	36	45
10	10	45	55

Power flowing from the first named terminal to the second should be considered as positive. It is convenient to choose the order of terminals so that most of the flows in each line will be positive. Horizontally opposite each line in separate columns set down the megawatt flows from item 7 for each source. In this discussion the various megawatt generator and interconnection loadings will be designated as G_A, G_B, G_C, and so forth.

9. Determine KV and *PF*, defined as follows: Let KV be the average phase-to-phase voltage (in kilovolts) for the heavily loaded portions of the transmission system (usually near generating plants). Let *PF* be the average decimal power factor in the heavily loaded portions of the transmission system.

10. Calculate *H*, which is a common constant factor to be applied to the square and product-flow totals to get three-phase kilowatt loss (see Appendix *A* for derivation).

$$H=\frac{(0.1)}{(PF\times kv)^2}$$

11. Set up a tabulation of the square terms listing the lines as in item 8 and horizontally opposite the product of resistance times the square of power flow in megawatts (with each source in a separate vertical column).

12. Set up a tabulation of product terms listing the lines as in item 8 and horizontally opposite the product of resistance times power flow from one plant times the flow from another plant. List separately each pair of sources, until each source has been used once with every other source.

13. Total the columns in item 11 vertically, and multiply each of the totals by *H*, thus forming the square coefficients in the loss equation. These coefficients will always be positive. In this discussion, these coefficients will be designated as K_{AA}, K_{BB}, K_{CC}, and so forth. Each coefficient when multiplied by the square of the symbol denoting megawatt generation at its respective plant becomes a term in the final complete loss equation (see Appendix *A* for derivation).

14. Total the columns in item 12 vertically, and multiply each of the totals by 2 *H*, thus forming the product coefficients in the loss equation. These coefficients may be either positive or negative. In this discussion these coefficients will be designated as K_{AB}, K_{AC}, K_{BC}, and so forth. Each coefficient when multiplied by the two symbols denoting megawatt generation at its respective two plants becomes a term in the final complete loss equation.

15. Set up the results of items 13 and 14 as follows:

Loss (in three-phase kilowatts) = $G_A^2K_{AA}+G_B^2K_{BB}+G_C^2K_{CC}+$ and so forth $+G_AG_BK_{AB}+G_AG_CK_{AC}+G_BG_CK_{BC}+$ and so forth

Use of Loss Formula

16. The loss equation in item 15 consists of *K* values determined once for all and of plant and interconnection loadings designated as G_A, G_B, and so forth, selected for each case for which losses are desired. Use loadings in megawatts. The total loss will be in kilowatts. The algebraic sum of the loadings will equal the net system input. Net system input equals territorial load plus losses. Generation and tie-line receipts are considered positive. Tie-line deliveries are considered negative. The loss equation is now ready for use, unless it is desired to set up the results graphically, or to determine minimum loss conditions, or to derive a shorter approximate equation with only the major sources. After the coefficients

Table II. Conversion of Load Flows Into Loss Coefficients

Line Terminals	Resistance in Ohms at 60 Kv	Burton B	Washington W	Madison M	Nelson N	Anderson A	B^2 × Resistance	W^2 × Resistance	M^2 × Resistance	N^2 × Resistance	A^2 × Resistance
							+	+	+	+	+
A-B	18.1	13	12	−3	−3	6	3,060	2,610	160	160	650
B-C	38.2	6	5	−10	−10	−1	1,380	960	3,820	3,820	40
A-C	9.0	28	25	−26	−26	5	7,050	5,630	6,080	6,080	220
C-D	3.8	30	30	30	−70	30	3,420	3,420	3,420	18,620	3,420
C-E	6.0	−5	−9	25	25	−35	150	490	3,750	3,750	7,350
E-F	8.6	−9	−12	12	12	8	700	1,240	1,240	1,240	550
F-G	12.0	−15	−18	6	6	2	2,700	3,880	430	430	50
E-H	22.7	−2	−3	7	7	−49	90	200	1,110	1,110	54,500
H-G	30.8	−8	−9	1	1	45	1,970	2,500	30	30	62,370
G-A 60 kv	1.9	−21	30	−13	−13	25	840	1,710	320	320	1,180
G-A 12 kv	17.1	−25	20	−3	−3	−1	10,670	6,840	150	150	20
Total							32,030	29,480	20,510	35,710	130,350
Total ÷ 10^4							3.203	2.948	2.051	3.571	13.035
Above ×0.343							+1.10	+1.01	+0.704	+1.22	+4.47

		B×W × Resistance	B×M × Resistance	B×N × Resistance	B×A × Resistance	W×M × Resistance	W×N × Resistance	W×A × Resistance	M×N × Resistance	M×A × Resistance	N×A × Resistance
A-B	18.1	2,820	−710	−710	−1,410	−650	−650	1,300	160	−330	−330
B-C	38.2	1,150	−2,290	−2,290	−230	−1,910	−1,910	−190	3,820	380	40
A-C	9.0	6,300	−6,540	−6,540	1,260	−5,850	−5,850	1,120	6,080	−1,170	−1,170
C-D	3.8	3,420	3,420	−7,980	3,420	3,420	−7,980	3,420	−7,980	3,420	−7,980
C-E	6.0	270	−750	−750	1,050	−1,350	−1,350	1,890	3,750	−5,250	−5,250
E-F	8.6	930	−930	−930	−620	−1,240	−1,240	−820	1,240	820	820
F-G	12.0	3,240	−1,080	−1,080	−360	−1,300	−1,300	−430	430	140	140
E-H	22.7	140	−320	−320	2,220	−480	−480	3,340	1,110	−7,790	−7,790
H-G	30.8	2,220	−250	−250	−11,030	−280	−280	−12,470	30	1,390	1,390
G-A 60 kv	1.9	−1,200	520	520	−1,000	−740	−740	1,420	320	−620	−620
G-A 12 kv	17.1	−8,560	1,280	1,280	430	−1,030	−1,030	−340	150	50	50
Total		10,730	−7,650	−19,050	−3,450	−11,410	−22,810	−1,760	9,110	−8,960	−20,700
Total ÷ 10^4		1,073	−7,765	−1.905	−0.345	−1.141	−2.281	−0.176	0.911	−0.896	−2.07
Above×0.686		+0.734	−0.524	−1.31	−0.236	−0.783	−1.56	−0.121	+0.625	−0.615	−1.42

are determined, the loss equation can be solved for any given loading in a few minutes, even if five or six sources are involved. Certain simplifications are possible in many cases, and the number of sources may be reduced without too great an effect on accuracy of the loss calculations. However, it is preferable to start with a comprehensive and accurate loss equation, so that the effect and validity of approximations may be determined.

Methods of Simplifying Loss Formula

17. In most cases two or more sources (plants and/or tie lines) in a small area may be totalized as one. One scheme of pooling is to determine from the complete loss equation new coefficients for the pooled sources which will give the same total loss as the individual coefficients for the distribution at the annual maximum hour. Errors at smaller system loads will be of little importance.

18. If any plant is operated at a flat base load all the time (or whenever losses need to be calculated), a numerical value of megawatts can be substituted for the respective plant loading symbol, thus eliminating one variable and simplifying the loss formula for most purposes. Sometimes a tie line may be handled similarly.

19. If any plant is either shut down or operated at full load, the above substitution and one for zero load can be made, resulting in two loss equations with one less variable. One or the other may be used, depending on whether the plant in question is on or off. Sometimes a tie line may be handled similarly.

20. The most effective and generally useful simplification of a loss formula is to make use of the fact that all power systems have a planned order of loading plants, except for infrequent emergency situations. Sometimes this order changes seasonally or with hydro or fuel conditions. Even if three different loading schedules are used during the year, three loss formulas with variables for only the regulated plants are preferable to one formula with a variable for each plant.

21. If losses must be determined frequently, tables are much superior to charts.

22. Even though the loss equation is used only to derive loss tables or monthly average loss percentages, any type of loss chart conveys so much information that it seems desirable to construct a loss chart, even if it is based on an approximate loss equation obtained by reducing the system to two or three major source areas.

23. Total loss varies as the square of the total system load for any given fixed ratio of loading between plants. Therefore, it is possible to take loss values from general charts plotted in percentage or decimal distribution and to secure total loss by multiplying the chart value by the square of the system load. The most convenient form of chart is one based on 100 megawatts total load, with the plant and tie loadings in megawatts (or per cent). The total system loss is then equal to the chart value multiplied by the square of the system load in hundreds of megawatts.

24. In those few cases where there are only two sources, or where the system can be approximated by only two major source areas, a rectangular co-ordinate chart of system loss can be drawn up. The co-ordinates may be "total net system input" and "source *A*"; or, "source *A*" and "source *B*." The loci of constant loss will be concentric ellipses in either case.

25. In systems involving three source areas with one source always base loaded, the loss equation contains only two variables and the above type of rectangular chart can be used. This chart has the further advantage that it is direct reading (instead of being on a 100-megawatt basis, as described in item 27).

26. In all cases where the loading of three plants or ties is of major interest, the use of triangular co-ordinate paper is the most feasible means of plotting loss curves.

27. In using triangular co-ordinate paper, the three plant loadings, in per cent (totaling 100) or in megawatts for 100 megawatts system load, are measured perpendicularly to each side of an equilateral triangle. (*K* and *E* triangular co-ordinate paper 359-32 or 344-*A* is convenient for this purpose.) The sum of any three such altitudes always equals the height of the triangle or 100 per cent. The same relation holds for points outside the basic triangle, if altitudes wholly outside the triangle are considered negative. This permits the plotting of deliveries to tie lines as negative generation.

28. For four or more sources with variable loading, no means of graphic representation seems to be possible. Nomographic charts cannot be used with this type of equations.

Procedure for Determining Loading for Minimum Loss

29. While plant capabilities and relative power costs usually prevent allocation of plant and tie loading on a minimum transmission-loss basis, the determination of this optimum condition is frequently of value. Inspection of minimum loss loadings conveys a great deal of information on the adequacy of transmission planning in the past and offers a needed guide to adequate transmission planning in the future. It provides a means by which the load dispatcher can compare the delivered costs of power from sources of equal or nearly equal cost (at the source). It provides a starting point for empirical charts or formulas of transmission losses in those cases where the general loss formula after all possible rational simplification still contains too many terms for ready use.

30. The general equation for loss cannot be solved for ***minimum*** loss loadings until use is made of the fact that the sum of the loadings is equal to unity, if each is on a percentage basis. (Otherwise the partial-derivative equations are homogeneous, giving a determinant equal to zero.)

31. Take the partial derivative of equations in item 15 with respect to each one of the sources in that equation, giving as many partial-derivative equations as there are sources in item 15. Equate each derivative equation to $2x$.

32. *Assumptions as to system operation must be inserted into the equations in item 15 before taking partial derivatives in item 31.*

33. Rearrange the terms in the equations in item 32 with like sources in the same columns and with the columns and rows in the same order. Place the $2x$ terms on the right-hand side of the equality sign.

34. Subtract from the first partial-derivative equation in item 34 each of the other partial-derivative equations in turn. This will reduce the right-hand side of each new equation to zero, by eliminating the $2x$ term.

35. Divide each of the new equations in item 35 by the unknown in the first term. This makes the first term numerical, changes the unknowns to ratios, and reduces the number of unknowns to equality with the number of equations. The equations can now be solved simultaneously for the unknown ratios (see pages 35–7 of "Determinants" by Weld).

36. Solve the simultaneous equations in item 37, preferably by means of determinants. The results will be the loading ratios for minimum loss with the loading of each source variable.

37. If the simultaneous equations in item 36 are more than three in number, and if they are solved by addition and subtraction instead of by determinants,

care should be taken to eliminate between the first equation and each of the others in systematic order, so that each of the derived equations will be independent of the others.

38. Total the ratios in item 36, add unity for the ratio of the source in the denominator of each unknown, and prorate so that the new total of all source loadings equals unity. These decimal fractions totaling unity constitute the simultaneous loadings for minimum total loss, with the loading of each source variable.

39. While the preceding use of partial first derivatives is in general a necessary but not a sufficient condition of a minimum, it can be shown that the loss loci are concentric ellipses in any component plane and have a minimum at the center. Therefore, there can be no ambiguity about the point of minimum loss, and there is no need of taking second derivatives to determine sufficiency (see pages 484–6 of "Calculus" by Granville, Smith, and Longley).

Procedure for Constructing Rectangular Loss Charts

40. If only one source loading is variable, the loss equation can be represented by a parabola.

41. If only two variable sources are involved, a rectangular type of loss chart may be constructed from the loss equation derived in item 15. The general form of such an equation is as follows:

$$\text{Kw loss} = K_{AA}A^2 + K_{BB}B^2 + K_{AB}AB$$

42. If the equation in two variables has been derived by base loading one or more sources, there will be three additional terms as shown below:

$$\text{Kw loss} = L_{AA}A^2 + L_{BB}B^2 + L_{AB}AB + L_AA + L_BB + L_C$$

43. Either form of equation 41 or 42 may be represented graphically by an ellipse. (See page 66 of "Handbook of Engineering Fundamentals" by Eshbach. This reference shows that the loss equation in item 15 is a conic section in the plane of any two variables and that this section meets the test for an ellipse.)

44. The construction of ellipses representing loci of constant loss requires a systematic procedure if speed and accuracy are to be realized.

Table III. Loss Formulas

I. Transmission Losses—Using Two Source Areas—Overton–Madison Line and Anderson–Wilson Line Not in Service

S = megawatts generated at Washington plus Burton
N = megawatts generated at Nelson plus megawatts supplied by Riverton line at Nelson (negative if feeding out)
Kilowatt loss = $+0.71S^2+1.22N^2-1.43SN$

II. Transmission losses—Using Three Source Areas—Overton–Madison Line Not in Service

S = megawatts generated at Washington plus Burton
N = megawatts generated at Nelson plus megawatts supplied by Riverton line at Nelson (negative if feeding out)
M = megawatts supplied by Overton line at Madison (negative if feeding out)
Kilowatt loss = $+0.71S^2+1.22N^2+0.70M^2-1.43SN-0.65SM+0.62NM$

III. Transmission Losses—Using Four Source Areas

S = megawatts generated at Washington plus Burton
N = megawatts generated at Nelson plus megawatts supplied by Riverton line at Nelson (negative if feeding out)
M = megawatts supplied by Overton line at Madison (negative if feeding out)
A = megawatts supplied by Wilson line at Anderson (negative if feeding out)
Kilowatt loss = $+0.71S^2+1.22N^2+0.70M^2+4.47A^2-1.43SN-0.65SM-0.185A+0.62NM-1.42NA-0.62MA$

IV. Transmission Losses—Using Five Source Areas

B = megawatts generated at Burton
W = megawatts generated at Washington
N = megawatts generated at Nelson plus megawatts supplied by Riverton line at Nelson (negative if feeding out)
M = megawatts supplied by Overton line at Madison (negative if feeding out)
A = megawatts supplied by Wilson line at Anderson (negative if feeding out)
Kilowatt loss = $+1.10B^2+1.01W^2+1.22N^2+0.70M^2+4.47A^2+0.73BW-1.31BN-0.52BM-0.24BA-1.56WN-0.78WM-0.12WA+0.62NM-1.42NA-0.62MA$

45. The major and minor axes of the ellipses are seldom parallel to the co-ordinate axes. The angle of one axis of the ellipses with respect to the B co-ordinate may be determined by the following expression (but the sign of the angle and determination of whether major or minor axis should be checked by calculation of a few points from the loss equation):

$$\tan = 0.5\tan^{-1}[L_{AB} \div (L_{BB} - L_{AA})]$$

L should be replaced by K if the first form of equation is used.

46. Another important determination is the location of the center of the loss ellipses. This center is also the point of minimum loss, unless the center is at the origin of co-ordinates—in which case the minimum loss must be determined by other means. The co-ordinates of the center are as follows:

$$A_R = \frac{2L_AL_{BB} - L_BL_{AB}}{4L_{AA}L_{BB} - L_{AB}^2}$$

$$B_R = \frac{2L_BL_{AA} - L_AL_{AB}}{4L_{AA}L_{BB} - L_{AB}^2}$$

47. In the first form of equation (consisting of K terms instead of L terms, the loss ellipses are concentric about the origin of co-ordinates, and A_R and B_R are each equal to zero.

48. The next items to be determined are the major and minor radii. There will be a pair of these for every value of loss locus to be plotted. To determine these radii, make the following substitutions:

$$F = L_A^2L_{BB} + L_B^2L_{AA} - L_AL_BL_{AB}$$
$$G = 4L_{AA}L_{BB} - L_{AB}^2$$

49. Minimum loss occurs when:

$$\text{Kw loss} = L_C - (F \div G)$$

In item 31, x equals minimum loss (then unknown).

50. Continue the substitutions begun in item 48:

$$P = L_{AA} + L_{BB} - \sqrt{(L_{AA} - L_{BB})^2 + L_{AB}^2}$$

$$Q = L_{AA} + L_{BB} + \sqrt{(L_{AA} - L_{BB})^2 + L_{AB}^2}$$

$$R_L^2 = (\text{major radius})^2 = \frac{(F \div G) + (\text{kw loss}) - L_C}{0.5P}$$

$$R_s^2 = (\text{minor radius})^2 = \frac{(F \div G) + (\text{kw loss}) - L_C}{0.5Q}$$

51. An ellipse can be constructed most easily and with adequate accuracy by using three radii (see pages 2–51 of "Handbook of Engineering Fundamentals" by Eshbach). These radii have been determined analytically by substitution of the standard formula for radius of curvature of an ellipse (see page 152 of "Calculus" by Granville, Smith, and Longley).

52. For each locus of constant loss, determine the following (in addition to R_l and R_S):

At the ends of the major axis (center on the major axis)

$$R_C = R_s^2 \div R_L$$

At the ends of the minor axis: (center on the minor axis)

$$R_C = R_L^2 \div R_s$$

At some intermediate arc (length and center to be determined by trial)

$$R_C = \sqrt{R_LR_s}$$

See Figure 1 for a typical loss diagram on rectangular co-ordinates. The ellipses are loci of constant loss, with all sources except two base-loaded. Instead of using the two sources as co-ordinates, the total territorial load of the system is used as one co-ordinate, and one source as the other.

53. Minimum-loss co-ordinates as obtained in item 46 should be checked by the results of item 36.

54. Minimum loss in kilowatts as obtained in item 49 should be checked by substituting the co-ordinates in item 36 in the loss equations of item 15.

55. Loss values at several diverse points on a chart constructed as per items

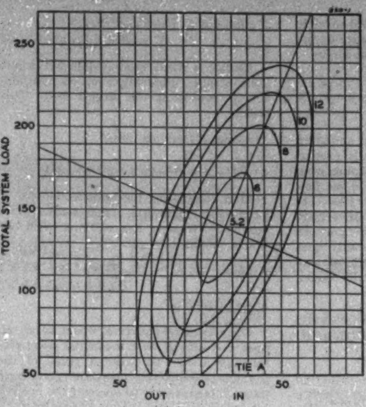

Figure 1. Typical loss chart

System load, tie-line load, and loss figures are in megawatts. Minimum loss is 5.2, at center of ellipses

41 to 52 should be checked by comparing values computed from the loss equation of item 15.

Application of New Method to A-C Calculating-Board Studies

56. The superposition method has been used to date for operating purposes, where the use of the a-c calculating board (except for determining load division) would be impractical. There has been no opportunity as yet to apply the superposition method in a-c board studies. If kilowatt and kilovar load flows were read and totalled separately, the loss equation would take account of generator excitation, charging current, synchronous condensers, different load power factors, different R/X ratios, and so forth. True vector relations would be maintained, and it would be possible to determine definitely the accuracy and limitations of the longhand scalar method. This proposed combination of a-c calculating-board analysis with the superposition method is largely an unexplored field.

Time Requirements of New Method

57. Power systems differ widely in the time required to calculate loss formulas, as well as in the time required to use them. On a system involving five sources (plants and tie lines) but with only a few loops, a loss formula was calculated completely and checked by two men in two days. Other systems with eight to ten sources and complicated networks have required two or three weeks work by two men to calculate the loss formula. If a d-c or a-c calculating board is available for load division, the balance of the work in determining the loss formula can easily be done in N^3 man-hours where N is the number of sources.

58. The use of the loss formula for a given set of operating conditions may require one minute or 15 minutes, depending on the number of variable sources. A large number of operating conditions can be calculated very quickly, if the data are set up in tabular form and carried out systematically.

59. In general, a loss formula can be worked out in less time than data could be calculated and tabulated for an a-c board study. Losses for a number of conditions can then be calculated in a small fraction of the time required to read losses on the calculating board, not to mention the time to change board setups.

Accuracy of New Method

60. The accuracy of loss calculations by the superposition method outlined herein is estimated to be better than ten per cent plus or minus on single readings and better than five per cent plus or minus on daily totals—even under difficult conditions. Since losses are only 2 per cent to 15 per cent of power transfer, errors in determining transmission losses are comparable with errors customarily tolerated in metering input and output to the transmission system.

61. The use of the scalar value of Z in determining load division without regard to the R/X angle has a negligible effect on accuracy, as may be seen by calculating a few typical cases (determine 1 in loop branches with Z constant and R/X variable).

62. Loss data on typical transmission systems show that incremental losses cannot be approximated satisfactorily by taking total losses as proportional to the square of the system load. Prior to the development of the superposition method, this was attempted, using a peak loss determined from the annual load factor and the annual kilowatt hour loss (as determined from monthly generation, tie-line, and primary-substation meter readings), following the procedure described on page 1159 of the seventh edition of the "Standard Handbook of Electrical Engineers." This square law relation for total loss applies only to systems with one power source or with several sources all loaded uniformly (on the same percentage basis).

63. On the usual transmission system, the curve of total losses against total system load may change direction abruptly whenever load is placed on a new plant. Each change in regulating plant means a change in the shape of the loss curve. The total loss curve usually consists of segments of parabolas (one regulating source) or ellipses (more than one regulating source) with a discontinuity at each junction.

64. Incremental intrasystem transmission loss may be either positive or negative.

65. The possible sources of error in this method are:

A. The product of voltage and power factor may not equal the assumed average value. However, most of the line loss occurs near the sources, and the average voltage and

Table IV. Summary of Minimum-Loss Determinations

I. Minimum-Loss Loadings (in Per Cent of Net System Input)

	Burton	Washington	Nelson	Madison	Anderson
5 sources	17	28	30	17	8
4 sources	19	31	30	20	*
4 sources	20	30	41	*	9
5 sources	23	34	43	*	*

II. Megawatts Loss for 100 Megawatts Net System Input With Above Minimum-Loss Distribution**

	Megawatt Loss
5 sources	0.38
4 sources	0.67
4 sources	0.61
3 sources	1.00

* Supply at this source equals zero.

** Total loss varies as the square of the net system input.

line power-factor conditions should be determinable with fair accuracy.

B. The product of voltage and power-factor changes with substation loads, interconnection transfers, generator kilowatt loading, and generator reactive loading. On systems with large charging current, the uncorrected calculated losses will be too high at times of light generation and low interchange. An estimated correction is desirable in such cases.

C. The principle of superposition applies to a-c load flows only if the line power factor is uniform and constant. Short-circuit determination on the d-c calculating board and other data indicate that the error due to arithmetic addition of currents from various sources is not important on systems with reasonably uniform R/X ratios on major tie lines and with reasonably uniform power factors on major plants.

D. Losses are neglected in determining the synthetic load-flow diagrams. Analysis indicates that losses do not greatly change the load division near power sources—where the losses are important. Furthermore, the calculated currents near the sources would not be reduced much if line losses were subtracted.

E. The percentage distribution of kilowatt load between substations changes hourly and seasonally, but the net effect is ordinarily small, as may be seen by comparing peak and off-peak board studies of typical systems.

F. The prorating of small substation loads at terminal substations causes the calculated losses to be too small, but this error can be reduced by using a more complete transmission diagram.

G. Line loss is calculated on the basis of uniform current throughout.

66. Errors listed under A, C, D, and G are eliminated if an a-c calculating board is used to determine the load flows from each source (provided kilowatt and kilovar flows are tabulated separately). The use of an a-c calculating board will facilitate determination of loss equations for two or more conditions, such as peak and off-peak, reducing the errors listed under B and E.

67. Even when using longhand calculation, losses under the most important operating conditions can be determined with reasonable accuracy, using a single loss equation (with three to six variables, depending upon the number of major source areas).

Appendix A. Derivation of Loss Formula

Each megawatt flow in a given line in item 8 for a given source multiplied by the ratio of actual generation at that source to 100 megawatts would give the actual megawatts in the line. The total megawatt flow in each line is as follows designating the various megawatt generator and interconnection loadings as G_A, G_B, G_C, and so forth, and the flows—on a 100-megawatt or per cent basis—as F_A, F_B, F_C, and so forth:

$$\text{Total flow in megawatts} = (G_AF_A + G_BF_B + G_CF_C + \text{and so forth}) \text{ divided by } 100 \text{ per cent}$$

At a given transmission voltage and given power factor, amperes are linearly proportional to megawatts, and a common constant factor H may be applied to the flow totals to get three-phase kilowatt loss. Let kv be the average phase-to-phase voltage (in kilovolts) for the heavily loaded portions of the transmission system (usually near generating plants). Let PF be the average decimal power factor in the heavily loaded portions of the transmission system.

$$\text{Three-phase transmission loss in kilowatts in any line} = (3)(I^2R)(0.001) = (0.003)(I^2R)$$

$$\text{Loss in any line} = \frac{(0.003)(\text{kva})^2(R)}{(1.73)^2(\text{kv})^2}$$

$$= \frac{(0.001)(\text{kw})^2(R)}{(PF\times\text{kv})^2} = \frac{(1{,}000)(\text{mw})^2(R)}{(PF\times\text{kv})^2}$$

Loss in any line

$$= \frac{(1{,}000)(G_AF_A + G_BF_B + \text{ and so forth})^2(R)}{(PF\times\text{kv})^2(100\text{ per cent})^2}$$

$$= \frac{(0.1)(G_AF_A + G_BF_B + \text{ and so forth})^2(R)}{(PF\times\text{kv})^2}$$

$$H = \frac{(0.1)}{(PF\times\text{kv})^2}$$

$$\text{Loss in any line} = H(G_AF_A + G_BF_B + \text{and so forth})^2(R)$$

Intrasystem loss in kilowatts

$$= H\Sigma[(G_AF_A + G_BF_B + \text{ and so forth})^2(R)]$$

$$= H\Sigma[(G_A^2F_A^2 + G_B^2F_B^2 + 2G_AF_AG_BF_B + \text{and so forth})(R)]$$

$$= HG_A^2\Sigma(F_A^2R) + HG_B^2\Sigma(F_B^2R) + \text{ and so forth} + 2HG_AG_B\Sigma(F_AF_BR) + \text{ and so forth}$$

$$K_{AA} = H\Sigma(F_A^2R)$$

$$K_{AB} = 2H\Sigma(F_AF_BR)$$

Making the above types of substitutions gives the general loss formula in item 15.

The square terms will always be positive, regardless of the direction of flow in the line, and regardless of whether the plant or interconnection is delivering or receiving power. The product terms will be either positive or negative, depending upon whether the basic flows from the two sources are in the same direction or not, and upon whether power is being delivered or received at each of the two sources.

If N is the number of sources, the number of square terms will be N. The number of product terms will be $(N^2-N)\div 2$ (see Table I).

Appendix B. Procedure for Constructing Triangular Loss Charts

If only three variable sources are involved, a triangular co-ordinate type of loss chart may be constructed from the loss equation derived in item 15.

$$\text{Kw loss} = K_{AA}A^2 + K_{BB}B^2K_{CC}C^2 + K_{AB}AB + K_{AC}AC + K_{BC}BC$$

Assume a fixed total-system net input. This permits elimination of one variable source (preferably the largest one). Substitute:

J = total-system net input

$C = J - A - B$

$C^2 = J^2 + A^2 + B^2 - 2JA - 2JB + 2AB$

After completing the substitution for J, let $J = 100$ megawatts. This results in an alternative form of loss equation with one source eliminated.

$$\text{Kw loss} = M_{AA}A^2 + M_{BB}B^2 + M_AA + M_BB + M_{AB}AB + M_C$$

$$M_C = 10{,}000\,K_{CC}$$

The preceding equation would be an ellipse if A and B were measured *along* rectangular axes. To make a graph of the preceding equation on triangular co-ordinate paper, it is necessary to measure the preceding values of A and B *from* triangular axes. The shape of the resulting figure is to be determined from a new equation derived by transforming co-ordinates. The usual geometrical methods of constructing curved figures representing second-degree equations assume the use of rectangular co-ordinates. Denoting by subscripts whether triangular (T) or rectangular (R) co-ordinates are used:

$$M_{AA}A_T^2 + M_{BB}B_T^2 + M_AA_T + M_BB_T + M_{AB}A_TB_T + M_C - \text{kw loss} = 0$$

Substitute

$A_T = 0.866B_R - 0.500A_R$

$B_T = A_R$

This substitution results in another ellipse, referred to rectangular co-ordinates, and therefore easy to construct when the major and minor radii, location of center, and slope of major axis are known. The last two items are the same for all values of assumed loss.

$$N_{AA}A_R^2 + N_{BB}B_R^2 + N_AA_R + N_BB_R + N_{AB}A_RB_R + N_C - \text{kw loss} = 0$$

From this point on, the construction of elliptical loci on triangular co-ordinates is the same as outlined for the L type of equation in item 41, except that the N values are used. Minimum-loss co-ordinates on triangular co-ordinate paper will be at the center of the loss ellipses. These co-ordinates may be checked as follows:

$$\frac{\partial\text{ loss}}{\partial A_T} = 2M_{AA}A_T + M_{AB}B_T + M_A = 0$$

$$\frac{\partial\text{ loss}}{\partial B_T} = M_{AB}A_T + 2M_{BB}B_T + M_B = 0$$

Solution of the above equations will give the minimum-loss co-ordinates and check the center of the loss ellipses on the triangular-co-ordinate diagram.

The slope of the ellipse axes is best referred to the B_R axis by using the N equation.

In general, quantities derived by the conventional formulas of analytic geometry require the use of the N equation and rectangular co-ordinates, while quantities plotted as triangular co-ordinates require the use of the M equation.

Metering Power on the Low-Voltage Side

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POWER consumers often are supplied from high-voltage distribution or subtransmission circuits through transformer banks. When such consumers are to be charged on the basis of high-voltage or primary supply, some way of including the transformer losses is desirable without resorting to the difficulties and expense of direct primary-circuit metering.

Where metering is applied on the low-voltage side of the power transformers, errors introduced in the high-voltage input values are due to the losses in the transformers, and are of two kinds:

1. Core losses: These are very closely proportional to the square of the voltage and independent of load. The core losses consist of hysteresis and eddy-current losses in the iron. These losses are also affected by frequency, but the variations in frequency on modern systems are so small that the effect is negligible. Changes in temperature likewise have too small an effect to be considered.

2. Copper losses: These are proportional to the square of the current. Copper losses may be divided into two parts; namely, the ordinary I^2R losses, such as would be caused by resistance R, and the eddy-current losses. The temperature coefficient of the former is positive, while that of the latter is negative. Since the I^2R loss is the larger, the net effect is that losses increase with temperature. A temperature rise of 50 degrees centigrade increases the losses by about ten per cent, whereas the resistance of copper changes by roughly 20 per cent over this range. Actually, the effect of temperature changes can be disregarded in the present discussion, since it is a small percentage of the power being measured. Some of the methods to be given can be made to compensate easily for temperature changes, but this is more of a theoretical advantage than a practical one.

Transformers of the size used in serving consumers from medium-voltage lines may be about 99 per cent efficient at full load, with roughly equal division of the losses, or one-half per cent core loss and one-half per cent copper loss. In some cases, however, the copper loss at full load may be almost twice as high as the core loss.

There are two different metering arrangements in common use:

1. Current transformers on the high side and potential transformers on the low side.
2. All instrument transformers on the low side.

Arrangement 1 will measure the core loss but not the copper loss. Arrangement 2 does not include any transformer losses.

Various[1] methods may be used to include the effect of the transformer losses. Some are:

1. Estimation of losses from the meter readings.
2. Loss or compensating meters may be added to measure voltage squared and current squared, which, by proper calibration, will read core loss and copper loss, respectively.
3. Line-drop compensators may be used to correct for transformer resistance drop and so include copper loss.
4. Dummy loads may be added to instrument transformer secondary windings to correct for core loss.
5. Methods 3 and 4 may be combined, using compensators for copper loss and dummy loads for core loss.
6. Light-load and full-load meter adjustments may be altered to shape the load curve to the reciprocal of the transformer efficiency curve.
7. Methods 3 and 6 may be combined, by using compensators for copper loss and light-load adjustment for core loss.

These methods will be taken up in order for a typical case. The following assumptions are made:

1. The installation is three 200-kva single-phase transformers fed from 13,200 volts.
2. Core loss is 1.1 kw per transformer at rated voltage.
3. Copper loss is 1.9 kw per transformer at rated load.
4. Consumption is 150,000 kilowatt-hours per month. The month is assumed to consist of 730 hours.

From the above information, the core loss at rated voltage is seen to be:

$3 \times 1.1 \times 730 = 2{,}409$ kilowatt-hours

If the average voltage differs from rated voltage, the core loss will be approximately 2,409 × (average voltage ÷ rated voltage)^2.

The copper losses can only be approximated. The 150,000 kilowatt-hours could be 206 kw for 730 hours, or 600 kw for 250 hours, or many other combinations of load and time between these limits. The figure of 600 kw is set as the probable upper limit of the load, since any higher load might damage the transformers.

206 kw for 730 hours, the lowest possible loss for this registration, gives $3 \times 1.9 \times 730 \times (206/600)^2 = 490$ kilowatt-hours

600 kw for 250 hours, the highest probable loss, gives $3 \times 1.9 \times 250 = 1{,}424$ kilowatt-hours

The customer ordinarily would not install an excessively large transformer bank, nor would he dangerously overload it. Consequently, the loss limits are set, the former condition giving a lower loss limit (core loss plus continuous-load copper loss), and the latter giving the upper loss limit.

The various methods will now be discussed in detail.

1. Estimation of Losses From the Meter Readings

The increase in the energy rate to cover losses should be such that the total

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energy per month, multiplied by the increase in rate, will equal the total transformer losses over the month multiplied by the usual energy rate. Expressing this as an equation, let

E = energy metered on secondary winding side per month
L = transformer losses per month
R = usual energy rate
ΔR = rate increase

Then

$$\Delta R \times E = L \times R$$

or

$$\Delta R = R \times L/E$$

The percentage increase is evidently L/E which in most cases will be perhaps two per cent as a maximum. Two per cent would be too great at full load, but this compensates for the fact that no energy is being registered at no load, although core loss is still being supplied. The approximation is quite close, as will be seen.

The demand rate should be increased by about one per cent to cover losses at full load (expected demand).

The previous reasoning also makes it plain that discounts may be reduced by the same percentages in order to compensate for the losses; namely, two per cent for energy and one per cent for demand. The estimation below shows how this will work out.

The losses might also be estimated and billed separately at the usual rate.

The estimation in any event would be as follows:

Energy metered = 150,000 kilowatt-hours

Core loss (one-half per cent of transformer rating for 730 hours) is $730 \times 1 \times 3 = 2{,}190$ kilowatt-hours

Copper loss (assume one-half per cent of kilowatt-hours) = 750 kilowatt-hours

Estimated total losses = 2,940 kilowatt-hours

Total energy billed = 152,940 kilowatt-hours

As previously mentioned, there are two limiting conditions:

(*a*). A steady load all month which gives minimum losses.

In this case, the steady load which would deliver the given energy in one month would be (150,000/730) = 206 kw.

Copper losses would be 490 kilowatt-hours, as calculated previously. Total losses would be 2,680 kilowatt-hours. Total energy supplied would be 152,680 kilowatt-hours.

(*b*). Rated load on for a length of time which delivers the given energy, and no load the remainder of the time. Thus, 600 kw for 250 hours would give 150,000 kilowatt-hours. The copper losses under these conditions would be 1,424 kilowatt-hours as calculated previously. Total losses would be 3,614 kilowatt-hours, and total energy supplied 153,614 kilowatt-hours.

The estimate is 2,940 kilowatt-hours, whereas the average of the two limiting cases is 3,147 kilowatt-hours. The billing based on the estimation would be within 0.15 per cent of the billing based on the average of the two limiting conditions. Actually, this error is less than those encountered in meter calibration.

The total losses are seen to be just about two per cent of the measured power in either case, which is the justification for the two per cent figure used above.

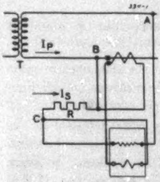

Figure 1 (left). Line-drop compensator—single-phase circuit

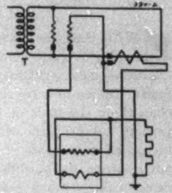

Figure 2 (right). Line-drop compensator—single-phase circuit with alternative connections

Even the limiting conditions above give errors of only 0.2 per cent and 0.45 per cent, and these conditions are very improbable. For case *b* (maximum losses), the estimate would be within 674 kilowatt-hours of the actual losses. At 1.5 cent per kilowatt-hour, this would mean a loss of revenue of $10 a month or $120 a year. A metering outfit for use on the high side would cost over $1,000. Interest, taxes, and depreciation would take most of the revenue collected, showing little justification for the use of a metering outfit on this size of load.

Another billing method, not quite so simple as the first given but slightly more accurate, is to add a fixed number of kilowatt-hours, plus a percentage of the energy registered. The fixed number of kilowatt-hours would equal the core loss as previously estimated. The percentage of registered energy would be (copper loss/registered energy) also as previously estimated. This method is obviously the same as estimating the losses and billing at the usual rate.

Also, in general, it is true, that, the higher the registration for a given transformer rating, the more accurate the estimate can be made.

2. Loss or Compensating Meters

Since core loss varies as the square of the voltage, a voltage-squared hour meter consisting of one element for each phase of the transformer will register the core loss if the correct multiplier is used. Similarly, copper loss may be measured by a current-squared meter with the correct multiplier. The error in loss measurement can be reduced to a very small amount, but accurate transformer data are required, and these are not always available or obtainable. Transformer data can be estimated, but design variations are such that the error may be as much as 0.3 per cent of the total power, and the accuracy is not much better than the estimating methods given. When the data are accurately known, the errors generally will be no greater than those involved in metering directly on the high-voltage side and, as will be seen later, may be less.

Compensating meters may be individual current-squared and voltage-squared hour meters, or they may be combined in one meter. Although three elements of each type theoretically would be required, the accuracy is almost as good using one voltage and two current elements.[2] If the load is normally well balanced, one current element is sufficient.

Loss meters also may be used to form the basis for an estimating method. That is, a compensating meter may be installed for a representative period of time, and the losses compared to the registration of the watt-hour meter during that period. This may be used then as a basis for changing the rate. For example, if the losses were 1.75 per cent of the registration during this period, the energy rate evidently should be increased, or the discount reduced, by 1.75 per cent. Loss meters of the current-squared type only would be sufficient in cases where the voltage is known fairly accurately. Most installations probably will fall into this category. A five per cent change in voltage will change core loss by about ten per cent. However, core loss will normally be about one per cent of the registration, so that the net error would be only about 0.1 per cent, if the average voltage differed by five per cent from that estimated.

3. Line-Drop Compensator

Impedances may be used in the secondary windings of current transformers to produce a drop equivalent to that of the transformer bank. This drop may be added to the secondary potential transformer voltage to make a sum proportional to the primary voltage. Since a reactance drop would be at right angles to the current, the added voltage from a reactance-drop compensator would produce no registration on a watt-hour meter. Therefore, only resistance compensation is needed in power metering.

The type of transformer bank determines the number of compensators required; two for open delta, three for star or delta.

Figure 1 illustrates the principle of the line-drop compensator. The current transformer circulates the current I_s in the direction indicated, while I_p proportional to it flows in the main circuit. The voltage between B and A must be increased by the resistance drop in the transformer T to obtain the equivalent high-side voltage. The resistor R is adjusted so that the secondary current produces a drop equivalent to the transformer drop. It is seen that I_p and I_s both flow toward B, so that the polarity is such as to add the voltage drop.

Another way of looking at this is to say that voltage BA would normally be applied to the potential coil, B being at higher potential than A when the current flow is as indicated. The point C, however, is at higher potential than B by the amount of the resistance drop in R. Therefore, if the potential coil is connected from C to A, the potential across it will be greater than BA by the drop in R. The drop in R, in turn, is equivalent to the drop in T, so that the equivalent of the high-side voltage is applied to the meter.

Figure 2 shows the same method, rearranged to permit grounding of instrument transformer secondary windings. The difference from Figure 1 is in the order in which the transformer secondary compensating resistor and meter coil are connected in series.

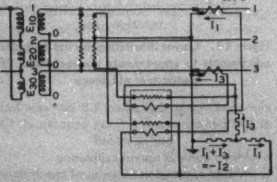

Figure 3. Line-drop compensator used in three-phase three-wire circuit

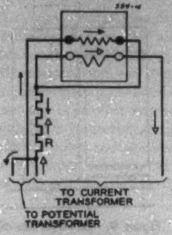

Figure 4. Polarity requirements for line-drop compensators

In Figure 2 the current flows through the compensator and then through the meter. The potential coil is connected across DF. The voltage DE, which normally is applied to the meter, is increased by the drop EF and the total DF, applied to the coil. E is at a lower potential than D, and the drop in EF places F at a still lower potential than E.

Figure 1 is the clearest for single-phase, whereas Figure 2 is simpler in a three-phase circuit, in which a third resistor is needed to compensate for the third phase current as shown in Figure 3.

Note that although only two elements are used, the drops in the three transformers are taken care of by the wye-connected compensator. In Figure 3 the voltage E_{12}, for example, $=E_{10}+E_{02}=E_{10}-E_{20}$. The drops which are added vectorially to this voltage before applying it to the potential coil are the ones caused by the currents I_1 and $-I_2$. ($I_1+I_3=-I_2$.) The addition of these drops gives the equivalent high-side voltages E_{10} and $-E_{20}$, which equals E_{12}.

Although the transformers are shown connected delta-wye, the secondary of course, could be delta-connected, and the meter and compensator connections would not be changed.

The polarity of the connections on a line-drop compensating scheme is important. To add a voltage to correct for IR drop, a drop $-IR$ is needed. This is obtained by connecting the voltage leads so that the currents of the voltage and current circuits are opposite in the compensator (see Figure 4).

Sometimes the wiring may be used to provide the resistance required. For example, with one-half per cent drop, or 0.6 volt on 120-volt basis, the value of R would be about 0.12 ohm at five amperes or say 75 feet of number 12 American wire gauge wire. Where common return wires are used for both current and voltage, a compensating effect occurs. With the usual connection, it results in low readings, since the currents of the voltage and current circuits are in the same direction. When properly connected, however, compensation may be obtained this way without other equipment.

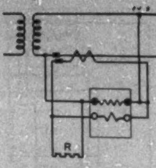

Figure 5. Connection of dummy loads to measure core loss

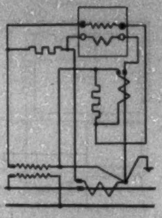

Figure 6. Combination of line-drop compensator with dummy loads using an additional current transformer, single-phase circuit

4. Dummy Loads

Dummy loads may be used to correct for core loss. The saturation curve of the core is not a straight line, with the result that the correction will be slightly low on higher than normal voltage, and slightly high on lower than normal voltage.

Figure 5 shows core-loss compensation by means of a dummy load (the resistance R), which allows a current to circulate through the current coil in such a direction as to add the core loss. The value of R usually will be around 5,000 ohms. The saturation curve could be approximated by an iron-cored impedance Z, but adjustment difficulties make this impractical.

5. Line-Drop Compensators and Dummy Loads Combined

Methods 3 and 4 may be combined to correct for copper loss and core loss, but it will be found that the voltage and current connections required are not consistent with each other if both the transformers are to be grounded.

Four alternatives exist:

(*a*). Use additional current transformers to reverse the current for the resistor compensating for copper loss (Figure 6).

(*b*). Use additional voltage transformers to reverse the voltage for the resistor compensating for core loss (Figure 7).

(*c*). Add an extra current winding on the meter to provide the equivalent of *b* (Figure 8).

(*d*). Use grounds on only the current transformer secondary windings. The voltage

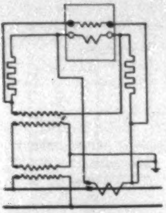

Figure 7. Combination of line-drop compensator with dummy loads using an additional potential transformer, single-phase circuit

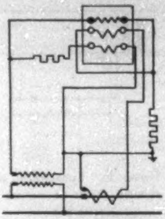

Figure 8. Combination of line-drop compensator with dummy loads using an additional series winding, single-phase circuit

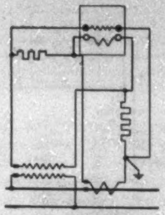

Figure 9. Combination of line-drop compensator with dummy loads, grounding the potential transformer through the current circuit, single-phase circuit

transformer secondary windings will then be grounded through the current circuit[3] (Figure 9).

6. Meter Adjustments

This method consists of setting the meter about five per cent fast at ten per cent load, one per cent fast at full load, and two per cent fast on 50 per cent power factor. Since the effect of the adjustments practically will offset the transformer losses, the over-all result will be quite close over the ranges of load normally encountered.

From the transformer efficiency curve, the full-load and light-load settings can be determined. In the example shown in Figure 11, these values are found to be 1.5 per cent at 100 per cent load and 5.5 per cent at 10 per cent load. In this case, the resultant curve is fairly good over the whole range. If the transformers are under no load a fairly large portion of the time, it may be advisable to use a meter without anticreep holes or slots in the disk so that it will creep with voltage only applied.

If the disk has no irregularities of any sort, the core loss will be measured with very good accuracy, since the light-load adjustment provides a torque which varies as the square of the voltage, as does the core loss.

Note, however, that the same uniformity of the disk would be required of a meter on the high side, if the core loss were to be measured during periods of no load. Therefore, metering on the high side may be less accurate than some of the methods described here.

The method of determining the desired curve is as follows:

Take the reciprocal of the transformer efficiency curve. This is the curve which the watt-hour-meter registration should match. The standard watt-hour-meter curves are then changed by three adjustments. Full-load adjustment changes all points equally. Lagging adjustment changes all points at any given power factor (say 50 per cent) equally, and intermediate power factors are affected almost proportionally. For example, calibrating 100 per cent load to 101 per cent at unity power factor and 102 per cent at 50 per cent lagging power factor would cause all loads at unity power factor to register one per cent faster than the standard curve. All loads at 50 per cent power factor would register two per cent faster than the standard curve for 50 per cent power factor. At 75 per cent lagging power factor the change would be about 1.5 per cent.

The light-load adjuster supplies a torque independent of load, and therefore the effect on registration varies inversely with the speed. A change of five per cent at ten per cent load would change 50 per cent load by one per cent. Note that 50 per cent power factor will be affected twice as much on a percentage basis as unity power factor at the same current.

By means of these adjustments, the inverse of the transformer efficiency curve usually may be approximated rather closely. In this method, as well as any of the others, the transformer data are helpful in increasing the accuracy. If they are not obtainable, an average characteristic must be assumed.

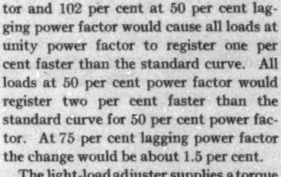

Figure 10. Combination of line-drop compensator with dummy loads, grounding the potential transformer through the current circuit, three-phase circuit

7. Combination of Meter Adjustment and Line-Drop Compensators

Line-drop compensators may be used for copper-loss compensation, and fast light-load adjustment for core-loss compensation. This would be more accurate on unbalanced loads, such as arc furnaces, than method 6, although the advantage is not great.

Reactive Power Measurements

The foregoing applies to power measurement. However, the same methods may be applied to the measurement of reactive power.

The errors introduced are, in general, the same as those involved in power measurement as previously discussed.

The reactive power losses again may be divided into two classes:

1. Excitation losses (corresponding to core loss)—dependent on voltage, but varying much more rapidly than the square.
2. Leakage reactance loss (corresponding to copper loss)—proportional to the square of the current.

Transformers of the type in question will have reactive power losses ranging from two per cent to ten per cent, depending on the transformer design.

The methods which were used for power metering may also be used to include reactive losses. The following points, however, should be noted.

When using line-drop compensators,

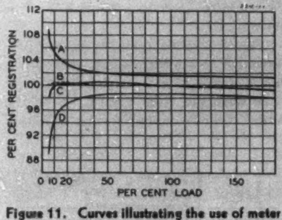

Figure 11. Curves illustrating the use of meter adjustments to approximate the reciprocal of transformer efficiency

A—Meter curve calibrated 1.5 per cent fast at 100 per cent load, 5.5 per cent fast at 10 per cent load
B—Meter curve at normal calibration
C—Meter registration in terms of transformer input
D—Transformer efficiency curve

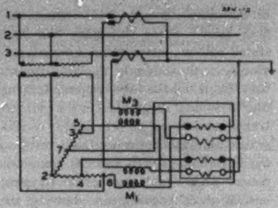

Figure 12. Line-drop compensation used on three-phase reactive meter circuit

reactance only is needed, for the same reason that resistance only is required to measure conductor loss. That is, a resistance drop added to the potential of the reactive meter would produce no rotation. Note, however, that when the reactive meter consists of a watt-hour meter and phase-shifting transformer, the above statements apply only when the compensation is applied on the primary side of the phase-shifting transformer. On the secondary side, resistance compensation could be used, but difficulties in making the proper connections made this undesirable. The best method is probably that shown in Figure 12 which used mutual inductors. This allows more freedom in making connections, since the two windings are not tied together.

It will be observed that the current transformer in line 1 feeds current through mutual inductance N_1 which is in the potential circuit 6–7. This voltage lags 3–2 by 90 degrees and is applied to the element in which the current from the transformer in line 3 is used. The connections of the other inductance are similar.

The use of dummy loads to measure kilovar-hour excitation loss gives somewhat less accuracy than the use of dummy loads for core loss.

If compensating meters of the voltage-squared and current-squared type are used to measure power losses, no additional equipment is necessary to measure reactive power losses. Multiplying the readings of these meters by the correct factors will give the reactive power losses.

Reactive power measurements are subject to errors, because the magnetizing current varies widely with fluctuations in voltage. For this reason, the most accurate method is the use of compensating meters of the voltage-squared type. The kilovar-hours can then be computed from the readings obtained. This method probably is justified only on transformers with high magnetizing current. If the reactive power measurement is used only to determine demand, the errors in any method will be rather small.

Summary

The foregoing will make it plain that several methods of including transformer losses in the billing are available. Both estimating and measuring methods may be used with reasonable accuracy. There is no best method for all installations. Each must be considered individually. The most desirable method is determined by the various factors involved in each installation. These factors will include expense, customer relations, commission requirements, and importance of maintaining laboratory routine, to mention a few of the more important.

Appendix

The fact that the line-drop compensation shown in Figure 3 is correct is proved as follows:

In any delta transformer bank with balanced impedances (Figure 13)

$$\begin{aligned} I_1 &= I_b - I_c \\ I_2 &= I_c - I_a \\ I_3 &= I_a - I_b \end{aligned} \qquad (1)$$

(Since the impedances are balanced)

$$I_a + I_b + I_c = 0 \qquad (2)$$

From equation 1 we find

$$I_1^2 + I_2^2 + I_3^2 = 2(I_a^2 + I_b^2 + I_c^2) - 2(I_aI_b + I_bI_c + I_cI_a) \qquad (3)$$

The actual copper loss $= (I_a^2 + I_b^2 + I_c^2)R$

where R is the a-c resistance of each phase of the transformer. In the meter (see Figure 3) the element with current I_1 has a voltage $(I_1 - I_2)R_c$ added to its potential coil, and similarly the element with current I_3 has a voltage $(I_3 - I_2)R_c$ added to its potential coil, so the loss measured,

$$P = I_1(I_1 - I_2)R_c + I_3(I_3 - I_2)R_c$$

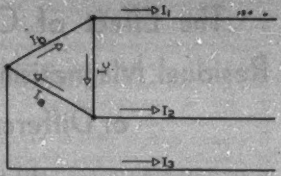

Figure 13. Line and phase currents of the secondary winding of a delta transformer bank

where R_c is the resistance of each leg of the compensator.

Combining terms

$$\begin{aligned} P &= [I_1^2 + I_3^2 - I_2(I_1 + I_3)]R_c \\ &= (I_1^2 + I_2^2 + I_3^2)R_c \text{ since } (I_1 + I_3) = -I_2 \end{aligned}$$

Using equation 3

$$P = [2(I_a^2 + I_b^2 + I_c^2) - 2(I_aI_b + I_bI_c + I_cI_a)]R_c$$

Now we add and subtract

$$(I_a^2 + I_b^2 + I_c^2)R_c$$

which gives

$$P = [3(I_a^2 + I_b^2 + I_c^2) - 2(I_aI_b + I_bI_c + I_cI_a) - (I_a^2 + I_b^2 + I_c^2)]R_c \qquad (4)$$

However

$$(I_a + I_b + I_c)^2 = (I_a^2 + I_b^2 + I_c^2) + 2(I_aI_b + I_bI_c + I_cI_a)$$

From equation 2 this equals 0 so the last two terms in equation 4 drop out. The measured loss is therefore

$$3R_c(I_a^2 + I_b^2 + I_c^2)$$

If

$$R_c = (R/3)$$

the measured loss will therefore equal the actual loss

$$R(I_a^2 + I_b^2 + I_c^2)$$

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The Effect of Current-Transformer Residual Magnetism on Balanced-Current or Differential Relays

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ASSOCIATE AIEE

Synopsis: High-speed current-balance relaying has been subject to occasional false tripping that has not been explained satisfactorily. Field and factory tests now show that this is probably due to differences in residual fluxes in the current-transformer cores. The limiting value of these fluxes can be calculated approximately, and they can be measured with portable instruments connected to the secondary winding. They may cause a deficiency in secondary current, especially during the first half-cycle of fault current, which will cause current-balance or differential-overcurrent relays to operate falsely. This false operation can be avoided by the addition of a short inherent or external time delay with relays having a high dropout, or by the use of special current transformers.

BALANCED-CURRENT relaying has been favored widely in system protection, because, in principle, its complete lack of sensitivity to faults outside the protected line makes time delays unnecessary and avoids all problems of co-ordination with the settings of relays further from the source.

Experience with induction-disk relays applied in this form of protection was entirely satisfactory until requirements of system stability or of uninterrupted operation of industrial motors caused a demand for reduction in the fault duration even below the minimum of a few cycles obtained with such relays. High-speed current-balance relays operating in less than a half-cycle were then applied and experience with them also has been satisfactory, generally. However, there has been a very small percentage of false operations resulting in the tripping of a breaker on a sound line, and, if the fault causing this tripping is on the only other line between the two terminals, such false operation naturally causes loss of the tie or interruption of supply. The purpose of this paper is to report an investigation which has shown the probable cause of most, if not all, of the false operations not otherwise accounted for and provided against.

The clew that provided the basis for the investigation was obtained from tests made by G. S. Whitlow, then of the General Electric Company, and C. E. Asbury of the Illinois–Iowa Power Company. They were conducting primary tests with a low-voltage source to determine the reason for one of the false operations mentioned above and found that the only way to cause false tripping with equal primary currents was to reverse the direction of current through the primary winding of one current transformer with respect to the other. The sudden first application of current after a reversal would usually result in operation of one or the other element of the current-balance relay. Since this relay had been tested without current transformers previously with suddenly applied current, they diagnosed the false tripping as being due to residual magnetism in the current-transformer cores. Fortunately, oscillograms were taken during these tests, so the offending element had left some evidence. Figure 1 is a tracing from a typical one of those oscillograms showing the currents which caused false tripping by providing insufficient restraint. The traces show the two secondary currents, and, since the primary conductors of the two current transformers were in series, it is obvious that the deficiency of secondary current in one of the current transformers in the first half-cycle was due to some phenomenon within the current transformer. This phenomenon has been analyzed and will be described briefly at the outset as a basis for an understanding of the test methods and test results described later.

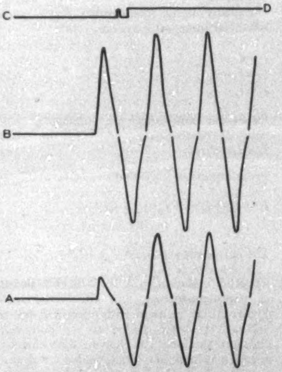

Figure 1. Oscillogram from field tests, showing difference between secondary currents of two similar 38-turn current transformers with similar burdens and 2,540 amperes primary current

2,540/38 = 67

A—Secondary current of current transformer with residual flux

B—Secondary current of current transformer with less (or opposite) residual flux

C—Relay contacts open

D—Relay contacts closed

Hypothesis to Explain the Observed Phenomenon

The following hypothesis appears to be in accordance with the results of the field tests and the factory tests:

1. A significant amount of residual flux is left in the core of only one of the two current transformers which supply one balanced-current relay.

2. This residual flux remains in the current-transformer core practically undiminished unless the circuit is loaded, in which case it is decreased gradually but not usually removed entirely.

3. A subsequent fault beyond (or near) the end of the pair of protected lines with both lines in service will draw equal (or nearly equal) currents through the primary windings of the pair of current transformers as shown in Figure 2. The direction of primary current during the first half-cycle is random and may be such as to require an increase in current-transformer core flux in the same direction as the existing residual flux.

4. The sum of these fluxes is likely to exceed saturation value, in which case there will be a deficiency of secondary current from this current transformer equal to the magnetizing current necessary to produce enough flux to generate the voltage required for the actual secondary current.

5. This deficiency of secondary current will be smaller, possibly even negligible, in the other current transformer because of its lack of residual flux.

6. The greater secondary current from the current transformer without residual flux may operate the balanced-current relay in one direction.

7. Since the location of residual flux (in one

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current transformer or the other) is unrelated to the location of the subsequent fault, and the currents are practically equal except for the transient error, an element may operate falsely for an external fault; or an element on the sound one of a pair of lines may operate for a fault in the other line, near its further end.

With the preceding brief outline to show the relation between the various ideas involved, we can profitably cover the first two of the points in more detail.

1. The residual flux may be left by a half-cycle impulse such as a fault current cleared by an expulsion gap or a fuse, or such as a power-frequency current following a lightning arrester discharge; or by a fault current cleared after an odd number of half-cycles; or by a fault current that is offset because it started in an inductive circuit near the zero point of the voltage wave; or by a power-transformer magnetizing-inrush current which is offset because of the presence of residual flux in the power-transformer core prior to its energization. Residual flux may be left in only one of the pair of current transformers because the primary conductor of its mate is out of circuit at the time of the transient; or because the fault causing the transient was on the primary circuit of one current transformer and so near by that the currents in the two current transformers were greatly different in magnitude.

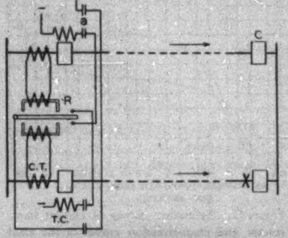
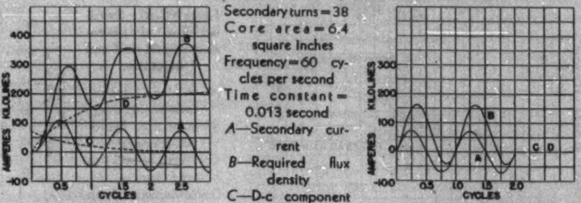


Figure 2. Schematic connections for balanced-current relaying of two parallel lines, showing equal currents in the two lines for a fault at the end of one of them (length of arrows indicates current magnitude)

a = Auxiliary switch, closed when *C* is closed
C = Circuit breaker
CT = Current transformer
R = Current-balance relay
TC = Trip coil
X = Fault

In earlier days of the industry it was considered a serious offense to open-circuit a current-transformer secondary winding partly because of the resulting high voltage, but also because of the danger of magnetizing the core. It is now known that any of the conditions previously mentioned can run the flux density in the current-transformer core up to saturation value if the primary current or secondary burden is high enough, or with smaller currents or burdens if the transient is applied twice or a few times with the same direction of initial half-cycle; and therefore the residual flux introduced by unavoidable circumstances may be practically as great as the value formerly supposed to result only from careless testing or faulty wiring or devices.



(a). Wave started at 90+(tan$^{-1}X/R$) degrees after instant of zero voltage, where *X* and *R* constitute the impedance limiting the current in the primary circuit; this gives a fully offset wave

(b). Wave started at (tan^{-1} X/R) degrees after instant of zero voltage; this gives a symmetrical wave

Figure 3. Secondary currents in burden of 0.77+*j*1.06 ohms, and current-transformer-core flux densities required for these currents

Figure 3a shows a fully offset primary-current wave and the corresponding wave of required flux applying to the current transformer and burden used in the error-current tests. This fully offset wave requires more flux than any other current of the same symmetrical value having a smaller degree of offset. The fluxes required for larger or smaller currents are in direct proportion to the current. Figure 3b shows a symmetrical current wave and the corresponding wave of required flux; this symmetrical wave requires less flux than any other current of equal magnitude with this burden. A purely reactive burden of the same total impedance would require a flux wave of the same amplitude, but this flux wave would be symmetrical, and therefore its peak value would be much less. A reduction in secondary resistance may be even more beneficial with an offset wave, because it decreases the unidirectional component of flux caused by the d-c component of current. These flux waves were calculated from the formula

$$\phi = \frac{10^8}{N}\left(R\int_0^t i_p dt + L i_p\right) \qquad (1)$$

2. The fact that normal operation fails to remove the residual flux completely is in accordance with the known characteristics of permanent magnet materials, which differ from transformer iron only in degree even though the degree is extreme. If a large magnetizing force is applied to either of these materials it produces saturation, as indicated in Figure 4, and, if it is reduced to zero without reversal, a residual flux is left, often of just as great a percentage of its own saturation value for high-quality annealed iron as for high-quality permanent magnet steel; the main difference between these steels is in the coercive force necessary to bring the flux to zero. Then, if a pulsating or alternating magnetomotive force is imposed, and this magnetomotive force is small compared with the coercive force of the particular material, the operation will stabilize on what is known in permanent-magnet theory as a minor loop, and this loop will be far removed from the zero axis of flux, one end coinciding with the residual-flux trace of the high-density hysteresis loop unless the small alternating magnetomotive force has been reduced from the maximum value attained since saturation. A phenomenon of this sort is characteristic of current-transformer operation at normal loads, because they require only a small flux density and therefore a fairly small magnetomotive force even when displaced from the zero axis of flux. Figure 4 is a sketch of half of the usual major loop *B* resulting from the application of fault current, within it a minor loop *C* resulting from the subsequent application of full load current, and also a smaller minor loop *D* obtained by finally reducing the load current to half rated value; these minor loops are

not test results, but are intended only to convey an understanding of the phenomenon.

Outline of Tests

In an effort to duplicate in the factory the false operation obtained in field test, a series of tests was laid out using a similar burden on current transformers of nearly the same size as those used in the field tests, and with secondary turns chosen to require the same flux density as those current transformers for the same secondary current. A method was devised, and checked, for measuring residual flux without decreasing it greatly and measurements were made by this method of the residual flux left by various transients. Tests were made to determine whether the passage of time or of rated current would cause a decrease in the residual flux left in the core. Oscillograms were taken showing primary current, secondary current, and error current with and without residual flux, and others were taken showing primary and secondary current and relay operation with and nonoperation without residual flux.

Table I lists for reference the dimensions and other pertinent data on the current transformers and burden used in the tests.

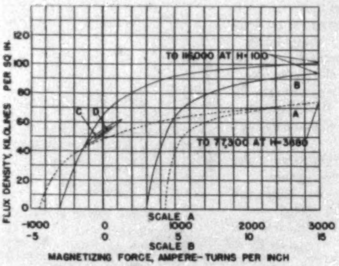


Figure 4 (left). Residual flux of permanent-magnet steel and ingot iron

A = Alnico permanent-magnet steel
B = Ingot iron, hydrogen-annealed
C = Minor loop in ingot iron
D = Minor minor loop in ingot iron

Nondestructive Test for Residual Flux

It was apparent at the outset that ballistic measurements of residual flux left by typical primary-current transients would not be feasible, since the zero-flux reference point would be obliterated by the transient; and also because splitting the core, even though the joint surfaces were ground, would introduce an unavoidable gap which would decrease the residual that was the most important quantity in the investigation. Also, it was felt that a method of residual-flux measurement using only portable meters and readily obtainable test power would be useful for field work. Another requirement for the investigation at hand was that the method should not decrease the residual flux very much in the process of measurement, since it was desired to produce a residual flux by applying a typical transient, measure this flux, and then use the flux after measurement to cause an error current which could be measured, or checked for its effect on a relay.

For the above reasons a method was devised which consists of applying to the secondary winding a very small measured 60-cycle magnetizing current and measuring the voltage necessary to circulate this current through the winding; this method will hereinafter be described as the impedance method of residual-flux measurement. By trial of this method it was found that the voltage decreased as the residual flux increased, and the relation between the volts per turn per square inch and the residual-flux density was determined experimentally by the use of a portable flux meter (which is equivalent to a ballistic galvanometer). This relation is shown in Figure 6 for a magnetomotive force of 0.258 root-mean-square ampere turns per inch of core length. The induced voltage is very sensitive to magnetomotive-force magnitude, as can be seen from the fact that a force half as great gave no measurable voltage, and so the curve should not be used to determine residual flux with any other value of magnetomotive force impressed. The voltages were measured by means of a rectifier type of voltmeter because of the relatively low impedance of the dynamometer type.

A determination of the decrease caused in residual flux by this magnetomotive force was made by leaving the flux meter connected during the instant when the measuring force was first applied; Figure 6 shows also this relation.

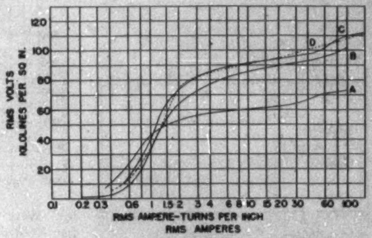


Figure 5. Saturation curves of current transformer and magnetization curves of the steel

A—A-c saturation curve, 38 secondary turns
B—D-c magnetization curve, 38 secondary turns
C—A-c magnetization curve of steel, calculated from *A*
D—A-c magnetization curve of steel, design data

Table I. Characteristics of Current Transformers and Burden Used in Test

Core material	2.75 per cent silicon steel
Core construction	Spiral strip
Core treatment	Annealed after welding ends
Core diameters	$8^{3}/_{4}$ inches outside, $6^{3}/_{4}$ inches inside
Core height	$7^{5}/_{8}$ inches
Active core area	6.4 square inches (space factor 0.88)
Mean flux path	24.4 inches
Windings	Two, of 38 turns each, 0.12 ohm each
Burden at 5*A*	$0.65+j1.06=1.16$ ohms
Burden at 50*A*	0.72 ohm
Size constant (2)	491
Saturation curves	Figure 5

Measured Values of Residual Flux

Measurements were made by the ballistic method to determine the relation between the flux density produced by application of a given value of magnetomotive force and the residual-flux density remaining after removal of this force. This test was made at various values of flux density up to saturation, and the results are shown in Figure 6. The results as plotted are a characteristic of the grade of iron in the transformer and not merely a characteristic of this particular size of transformer. It will be noted that the residual flux is 60 to 85 per cent of the maximum flux immediately preceding.

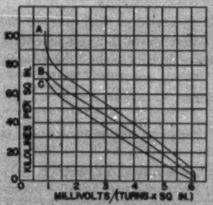
This information enables one to assign a maximum value to the residual-flux density which will be left in a current-transformer core by a given transient, if

Table II. Measured Flux Density Left in Current-Transformer Core by Various Transients

Trial	Oscillogram	Wave*	Amperes	Cycles	Flux Density, Kilolines Per Square Inch: Before Test	Added by Test (From Figure 3)	Residual: (From Figure 6)	Measured
1	8A	S	50	7.0	Small	40	24	39
2		S	50	6.5	0	162	75	53
3	8B	S	50	6.5	45	162	75	58
4		S	50	6.5	50	162	75	60
5		S	20	5.5	0	65	56	32
6		S	20	5.5	23	65	74	56
7		S	20	5.5	48	65	75	67
8		S	20	5.5	59	65	75	70
9	8C	O	50	6.0	0	380	75	35
10		O	50	6.0	27	380	75	67
11		O	50	6.0	60	380	75	67
12	8D	O	50	½	0	295	75	65

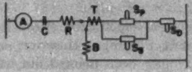
* *S* = Symmetrical.
O = Completely Offset.

he knows the initial density, core dimensions, turns, and voltage required by the burden for this particular transient; it is necessary only to determine from the usual transformer relation the peak flux density corresponding to the required voltage, add it to the initial density, and read from the curve the maximum residual-flux density that can be left in the core.



(Left) *A* = Maximum flux density during last half-cycle before applying test current
B = Residual flux density before applying test current
C = Residual flux density after applying test current

Figure 6. Relation between induced voltage and previous flux values, at 0.258 ampere turns per inch of core length, 60 cycles



A = Ammeter
B = Burden
C = Synchronous switch
R = Reactor with air gap
S_D, S_P, S_S = Shunts with oscillograph elements; difference current, primary current, and secondary current respectively
T = Current transformer under test

Figure 7. Circuit for oscillographic measurement of error current

Measurements were made by the impedance method to determine the residual-flux density left by various transients such as are likely to be caused by lightning discharges or other insulation failure. To permit direct comparison of the results with various types of transients, the tests were all made at the same steady-state value of secondary current, approximately 50 amperes, with a burden of 1.16 ohms. The results are given in Table II. The measured values are usually less than the ones predicted from Figure 6, partly because the maximum density is less than the calculated value by reason of saturation of the burden and partly because the d-c component of error current partially restores the core flux to symmetry if the transient continues more than one cycle, thus further reducing the maximum density reached during the last half-cycle.

A test was made showing how residual flux can accumulate as a result of successive transients which happen to have the same polarity of initial half-cycle. The results are the second, third, and fourth trials listed in Table II; also the fifth to eighth, and the ninth to eleventh.

Permanence of Residual Flux

The core was magnetized and the residual-flux density, measured by the impedance method, was found to be 75,000 lines per square inch. The core was left undisturbed for about 40 hours, after which a check measurement gave the same readings.

A current of five amperes at 60 cycles was then applied to the primary, requiring a secondary voltage of 5.8 and a calculated peak-to-peak flux density of 18,000 and was left flowing for about 24 hours, after which another check measurement showed that the residual-flux density decreased to 23,000. Lower currents would cause smaller decreases, but no investigation was made of this relation, or of the relation between time and residual flux, at five amperes load.

Oscillograms of Error Current

The concept of error current or exciting current in a current transformer has sometimes seemed elusive, and it was thought that the nature of the phenomenon being investigated would be made clearer if we could get a direct measurement of this current in a form which would permit visualizing its relation to the primary and secondary currents.

An oscillogram provides the best means for visualizing an electrical quantity, especially a transient one, and so the apparatus was arranged to provide a portion of the circuit where error current could be measured directly. Referring to Figure 7, the primary and secondary windings, having an equal number of turns, were connected in series with oscillograph shunts. From the junction of these two shunts a third one was connected to receive the difference between the primary and secondary current; this difference is the error current, when the turn ratio is 1/1. When the current transformer transforms perfectly, there is no voltage drop across the error-current shunt and no current in it, but, when the ratio breaks down, the error current flows in this shunt. This method is subject to a slight inaccuracy, because, when this shunt carries current, there is a voltage drop across its terminals which helps the secondary induced-voltage drive current through the burden impedance, so that the error current obtained is somewhat less than it would be if the secondary circuit were closed directly on itself, and therefore the results are slightly optimistic. The maximum instantaneous drop across this shunt was 13 volts as compared with an instantaneous value (calculated from the oscillogram) at the same instant, of 23 volts induced in the secondary winding, but the inaccuracy of results seemed slight enough to be tolerated for the sake of obtaining a direct record of error current.

Typical oscillograms obtained with this circuit are reproduced in Figure 8. Oscillograms (*A*) and (*B*) show the difference in behavior, with symmetrical waves of primary current, depending on the absence or presence of residual flux; (*C*) and (*D*) show the corresponding comparison with fully offset waves. The residual-flux measurements show how the succeeding cycles of fault current in (*C*) removed a large part of the flux put in by the first half-cycle, which was similar to (*E*).

Effect on Relay

The final test of the hypothesis was an experiment to determine whether the increase in error current, already shown to result from the presence of residual flux in the core, would permit picking up a relay which would be restrained successfully in the absence of the residual core flux.

The burden 'or this test consisted of a

current-balance relay plus enough artificial burden to bring the total external burden up to $0.57+j0.82=1.01$ ohms. Including the internal resistance, the total resistance was 0.69 ohm, giving a size constant of 550.

In the course of this experiment it was found that

1. The normal transient error current without residual flux was sufficient to permit operation of a current-balance relay energized from the primary and secondary currents of the same 1/1 current transformer.

2. The difference between the transient error currents of similar current transformers without residual flux and with equal primary currents but with different burdens was sufficient to permit operation of a relay balancing the two secondary currents.

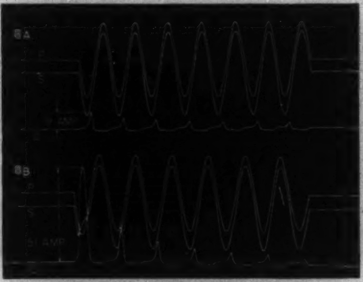
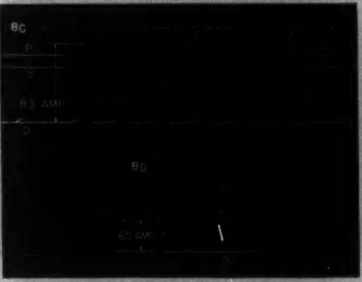


Figure 8. Typical oscillograms of error current (see Table II for identification)

D = Error current
P = Primary current
S = Secondary current



After these difficulties were overcome, it was found that

3. With similar current transformers, no residual flux, equal burdens, and equal primary currents, the relay would not operate.

4. With similar current transformers, equal burdens, and equal primary currents, but with residual flux in only one current transformer, the relay would operate.

The oscillograms of tests 3 and 4 are reproduced in Figure 9.

Effect on Generator Differential Protection

This phenomenon need have no effect on generator-differential protection using only two sets of current transformers, since they can be demagnetized before being put in service if it is suspected that they are magnetized, and their subsequent histories are identical. It should be considered, however if three sets of current transformers are used, as is sometimes necessary with double-bus arrangements or with station-auxiliary transformers tapped off between the generator and its circuit breaker. The reason is that the currents during a given fault, and therefore the residual fluxes resulting from it, are necessarily unequal in the three sets of current transformers; and a subsequent fault involving the opposite one of the two sets of current transformers in the line side of the generator winding will encounter different residual fluxes in the two sets involved this time and may cause false operation of the differential protection if its delay is inadequate.

Effect on Bus Differential Protection

This phenomenon affects differential-overcurrent relays used for bus protection just as much as it affects balanced-current relaying. However, as has previously been pointed out,[1] this effect is unimportant in comparison with core saturation caused by the d-c component of an offset current, and so it need be considered only in marginal cases, if at all.

Effect on Instantaneous Residual-Current Relays

Residual-current relays are often considered advantageous, because their settings are independent of load current. In some cases, such as a single line terminating in an ungrounded winding of a machine or a transformer bank, no selectivity is required, and therefore instantaneous relays have been used. Such relays are liable to false operation because of error currents resulting from different residual fluxes in the current transformers of different phases. Such inequality of residual fluxes in the event of a phase-to-phase fault can result from a previous ground fault, or from a previous phase-to-phase fault involving a different pair of phases. Such false operation of an instantaneous ground-overcurrent relay has been recorded in service, although it has not been determined whether the major cause was the residual core flux or the inherent difference in d-c component of the transient currents in the three phases.

Conclusions

While the phenomenon described is reproducible at will under controlled conditions, reported field experience indicates that the percentage of false operations that might be traced to it is small, and undoubtedly there are many locations where these improbable operations can be tolerated. However, some locations require the greatest obtainable reliability,

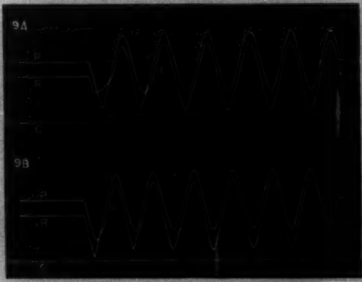


Figure 9. Oscillograms of relay operation with, and nonoperation without, residual flux

9A = With residual flux in only one current transformer
9B = Without residual flux in either current transformer
P = Primary current
R = Secondary current in restraining coil
C = Current in contact circuit

and for them it seems necessary to seek a means of avoiding false tripping.

One means, but not a very practical one, is to demagnetize both sets of current transformers after each external fault when only one circuit is in service, and after each internal fault.

Another means is to use only relays with sufficient delay to permit the error current to reduce the residual flux and thus prevent completion of their false operation. This delay may be inherent, or it may be incorporated in an external definite time relay if the balanced-current relay has a dropout high enough to permit it to reset when the secondary currents again approach equality.

In cases where high-speed relaying is essential, the best solution lies in the use of current transformers with small or large air gaps in the cores.[2] Ironless couplers[3] could be used, but would provide a smaller output and require a more sensitive relay.

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Effect of Radio Frequencies of a Power System on Radio-Receiving Systems

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IN the last few years considerable work has been done, in this country by the Joint Co-ordination Committee on Radio Reception of Edison Electric Institute, National Electrical Manufacturers' Association, and Radio Manufacturers' Association, and in Canada by the Department of Transport, to develop radio-noise measuring circuits and instruments.[1,2] The methods[3] of measuring radio noise and the instruments have reached the point in their development where the data obtained with these standard instruments can be correlated and the radio-influence characteristics of electric apparatus and power systems can be determined.

Radio co-ordination of electric apparatus is conveniently divided into two classes—low-voltage and high-voltage. The low-voltage class consists of apparatus used below 1,200 volts and frequently operated by and on the premises of the radio listener. Considerable co-ordination work has been done on this class of apparatus, and values of radio-influence voltage have been established for certain appliances and small motors. The high-voltage class includes apparatus used in this generation, transmission, and distribution of electric power. The problem of co-ordination of radio reception with this class of apparatus is more complex than it is for the low-voltage equipment and has not been completed.

This paper is concerned with high-voltage apparatus and describes tests and method for calculating the over-all effects on radio reception resulting from the radio-frequency voltages produced incidentally by power apparatus. The calculations depend upon a number of factors for some of which the range and probable values have not been determined. One of these factors for which information is required is the coupling factor of antenna to power lines and is defined as the ratio of field intensity at the radio listener's antenna to the field intensity under or near the transmission line. To determine this coupling factor, field tests were made at radio frequencies in a city receiving power from 25-kv transmission lines and 4-kv distribution circuits. A radio-frequency generator was connected to the 25-kv bus at the substation, and field-intensity measurements were made near the power lines and at 29 receiver antennas located at various distances from the 25-kv lines. The coupling factors to the nearest power circuit and to the 25-kv line were thus obtained for all the antennas tested.

Various other data useful in radio co-ordination were obtained. These include equivalent effective height of outdoor antennas used by radio listeners, types of grounds and distances from antenna to high-voltage and low-voltage lines.

Radio Co-ordination—High-Voltage Apparatus

In order to obtain a basis for evaluating the various factors in the radio co-ordination problem, it is necessary to provide a method of calculating the effect on radio reception of the radio-frequency voltages which may be generated by line and station apparatus. It can be shown that the following factors must be considered:

(*a*). Broadcast-station field intensity in microvolts per meter.

(*b*). Signal to noise ratio acceptable to radio listener.

(*c*). Ratio of interference field intensity at antenna to value at high-voltage line.

(*d*). Ratio of radio-influence voltage on line to field intensity under or near the line.

(*e*). Ratio of radio-influence voltage measured with standard laboratory-test circuit to voltage which will appear on line when apparatus is connected.

(*f*). Effect of service conditions.

Discussion of Co-ordination Factors

(*a*). Broadcast station field intensities depend on the locality and can be measured with the standard radio-noise meter. The magnitudes of the field intensities available as a function of the percentage of radio listeners is beyond the scope of this paper.

(*b*). The signal to noise ratio acceptable to radio listeners is probably in the range of 30 to 40 decibels. What the ratio should be depends on the type of radio program and the environment of the listener. Some background noise can be tolerated in most cases if the program is quite understandable without undue concentration provoked by the background noise.

(*c*). The coupling of antenna to high-voltage line determines the effect on the radio receiver of radio-frequency fields under or near the power circuits. Because the coupling factor varies over a wide range, it is necessary to use distribution curves for the data and to use values of the factor which are significant for the case or area considered. The tests described in this paper were outlined and made with the purpose of obtaining more information on this antenna-power-line coupling factor.

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The authors acknowledge the assistance of H. S. Fitch and A. J. Lois in arranging for the tests and the aid of R. D. Evans in rationalizing the general problem and placing it on a basis suitable for numerical calculation.

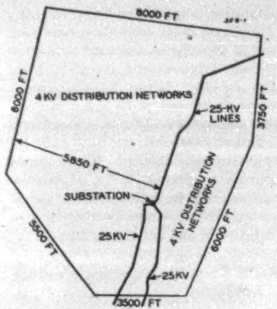

Figure 1. Outline of city in which radio coordination tests were made

Twenty-five-kilovolt lines to substation shown—four-kilovolt distribution spreads over area along with necessary street lighting, d-c feeder and telephone circuits

Twenty-five-kilovolt lines, four-kilovolt distribution, telephone on same poles along lines. D-c feeders also on two lower line sections

Table I. Antenna Characteristics

Type	Length (Feet)	Height (Feet)	Ground	Equivalent Effective Height (Meters)	Feet to Nearest High-Voltage Line	Feet to Low-Voltage Line	Coupling Ratio**
T	42x18	26	Rod	3.6	105	20	0.067
L	50	23	Gas pipe	3	50	12	0.3
L	30	14	Water pipe	0.4	45	21	0.55
L	40	21	Water pipe	3.0	114	10	0.5
L	75	28	Rod	5.0	63	8	0.45
Doublet	25x25	22	Water pipe	1.0	50	24	0.44
L	50	26	Rod	3.6	10	0.16	1.11
L	84	15	*	1.35	60	27	0.72
L	46	22		5.2	66		0.43
L	87	11	Water pipe	3.9	72	25	0.9
Doublets	30-30-12-12	25		0.78	130	8	0.18
L	60	18		13.9	78	25	0.6
L	100	28		1.0	66	20	0.8
Doublet	45x20	33		1.8	24	51	2.5
L	27	35	Water pipe	1.11	150	72	0.8
L	48	15	Water pipe	3.56	30	20	1.0
L	60	22	Water pipe	4.4	84	1	0.053
L	66	22	Water pipe	1.15	75	40	1.1
L	48	20	Water pipe	1.2	85	30	1.4
L	48	20	Water pipe	0.24	52	9	1.71
L (attic)			Water pipe	1.4	108	Home wiring	2.75
L	38	20		0.835	45	10	2.74
Doublet	51x20	29	Water pipe	3.86	75	19	0.21
L indoors					75	Home wiring	1.52
L	36	15	Rod		51	1	0.8
L	66	25	Water pipe	2.66	129	24	0.225
L					110	20	1.0
L					75		1.09
Loop					40		1.00
Averages	53 for *L*'s	22.3		2.83	73	20.7	0.93

* Where no ground was available, measurements were made by using neutral wire of 110-volt circuit as ground connection.

** This is ratio of field intensity at the antenna to the field intensity 30 feet from the nearest high-voltage line.

(*d*). This factor varies with the line height. Values from 55 to 90, average of 70, were measured 30 feet from the line, and under the line wires the values averaged only six per cent lower. In these cases there are 25-kv, 4-kv, and street-light circuits on the same poles, and the field under the line does not decrease with distance from the line so rapidly as it does with single-circuit lines.

(*e*). It is necessary to consider the effect of loading on radio-interference generators. Most generators connected to high-voltage circuits have several times the impedance (600 ohms) used in the standard high-voltage test circuit to load the apparatus. Because of the relatively high internal impedance the radio-frequency voltage which will exist when the apparatus is put in service on a transmission line is directly proportioned to the line impedance. For a line without reflections the load will be one-half the characteristic impedance which varies from 370 to 630 ohms, with an average of 494 ohms as obtained from measurements on several lines made at carrier-current and broadcast frequencies. The average is 494 ohms giving a value of 600/247 = 2.42 for (*e*).

(*f*). The effect of service conditions is not known. Electric apparatus is usually operated near its rating, and no large changes in the radio noise level will occur if ratings are not exceeded. In some cases the location, mounting, and proximity to ungrounded or grounded metal may be such as to change appreciably the electric field in the apparatus and outside of it in such a manner as to change the radio noise level.

The above factors can be expressed in the form of an equation:

$$RIV = \frac{ade}{bcf} \qquad (1)$$

where RIV is the radio-influence voltage referred to the standard high-voltage test circuit

If we use 31.6 (30 decibels) for b, 70 for

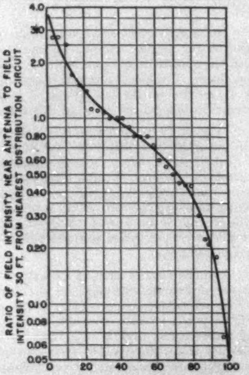

Figure 2. Percentage of total number of antennas tested with ratio (coupling factor) greater than ordinate

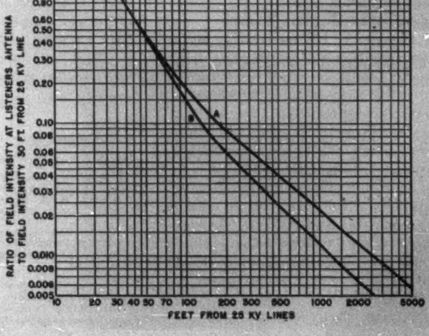

Figure 3. Coupling from high-voltage line to antenna as a function of distance from the line

(*A*). With distribution circuits over the area

(*B*). With no distribution circuits—all transmission through space

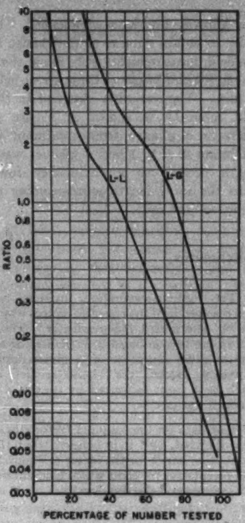

Figure 4. Ratio of line-to-line and line-to-ground voltages on 110-volt circuit to field intensity 30 feet from nearest high-voltage line

d, 2.42 for *e*, and 1 for *f*—all of which are reasonable values—and change nomenclature for broadcast field intensity from *a* to *E*, equation 1 may be written

$$RIV = \frac{5.36E}{c} \text{ microvolts} \qquad (2)$$

In this equation the factor *c* was considered as one requiring field investigation in order that numerical values of *RIV* for satisfactory radio reception could be obtained. Field tests were made therefore in an area as shown by Figure 1. This area has 16,000 people with 4-kv distribution, street-light circuits, street-railway network, and 25-kv lines located as shown.

Test Procedure

A slowly keyed signal of one frequency (980,000 cycles) was applied by means of capacitors to one phase of the 25-kv line at the substation. The location of this substation and 25-kv lines relative to the area inhabited by radio listeners is shown in Figure 1. The signal was of such a magnitude that it could be received, without interference, anywhere in the test region and along all transmission lines including the 132-kv lines feeding the 132-kv to 25-kv substation which is several miles from the test area.

Two standard[2] radio-noise meters and a standard signal generator were used as the measuring equipment. Test locations, that is, listeners' antennas, were selected at random along the 25-kv lines and at points several thousand feet and on both sides of the 25-kv lines. At each listener's antenna, the following 980-kilocycle (test signal) measurements or records were made:

1. Field intensity directly under and 30 feet from 25-kv or distribution lines whichever was nearest. (Thirty feet has been tentatively considered as a standard distance. In most cases it is not necessary to leave the roadway to take readings.)

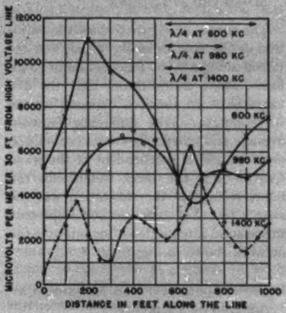

Figure 5. Variation in radio-influence field along same section of line at three frequencies

2. Field intensity at the antenna.

3. Voltage across listener's radio receiver with antenna connected.

4. Voltage across 1,000 ohms resistance—used in place of radio receiver. (This value of load resistance has been used by Department of Transport in Canada, as it makes possible comparison of antennas on a common load basis.)

5. Line-to-line and line-to-ground voltages were measured at the 110-volt 60-cycle outlet near listener's radio receiver. The network (Figure 6 of reference 2) was used for these measurements.

6. Distances from antennas to 25-kv line, to nearest distribution line, and to 110-volt line to house.

7. Several other measurements were made such as field intensity under 132-kv line connected to the same system as the 25-kv line to which test signal was impressed; readings of field intensity at three frequencies along the same section of 25-kv line; field intensity along and at the end of a short length of street light circuit.

Results of Tests

Antenna Characteristics

The data given in Table I are for 29 antennas—all of which are of reasonably good dimensions and good construction. It was found that 45 per cent used water pipe for ground, 28 per cent had no connections to ground, and the rest used ground rods which measured in some cases several hundred ohms with direct current. The average characteristics for the antennas tested are as follows:

Length: 53 feet for *L* types.
Height: 22.3 feet.
Equivalent effective height: 2.83 meters (this was determined by measuring the voltage across 1,000 ohms resistance connected from antenna to ground and the field intensity near the center of the antenna).
Distance to nearest high-voltage line: 73 feet (25 kv and 4 kv).
Distance to low-voltage line: 20.7 feet (110-volt circuits).
Average coupling ratio: 0.93 (ratio of field intensity at the antenna to the field intensity 30 feet from the nearest high-voltage line).

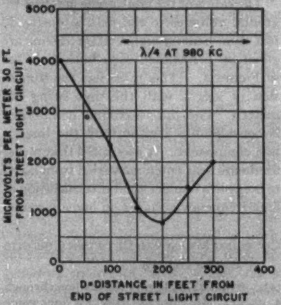

Figure 6. Radio-influence field increase at the end of a circuit

Coupling Ratio—Antenna to Nearest 25-Kv and 4-Kv Lines

The ratio of field intensity at the antenna to the field intensity 30 feet from the nearest power line is shown on Figure 2 as a percentage-distribution curve. These data indicate that the radio-influence fields at the antenna are on the average almost the same as the radio-influence field intensities at the nearest power circuit. There is no simple correlation between these coupling ratios and the distances to either high-voltage or low-voltage circuits.

Effect of Distribution Circuits on Transmission From 25-Kv Lines to Antenna

The field intensity was measured 30 feet from the 25-kv line at several points, and the average of these readings was compared to field intensities measured at the antennas. Curve *A* of Figure 3 shows the attenuation across an area having distribution circuits, and so forth. The loss is 0.0182 decibels average per foot taken from 100 feet to 1,000 feet from the 25-kv line. Curve *B* of Figure 3 is for an area without any other circuits in the vicinity and gives, therefore, the attenuation for space transmission in the vicinity of a transmission line. The attenuation in this case is 0.024 decibels average per foot from 100 to 1,000 feet. Curve *B* was extrapolated after 300 feet, assuming that the field intensity varies inversely as the distance. The effect of distribution circuits is to increase the field intensity up to about twice that obtainable by space transmission alone.

RADIO-FREQUENCY VOLTAGES ON THE 110-VOLT HOME CIRCUITS

The ratios of the line-to-line and line-to-ground voltages to the field intensity 30 feet from nearest high-voltage circuit are given as percentage distribution curves in Figure 4. (This ratio has the dimension of length with the unit being the meter.) The line-to-ground component is considerably greater than the line-to-line component. It was noted that measurements near antennas varied from home to home in a manner similar to variation of high-frequency voltages on home circuits.

COUPLING MEASURED BETWEEN CIRCUITS ON POWER SYSTEM

(*a*). From 25-kv line to 132-kv line through a station. In this case the two lines came to the station at about 90 degrees from each other, and the signal transfer was through the station. The field intensity measured under the 132-kv line was 18.5 per cent of that measured under the 25-kv line.

(*b*). From 25-kv line to 4-kv line and to telephone line. The four-wire four-kilovolt line is five to six feet away from and the telephone line 12 feet below the 25-kv line conductors. The voltage to ground on the four-kilovolt line was 8.2 per cent, and on the telephone line it was 2.4 per cent of the radio-frequency voltage on the 25-kv line.

VARIATION OF RADIO-FREQUENCY FIELD ALONG THE 25-KV LINE

The curves of Figure 5 show the variation in the field intensity which was obtained along a 1,000-foot section of 25-kv line. There is indication that the distance between maxima depends on the wave length. The difference between average and maximum values is about 50 per cent and is independent of the wave length. For the three frequencies the input voltage at the substation was the same, so that the curves represent the relative attenuation of signals over the network shown in Figure 1.

RADIO-FREQUENCY FIELD MEASURED AT THE END OF A CIRCUIT

It was observed during preliminary tests made with a receiver located in a car that the signal level increased whenever the end of a street-light circuit or of a four-kilovolt line was approached. In order to obtain data as to the change in the field, measurements were made along and to the end of one of these street-light circuits. The variation obtained in the field intensity is shown in Figure 6. There is a change from minimum to maximum of 5 to 1 and a quarter wave length occurs at approximately four-fifths the wave length corresponding to the velocity of light.

Conclusions

1. The radio-co-ordination problem has been resolved into the essential factors which can be determined independently. These factors can be combined to estimate the effect on radio reception of a given radio-frequency source, the characteristics of which have been measured by means of a standard test circuit.

2. Test data on the coupling factor *c* have been obtained, and it is believed that the distribution curve for the factor *c*, Figure 2, is representative for cities which have similar high-voltage and distribution lines.

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Application of Lightning Protective Devices in Wartime

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I. Introduction

UNDER present wartime conditions the need for conserving critical materials has been impressed upon the electrical industry with emphasis. It is for this reason that the lightning arrester subcommittee of the protective devices committee undertook a review of lightning protective devices and practices and their application to uncover and present any worth-while savings which it seems possible, practical, and reasonable to obtain. Within the short time since this project became active, it has not been possible to obtain committee agreement on all the various considerations involved. Therefore, rather than withhold from the industry the value of constructive study and suggestions already available, this paper is being presented without the sponsorship or any commitment by the lightning arrester subcommittee. It is hoped that its presentation and the discussions it will initiate will be helpful to the committee in continuing its work on this project to a conclusion. It is the purpose of the paper to give some results of a study on this problem up to the time the paper was prepared.

It should be pointed out clearly, however, that it is not the intent of this paper to imply that the suggested methods of protection and practices are suitable for universal application. Neither is it the intention to force the industry to use any of the suggested expedients where the particular conditions encountered are not suitable.

It is solely the intent of this paper to cover from a broad point of view factors in lightning-arrester protection which, if applied on an engineering basis, should result in some worth-while savings, both direct and indirect, in critical materials without lowering appreciably past standards of protection. In most cases, the protection should actually be improved.

II. Method of Approach

In undertaking this study, it was felt at first that the savings possible would be of limited extent. However, a further consideration indicated that, while an actual saving of critical metals was possible by applying lower-rated arresters where feasible, or by revamping old arresters, or reducing the length of connections and substituting other materials, there were extensive savings possible in the equipment to which the protective device was applied.

Carrying the analysis somewhat further, it was believed that a careful study of the protective problems of individual application would result in savings of both material and man hours for old installations already in service as well as for new installations being planned. At first thought, it might be assumed that the use of substitute materials for those now considered critical and the suggested reduction in arrester ratings in certain applications might introduce a greater hazard to the system as a whole. A more careful analysis of the situation, however, seemed to indicate that, in most cases, the over-all hazard to the system would be considerably reduced, although it might be expected that occasional failures of the protective devices themselves might result from the rebuilding of old equipment and the use of smaller arresters (line-type instead of station-type) and reduction of arrester rating.

The reports of some companies who had already put into use some of the methods discussed here indicated that satisfactory results were obtained. In many cases such changes from old practices had been made prior to the necessity for conserving scarce materials. The author, therefore, decided at this time to present and discuss the various proposed methods of conserving scarce materials and saving in man hours which are outlined below, since experience, limited in some cases, has been obtained already on some of the methods under service conditions. It is hoped that this paper will stimulate further interest and activity in the application of surge protective devices in a way that will save valuable critical material and man hours for the war effort.

III. Discussion of Proposed Methods of Protection

The work in recent years in getting a clearer picture of insulation strength of equipment and the protection afforded by protective devices has been presented and discussed quite thoroughly in the past and therefore will not be presented here. The lightning arrester and similar circuit-clearing devices have a very definite maximum voltage (60-cycle) rating which must not be exceeded in service without the possibility of failure of the device. In the past, many arresters have been applied to a system merely by matching the arrester rating with the system voltage without giving consideration to the actual voltage which might appear across the arrester in service. This is particularly true of solidly grounded neutral systems.

1. Reduced-Rated Arresters

The first consideration, therefore, is that of applying arresters on the basis of the highest system, or 60-cycle, voltage expected across their terminals in service. If this principle is adhered to, it is possible on many grounded neutral systems, at least, to use an arrester having a rating somewhat below, and in some cases considerably below, the line-to-line rating of the system. In the past, the entire field was covered by the general rule that on a grounded neutral system, the arrester would have a 60-cycle rating of approximately 80 per cent of line-to-line voltage. While this rule seemed to work out satisfactorily in most cases from an operating point of view, many cases exist today where an arrester of lower rating than one selected by this rule can be and has been used successfully. On a 132-kv nominal system, for example, the preferred rating for apparatus is 5 per cent higher or 138 kv, and the rating for arresters is 5 per cent above this value, or 144 kv. If the system neutral is grounded however, the industry recommendation for the arrester is 80 per cent of 144 or—the nearest standard arrester—121 kv. Thus, the use of the so-called grounded neutral arrester, instead of the full-rated arrester, on a system the neutral of which is grounded, results in a saving of approximately 20 per cent of the arrester material and cost, with considerably better protection provided. In carrying the analysis a step further, a calculation of the system overvoltage for each arrester application is made by our company, which showed in practically all cases that the overvoltage to ground does not exceed 109 kv. This results in a further

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decrease of 10 per cent in the materials and cost of the arrester. Three large companies have reported that they have been adopting this general practice of using the so-called reduced-rated arresters for grounded neutral systems for a considerable period of time with completely satisfactory results. The practice can be applied to both new installations and revamped old installations with direct savings of material in both cases.

This application of so-called "reduced-rating" arresters on solidly grounded neutral systems on the basis of the calculated 60-cycle system voltage under fault conditions instead of on the customary 80 per cent rule has resulted in savings other than in the arrester alone. It has been found possible in some cases to use equipment in the next lower voltage class, thereby using less critical material in the equipment itself.

2. LINE-TYPE IN PLACE OF STATION-TYPE ARRESTERS

A second consideration relates to the use of line-type arresters in the place of station-type in certain locations and under some conditions. Several savings are possible here:

1. In the amount of material required for the smaller line-type arresters.
2. In the possibility of less structural work for supporting the arrester.

It should be appreciated, of course, that the line-type arrester has a higher voltage level of protection than a station-type, and for this reason this application may not be desirable in some cases. On the other hand, where new equipment is involved, it is often possible to use the line-type arrester instead of the station-type and still obtain adequate protection. Four large users of arresters have reported the successful application of line-type arresters in place of station-type under some conditions.

3. PROTECTING EQUIPMENT NOT ALREADY PROTECTED

It might seem at first that the recommendation to add an arrester or similar protective device to equipment not now so protected is a "spending" of critical materials rather than a "saving." The broad point of view, however, is that the protection of equipment, subject to frequent hazards of overvoltage, may save that equipment from failure. It thus obviates the necessity of using critical materials for replacement of the damaged apparatus and further saves the man hours involved in building new replacement equipment or repairing the damage, as well as the man hours involved in replacement in the field.

It is not intended to imply that extensive and elaborate protective schemes be added to many pieces of equipment or in many situations where it is felt that past experience has given satisfactory performance, but a careful analysis of many situations in most companies will reveal cases where it is believed a thorough study and application of protective devices will yield considerable over-all benefits.

Cables, for example, subject to lightning require a large amount of critical material which well might be saved by suitable protection. Likewise, the protection of rotating equipment such as generators and large motors, subject to transient overvoltages, well may be given careful study, since the failure of such a piece of equipment may result in consequences more important than even the saving of critical materials. For example, the loss of generating capacity over a period of time or interruption to service to important war industry loads, although not "critical materials," is in a sense as important, if not more so.

4. REBUILDING AND REVAMPING OLD ARRESTERS

A fourth factor to be considered is the savings of materials made possible by rebuilding or revamping old-type arresters. Several companies have reported that they have been doing work along these lines for some time. In the case of oxide film arresters which were extensively used in the past, it is general practice in a number of companies to reduce the number of cells in existing arresters, which, in effect, means reducing the arrester rating to match more nearly overvoltage conditions of the circuit where they are used. The principle involved has been discussed in the preceding paragraphs. Likewise, the changing of four-leg oxide film arresters to the three-leg type has been an accepted practice in our own company for some time, with complete success from the viewpoint of added protection and absence of arrester failure. Of course, in such cases the 60-cycle maximum rating of the remaining three legs must be checked against the system overvoltage under fault conditions. In some cases where the three-leg 60 cycle rating was too low, part of the fourth leg was left in service to supply the deficiency.

Successful rebuilding of mica-spaced and open-gap autovalve arresters has also been reported. The rebuilding of old arresters supplies added protection to equipment, where they are installed by lowering the protecting level, and also may make available some of the material removed for other arrester locations or spare parts. The rebuilding of even obsolete arresters may be warranted in some cases, if new arresters are not available. The added protection of rebuilding obsolete arresters may be justified in some cases, although the protective level may be higher than would be desired ordinarily.

5. PROTECTIVE DEVICES OTHER THAN ARRESTERS

While the lightning arrester has been mentioned here quite extensively, it is recognized that there are other protective devices such as the protector tube, open-air gap, and control gap which also take their place in the matter of conserving materials. The decision as to which device to select is one of individual application and protection desired. It is believed that the characteristics of all of these devices are available or can be obtained with sufficient accuracy for purposes of application. All of these devices have been reported as having been used with satisfactory results. Whether to use one device in preference to the other in an attempt to save critical materials will probably depend in most cases on the immediate protective problem at hand and the availability of the device.

6. CONNECTING LEADS BETWEEN ARRESTER AND EQUIPMENT

A sixth consideration is the conducting material, usually copper, which can be saved by eliminating the ground leads from the ground end of arresters to ground. Where arresters such as station type or arresters mounted on the well-grounded structural steel of stations or steel poles are involved, the elimination in new designs or the removal in old designs of direct ground-wire leads can be accomplished without impairing the protective value of the arrester. The removal of such existing ground leads may not be highly practical, but in new installations it most certainly seems justified as a wartime expedient.

7. CRITICAL MATERIALS IN ARRESTER MANUFACTURE

Inherently, an arrester or other protective device of this type has a relatively small amount of critical material, that is, copper, iron, and aluminum. In one case at least, aluminum supporting castings have been replaced by galvanized iron. It has been suggested that the internal mechanism of the sealed gap element in arresters could be made in whole or in part of iron in the place of brass, but since both materials are so-called "critical," the change in arrester design would be predicated probably on which may become most critical and in the arc-interrupting characteristics of a design using iron. In the design of devices, therefore, it does not appear that there is room for any appreciable saving in critical metals, and probably the manufacturers have gone about as far as they reasonably can under present conditions.

8. PROXIMITY OF ARRESTER TO EQUIPMENT

An eighth consideration relates to the location of the arrester or protective device close to the equipment to be protected. This is now recognized as fundamental in applying overvoltage protective devices. By locating arresters this way and even relocating those now in service in the field, it is possible in some cases to save copper conductor at the ground and line leads and, at the same time, improve protection to the equipment. It is well known that under some conditions long leads in the arrester circuit permit the protective level at the equipment to rise appreciably above the level at the arrester terminals.

Cases where this proposal can be applied are at cable terminals where the arrester should be located directly across the pothead terminal rather than have a separate ground lead extending to ground, provided, of course, that the cable sheath is well grounded at the arrester location. Another case involves the location of distribution arresters one pole distant from isolated transformers or transformer banks. Such methods of attempted protection are considered of limited value in exposed locations where a long arrester ground lead is used and there is in addition a long line lead between the arrester and the transformer. In recent years the practice has been followed by several companies of locating arresters directly on equipment such as transformers, the transformer tank being provided with a supporting bracket on the tank. Arresters likewise have in some cases been mounted on the transformer covers. This suggestion can be applied both to new arrester installations and to many existing ones.

9. SHIELDING STATIONS

Another consideration applies to protection at stations where considerable electrical equipment is located and the equipment is built up of a large amount of critical material and will require replacement or repair in kind, as well as requiring a large amount of man hours and possible loss of factory output, if it should fail in service. The adequate protection of such stations seems almost a necessity. One way of greatly adding to the protection is by shielding the station as a whole, in addition to applying arrester protection. While shielding requires some critical materials such as steel, copper, or combinations thereof, it is suggested that careful consideration be given to such shielding in the light of relative importance of the equipment contained in the station. It is not recommended that all stations be shielded, but there are undoubtedly some which can initially use critical materials such as shielding wires with considerable, overall saving from a long range point of view.

10. TESTS AND MAINTENANCE OF ARRESTERS

The last subject considered here centers around the idea of keeping in the best possible operating condition equipment which we now have. This means, with the lightning arrester, a high degree of careful and planned maintenance. So far as the arrester is concerned, it is suggested that special consideration be given to testing arresters wherever possible and feasible, to insure that they are in the best of condition to perform their expected functions. Several years ago recommendations were made by the AIEE lightning arrester subcommittee on methods of testing some types of arresters. Since that time, a large number of arresters have been tested, and other methods of test or refinements have been used. It cannot be urged too strongly that a special attempt should be made to keep the arresters in the best possible condition.

IV. Summary of Benefits and Savings

That there are possible savings in critical material and man hours in the more careful study and application of protective devices already installed on the existing equipment and new applications seems evident from the preceding discussion.

A. The application of arresters on the basis of their rating, co-ordinated with the overvoltage of the system, will reduce to a slight degree the number of arrester units required on new work and make available some arrester units now in service for spares or new installations.

B. By closer application of arresters on the basis of their voltage rating, better protection will be provided to the protected equipment, and this should result in fewer equipment failures and, consequently, less need for critical materials and a saving in man hours used for renewal.

C. The use of lower-rated arresters also will result in lower insulation requirements of equipment such as transformers, for example, with consequent saving in steel in the containing tanks and core and some copper in bushing studs and winding.

D. The use of line-type arresters instead of station-type, where expedient, results in a direct savings of materials in the arresters themselves, as well as in possible savings in steel-mounting members and structures.

E. Applying protection on equipment not already protected can result in indirect savings in replacement of equipment which otherwise might become faulty if not protected.

F. Rebuilding of old arresters provides greater protection to existing equipment and eliminates the requirement of critical material for new modern arresters.

G. The omission and removal of ground leads from arresters already connected to well-grounded metal structures provide critical material for scrap or other uses.

H. A well-planned shielding system for exposed stations, although initially requiring critical material, may be more than compensated for by the prevention of equipment failure in such stations.

I. Testing of existing equipment to keep it in first-class operating condition likewise may reduce to a large extent the requirements for new equipment for replacement of protective devices and equipment which may become faulty in service.

It should be kept in mind that, in applying some of these suggestions, the failure rate on the protective devices themselves may increase, but it is believed that, if the conditions of each situation are considered carefully, and action taken on an engineering basis, the net over-all effect will be of considerable benefit to the war effort.

The Auto-Blast Interrupter Switch

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Synopsis: An air interrupter switch may be defined as a nonautomatic air switch which combines the functions of a disconnecting switch with the ability to interrupt current up to a predetermined magnitude. It primarily differs in function from a circuit breaker in that it cannot interrupt overload or short-circuit currents. General specifications for interrupter switches are proposed.

The Auto-Blast interrupter switch is an air interrupter switch having each pole equipped with an arc chute into which the arc is driven by a blast of air generated automatically when the switch is opened. Interruption occurs within the chute by elongating and cooling the arc. The construction and operation of this switch is described, and test data are presented to show compliance with the proposed general specifications.

WHILE power circuit breakers are preferred for switching unit-substation and secondary-network transformers, it has been shown[1] that there is a definite economic field for metal-enclosed interrupter switches for these applications. The requirement for these switches exists because of the development of distribution systems having transformer capacity divided into a number of small banks and protected by a common-feeder circuit breaker. Economical switching provisions are necessary to provide means for disconnecting an individual transformer from a primary feeder without deenergizing the feeder.

Since an interrupter switch cannot open short-circuit current and a short circuit may occur at any time, there is always a certain degree of hazard associated with the operation of such switches. The probability of the occurrence of a short circuit in the transformer at the instant a switch is opened is remote, but the failure of a switch to interrupt the arc, for any reason, will probably result in a short circuit within the metal enclosure. Much can be done to minimize this possibility by giving due consideration to adequate design and tests. Good engineering judgment dictates the following general specifications for interrupter switches:

1. Long operating life with a minimum of maintenance.
2. Ability to interrupt current, within its interrupting rating, without arc or flame outside of the interrupting element.
3. A sufficient number of tests to demonstrate adequate safety factors in voltage, current, and operating life.
4. Speed of opening independent of operator.
5. Positive operation to prevent failure caused by partial closing and reopening.
6. An insulation level consistent with that of other apparatus in the circuit.

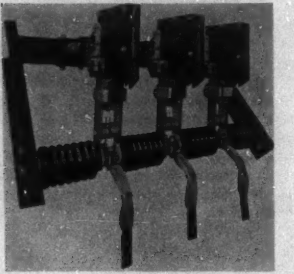

Figure 1. The Auto-Blast interrupter switch. 15 kv, 600 amperes continuous

Impulse insulation level 110 kv. Metal enclosure and operating handle removed

Table A. Three-Phase Reactance Load Interrupting Tests

Circuit Diagram Figure 6a

Tests	*Line-to-Line Recovery Voltage (Kilovolts)	**Test Current (RMS Amperes)	Arcing Time (Half-Cycles)			Muffler	Remarks
			Minimum	Maximum	Average		
1 to 45	14.5	4–8	0.6	1.0	0.8	Off	No disturbance—cleared
46 to 49	15.0	4–6	0.7	1.0	0.9		
50 to 92	14.5	11–12	0.5	1.0	0.7		
93 to 96	15.0	11–13	0.2	2.0	0.9		
97 to 139	14.5	20–21	1.0	1.0	1.0		
140 to 144	15.0	20–23	0.5	1.5	1.0		
145 to 186	14.5	39–48	No oscillograms				
187 to 199	15.0	40–50	0.3	1.9	1.0		
200 to 243	14.5	88–125	No oscillograms			On	
244 to 248	15.0	91–129	0.3	2.0	1.3		
249 to 253	14.5	165–172	1.1	1.5	1.3		
254 to 255	15.0	171–178	0.9	2.8	1.5		

* Average three-phase voltage.

** Two values indicate minimum and maximum current interrupted for each series of tests.

Table B. Single-Phase Reactance Load Interrupting Tests

Reactance Load—Circuit Diagram Figure 6b

Tests	Line-to-Line Recovery Voltage (Kilovolts)	**Test Current (RMS Amperes)	Arcing Time (Half-Cycles)			Muffler	Remarks
			Minimum	Maximum	Average		
256 to 258	5.2	69			1.1	Off	No disturbance—cleared
259	5.0	93			1.7		
260	5.1	113			1.1		
261	5.1	148			1.1		
262	4.9	165			1.0		
263	4.9	196			1.0		
264	4.6	220			1.4		
265	4.8	240			1.0		
266	7.5	98			1.1		
267	14.0	55			1.1		
268 to 270	14.0	54–55			1.0		
271 to 276	15.0	26–27	0.3	1.0	0.7		
277 to 282	15.0	56–58	0.8	1.1	0.9		
283	16.0	27			1.0		
284	16.0	39			0.8		
285	16.0	52			1.1		
286	17.0	29			1.0		
287 to 288	18.0	31			0.9		
289	19.0	32			0.8		
290 to 295	20.0	33–34	0.2	0.9	0.6		
296	22.0	36			1.0		
297	4.9	250			1.8	On	
298	4.7	270			1.8		
299	4.5	290			1.9		

** Two values indicate minimum and maximum currents interrupted for each series of tests.

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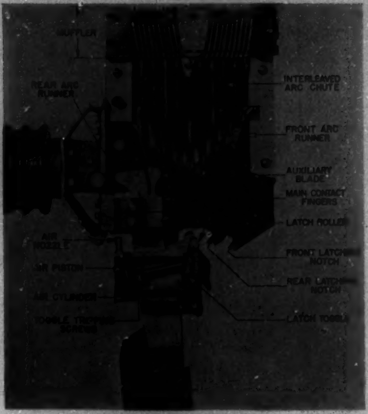
Figure 2. Cutaway views of Auto-Blast interrupter switch showing operation



(a—left). Main and auxiliary blades closed and piston at end of exhaust stroke

(b—right). Opening operation. Main blade partially open, auxiliary blade closed and piston at end of intake stroke

(c). Opening operation. Main blade partially open, auxiliary blade closed, with latch tripped and piston starting exhaust stroke

(d). Opening operation. Main and auxiliary blade partially open, latch reset and piston at end of exhaust stroke

(e). Closing operation. Main and auxiliary blade partially closed, toggle latch depressed

The Auto-Blast interrupter switch described in this paper is a new device which is intended primarily for application in a metal enclosure on the high-voltage side of unit substations and secondary-network transformers.

Construction and Operation

The Auto-Blast interrupter switch, Figure 1, is essentially a triple-pole group-operated device with three moving elements carried on a common rotatable operating shaft. The switch is arranged for mounting in a metal enclosure and for operating by means of a handle (not shown), keyed to either end of the rotatable shaft and located outside of the enclosure. In an opening operation the main switch blade opens first and, after a predetermined movement, trips a latch which releases the spring-actuated auxiliary blade. The auxiliary blade then snaps open, and the resultant arc is forced into the chute by a blast of air from a cylinder on the auxiliary blade. Figure 2 shows the construction of the switch and the relationship of the parts during successive steps of operation.

Each moving element of the switch has a pair of spaced main blades rigidly fastened to the operating-shaft insulators. An auxiliary blade and latch assembly is carried between and pivoted on the main blades at a point near the operating shaft. An air cylinder and nozzle form an integral part of the auxiliary blade, and a coacting air piston is attached to the main blades. A torsion spring normally maintains a parallel relationship between the main and auxiliary blades in which position the piston is at the exhaust end of its stroke.

Each stationary element of the switch consists of a contact and an arc chute assembly mounted on a fixed insulator. The contact assembly is made up of two spaced main contacts outside of the chute

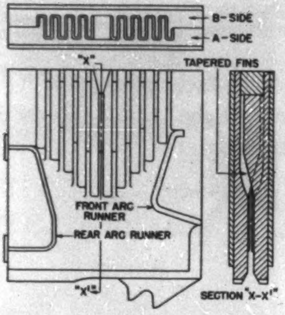


Figure 3. Interleaved arc chute

Side view with *A* side removed

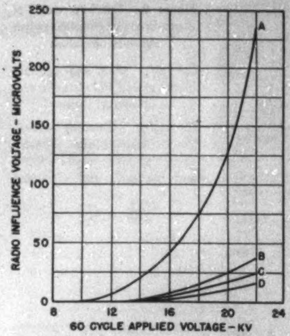


Figure 5. Radio-influence voltage tests on the Auto-Blast interrupter switch

	Impulse Level (Kilovolts)
A	60
B	75
C	95
D	110

and two arc runners centrally located inside the chute. The arc-chute assembly includes an interrupting element, with or without a muffler, and means for latching the auxiliary blade in the closed-circuit position during the first part of the opening stroke of the main blade.

In the closed position, Figure 2a, the main and auxiliary blades are in parallel, and the air piston is at the exhaust end of its exhaust stroke. When the operating handle is moved from the closed position, the main blade starts the opening travel, the auxiliary-blade latch engages the rear notch in the side plates of the chute assembly and holds the auxiliary blade in contact. As the main blade continues to move away from the fixed contact, the piston starts its intake stroke, and the auxiliary blade and air cylinder remain stationary until the switch reaches the position shown in Figure 2b. At this point the piston strikes and trips the toggle of the latch, Figure 2c, and the torsion spring snaps the auxiliary blade back toward the main blade and drives the cylinder over the piston forcing a blast of air from the nozzle into the arc chute. As the cylinder approaches the end of the exhaust stroke, the latch resets, the piston diaphragm covers the nozzle opening and traps air in the end of the cylinder, thereby cushioning the shock, Figure 2d. Both blades then move together to the full 90-degree open position.

In a normal closing operation, as the operating handle is moved from the open position toward the closed position, both blades and the toggle latch assembly move together until the latch is depressed, Figure 2e, by the cam surfaces formed on the arc-chute side plates. The latch roller then successively engages the front end and the rear latching notches as the switch is moved to the closed position, Figure 2a. The location of the front latching notch is such that it is engaged by the latch roller before an arc can be established during a closing stroke. This feature prevents the possibility of failure caused by partial closing and reopening.

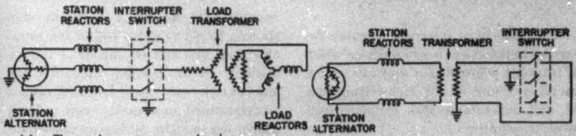
The interleaved arc chute, Figure 3, is similar to the chute of the Magne-Blast air circuit breaker.[3] It is formed by tapered fins alternating from either arc chute wall. The material of the chute is treated especially to be arc resistant and essentially nongas producing and nonexpendable. The entire arc-chute assembly is easily removable for inspection of contacts, without disturbing the adjustment of either the chute or the switch parts.

The arc is drawn in a narrow slot between the auxiliary-blade tip and the rear arc runner. As it is forced up the runners and into the chute, it progressively lengthens and encounters an increasing cooling effect as its path becomes longer. The maximum current that can be interrupted with no disturbance external to the interrupting element is a function of the size of the arc chute and of the pressure and volume of the air blast. When interrupting relatively low currents, such as transformer exciting current, the arc and incandescent gases are entirely confined within the short simple chute of Figure 3. With increased current, as indicated in the accompanying tables, the air blast tends to drive the incandescent

Figure 4. Dimensions of metal-enclosed interrupter switch mounted on transformer

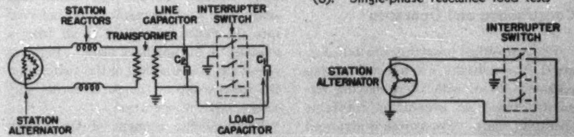
Impulse Level (Kilovolts)	A Width (Inches)	B Depth (Inches)	C Height (Inches)
60	28	27	43
75	35	31	43
95	38	32	44
110	43	34	45

Figure 6. Test circuit diagrams



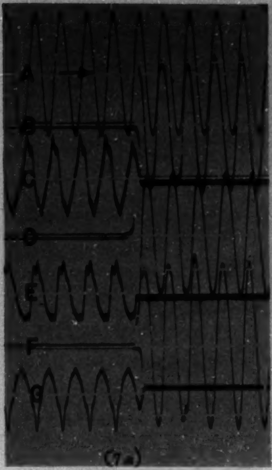
(a). Three-phase reactance load tests

(b). Single-phase reactance load tests

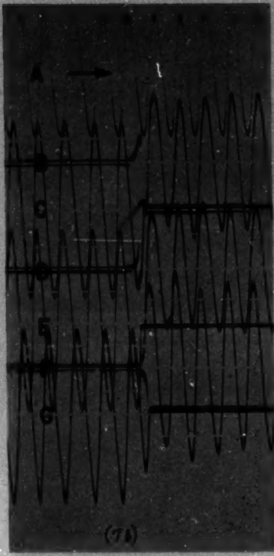


(c). Single-phase capacitance load tests

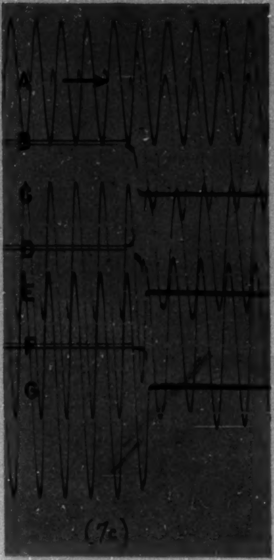
(d). Single-phase short-circuit tests



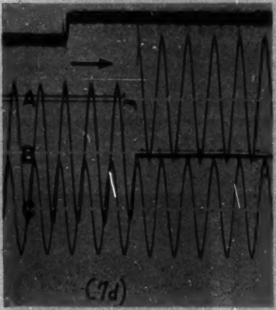
(a). Test 47
A—Timing wave
B—Voltage across switch—line 1
C—Current through switch—line 1
D—Voltage across switch—line 2
E—Current through switch—line 2
F—Voltage across switch—line 3
G—Current through switch—line 3



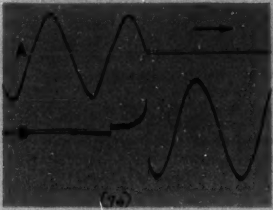
(b). Test 189
A to *G* same as (a)



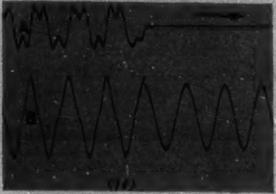
(c). Test 254
A to *G* same as (a)



(d). Test 296
A—Voltage across switch
B—Current through switch
C—Timing wave



(e). Test 299
A—Current through switch
B—Voltage across switch



(f). Test 322
A—Current through switch
B—Voltage across switch and capacitor

Figure 7. Typical oscillograms

Refer to Tables A, B, and C for data

arc gases through the short chute before adequate cooling can take place. The interruption of relatively high currents can be successfully accomplished in the same arc chute by the addition of a suitable muffler to cool further the arc gases.

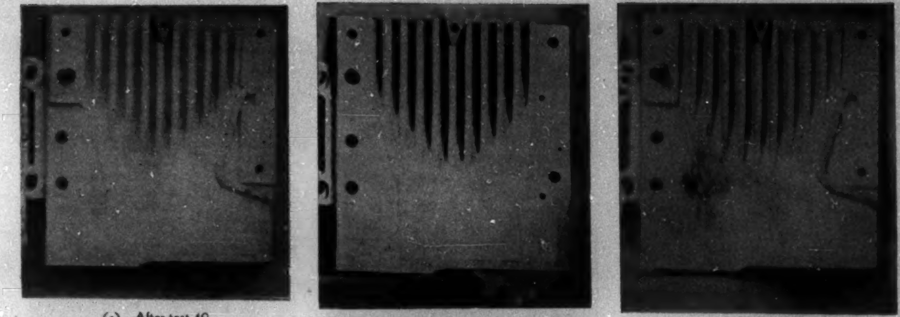
The combination of a small basic arc chute for low current and an easily attachable muffler for higher current provides the most economical arrangement for handling the complete range of current under consideration. It further provides a convenient means of increasing the interrupting ability of existing devices originally intended for the interruption of transformer exciting current only.

Since space is always at a premium, it is important that the dimensions of the switch and its enclosure be maintained at the minimum commensurate with adequate insulation levels. The required impulse-withstand test voltage is the major factor in determining the over-all dimensions of the Auto-Blast interrupter-switch enclosure shown in Figure 4. Dimensions of such magnitude must, of necessity, accompany the degree of insulation obtained.

Tests

The Auto-Blast interrupter switch, mounted in a metal enclosure, has been tested thoroughly to check its conformance with the design specifications.

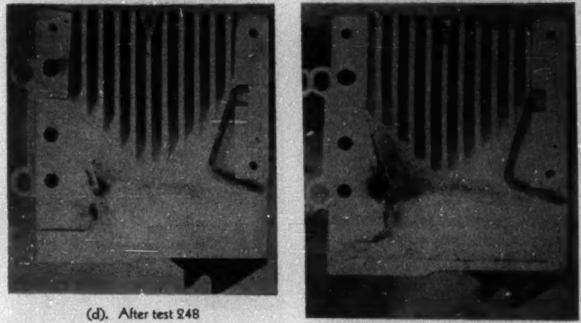
Radio-influence tests in both the open and closed positions of the switch with the metal enclosure grounded, Figure 5, show that the radio-influence levels of the interrupter switch for all classes of insula-



(a). After test 49

(b). After test 144

(c). After test 199



(d). After test 248

(e). After test 255

Figure 8. Arc chutes after three-phase interrupting tests

tion meet the limits established in the National Electrical Manufacturers Association Standards.[3]

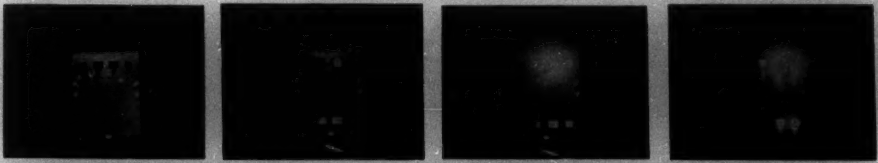
Dielectric tests, both 60-cycle and impulse, made on the switch in the open and in the closed positions with the metal enclosure grounded, determined the insulation required and the sizes of the metal enclosures, Figure 4. These tests were made in accordance with the AIEE Standard[4] for air switches.

Interrupting tests were conducted over a two-year period to investigate a number of basic methods of interrupting transformer exciting current and load current. More than 5,000 tests showed the Auto-Blast design to be by far the most outstanding. A few of the results are shown in Tables A, B, and C with typical magnetic oscillograms in Figure 7.

Various circuits were used to cover the complete range of current and voltage. To simulate in test the most severe service conditions, a value of 20 per cent power factor was established as the maximum at which interrupting tests should be made. However, all of the reactance-

Figure 9 (below). Test 326

Attempting to open short circuit of 30,000 amperes initial, 16,400 amperes final at 14.5 kv. Arc duration 23 cycles



(a). Before test

(b). Switch starts to open

(c). After one cycle of arc

(d). After four cycles of arc

(e). After nine cyles of arc

(f). After 17 cycles of arc

(g). After 19 cycles of arc

(h). After test

Figure 10. Switch parts after test 326

Table C. Single-Phase Capacitance Load Interrupting Tests

Single-Phase Capacitance Load Tests—Circuit Diagram Figure 6c

Tests	Line-to-Line Recovery Voltage (Kilovolts)	**Test Current (RMS Amperes)	Line-to-Ground Capacitance (Microfarads)		Muffler	Remarks
			C_1	C_2		
300 to 307	15.0	26–33	4.4	None	Off	No disturbance—cleared
308 to 311	15.0	29	4.4	4.5		
312 to 314	15.0	57	8.8	None		
315	15.0	57	8.8	8.4		
316 to 320	15.0	57	8.8	8.4	On	
321 to 324	15.0	115–120	17.6	None		

** Two values indicate minimum and maximum currents interrupted for each series of tests.

load interrupting tests were made with the current limited almost entirely by reactance, and the power factor in all cases approached zero.

The entire series of three-phase reactance-load interrupting tests in Table A was made on one set of arc chutes and without maintenance of any kind on the switch or chutes. After each group of tests, the switch was examined, the arc chutes removed, dismantled, and photographed. The only evidence of change during the entire series of tests was a slight pitting of the arc runners and a discoloration of the arc chutes by a nonconducting oxide deposit, Figure 8. After tests the chutes were in excellent condition and were obviously suitable for at least 1,000 successful operations.

The single-phase reactance-load interrupting tests, Table B, demonstrate that the 15-kv switch, without a muffler, performs successfully at voltages up to 22 kv across a single pole to ground. The single-phase capacitance-load interrupting tests in Table C, made with and without capacitance on the line side of the switch,[5] indicate the possibility of switching capacitor banks with the Auto-Blast interrupter switch.

Short-circuit tests at 14.5 kv, Table D, were made with the switch in the circuit near the generator terminals. The switch with manual mechanism was closed with a fast positive operation against a fault current of 31,000 amperes. The switch parts were damaged, but all resulting disturbance was confined within the metal enclosure. An opening test under approximately the same short-circuit current conditions produced a very severe disturbance as shown by high-speed motion pictures, selected frames of which are reproduced as Figure 9. Flame, hot metal, and gas were blown through the ventilating louvres and through the joint between the cover and the housing. The switch parts were badly damaged as shown in Figure 10, and most parts would have required replacement before the switch could have been put back in service.

Conclusions

1. Interrupter switches in general should be liberally designed and rigorously tested, since any failure in normal operation probably will result in a short circuit.

2. The Auto-Blast interrupter switch fully meets all modern requirements for high insulation level, low radio-influence voltage, and adequate safety factor in interrupting ability for the service intended.

3. The interrupting element of the switch is arc resisting, essentially nongas producing and nonexpendable, thereby providing long operating life with minimum maintenance.

4. The performance of the switch is consistently uniform with an arcing time in the order of one to two half-cycles at all currents within its interrupting rating.

5. It has been demonstrated that the switch, when closed with a fast positive operation, has mechanical and thermal strength to withstand closing against short-circuit current not exceeding the short-time current rating of the switch with disturbance and damage confined within the metal enclosure.

Table D. Single-Phase Short-Circuit Tests—Circuit Diagram Figure 6d

Test Number	Line-to-Line Test Voltage (Kilovolts)	Test Current (RMS Amperes)	Operation	Remarks
325	14.5	31,000	Close	No disturbance outside of housing except slight trace of smoke from louvres
326	14.5	30,000 initial 16,400 final	Open	Loud report with much smoke, flame, and hot metal from housing. Several bolts from lower edge of cover blown off

References

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Harmonics and Load Balance of Multiphase Rectifiers

Considerations in the Selection of the Number of Rectifier Phases

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FELLOW AIEE

Synopsis: This paper analyzes the operating characteristics of multiphase power rectifiers from the standpoint of harmonics, load balance, and their interrelation, and their effects on the apparatus and circuits to which they may be connected. These factors are considered in connection with selection of the number of rectifier phases, which varies from six (four for very small sizes) to combinations of six-phase units to form a much larger number, such as 36, 72, or even 108 phases. More specifically, the problems of harmonics, load balance, and number of phases are considered from the standpoint of the effects on

1. **Apparatus, particularly turbine generators and capacitors.**
2. **The wave-shape distortion on circuits that may be important in inductive co-ordination.**
3. **The possibility of resonance being encountered in the supply system to amplify one or more of the rectifier harmonics.**

These problems have increased greatly because of the great number and large size of rectifier installations made in connection with the war.

Determination of Harmonics in the A-C Supply Circuits

It is a well-known property of rectifiers and inverters that they produce distortion of both voltage and current waves which appear on both the d-c load circuits and a-c supply circuits, even though the latter contains no generated harmonics. The important harmonics of rectifier installations for all except traction application are restricted normally to those in the a-c supply and are the only ones here considered.

The harmonics in the supply system resulting from the operation of balanced rectifiers or inverters include the series $(pn+1)$ and $(pn-1)$, where p is the number of rectifier phases and n is a series of integers. For power applications the number of rectifier phases is usually limited to six or multiples thereof. Thus, the harmonics of a six-phase rectifier are the 5th, 7th, 11th, 13th, 17th, 19th, and so forth; the harmonics of a 36-phase rectifier are the 35th, 37th, 71st, 73d, and so forth. Actually, rectifier circuits are not perfectly symmetrical or balanced so that some harmonics, not characteristic of the total number of rectifier phases, may be present in reduced magnitude. Thus, if a rectifier of 36 phases is operating with one six-phase unit out of service, the 5th, 7th, 11th, 13th, 17th, 19th, and so forth, harmonics will appear in the supply as well as the 35th, 37th, and so forth, harmonics which are characteristic of a balanced 36-phase installation.

Methods are available for the calculation of harmonics in the supply circuit to a rectifier.[1–3] The theoretical methods[2,3] are based on Fourier analyses, assuming that the inductance of the d-c load circuit is very high and that the impedance of the supply circuit has a linear frequency-reactance characteristic. One method[3] takes into account the loading of the rectifier, the reactance of the supply, and the amount of voltage reduction by phase control. This method gives excellent results when the assumptions upon which it is based are approached. Extensions of this method are also described[3] for application to cases where the supply circuit does not have a linear frequency-reactance characteristic. These extensions are based on finding an equivalent 60-cycle supply reactance corresponding to the actual harmonic reactance of the supply.

Heating of Generators

Generators supplying rectifiers are subjected to increased heating because of harmonics, particularly local heating in rotors. The heating problem is more important for turbine generators than for salient-pole machines, being particularly less for machines provided with damper windings. The importance of this problem arises principally in connection with generators that are loaded fully with six-phase rectifiers and is of much less importance with 12-phase rectifiers. Conversely, the problem is of negligible importance for rectifiers of more than 12 phases and for conditions where the rectifiers are a small part of the total load. A favorable circumstance in the generator heating problem arises from the fact that usually the rectifier load is of higher power factor, 90 to 95 per cent, than generator rating, 80 per cent power factor. This condition reduces excitation loss and thus provides a margin for increased loss from harmonic currents.

Heating of Capacitors

Commercial capacitors are designed to carry in addition to the rated kilovolt-amperes at fundamental frequency, 35 per cent additional kilovolt-amperes to provide for overvoltage at fundamental frequency or for harmonics. The total kilovolt-amperes of 135 per cent of rating may be made up of fundamental and harmonic kilovolt-amperes added arithmetically. Thus, a capacitor will withstand 105 per cent voltage of fundamental frequency and 112 per cent current of fifth-harmonic frequency. The designs further assume that the average fundamental-frequency voltage will not exceed 105 per cent normal and that the short-time magnitude will not exceed 110 per cent. If capacitors are applied correctly with respect to fundamental-frequency voltages, the heating limitations will rarely be encountered unless the supply is subjected to very abnormal harmonic conditions which arise principally as a result of resonant conditions.

Wave Shape

Harmonics in supply circuits which parallel communication circuits may give rise to important noise-frequency coordination problems. These problems have been studied extensively by the Joint Co-ordination Committees of the Edison Electric Institute and the Bell Telephone System. One result of this work has been to set up two factors which take into account the over-all influence characteristic of a power-supply circuit from the standpoint of its tendency to produce noise in an adjacent telephone circuit. Because of the different effects of electric (electrostatic) and

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magnetic induction, separate factors are required, one for current and the other for voltage. These factors as applied to balanced circuits supplying rectifiers are the balanced $I \cdot T$ and $KV \cdot T$ factors; where I represents the rms balanced current and KV, the rms voltage in kilovolts between line terminals of the three-phase circuit, and T, condensed from *TIF* (telephone influence factor), is a factor which gives the relative weighting of the various harmonic frequencies of a power system from the standpoint of their tendency to produce noise in an adjacent telephone circuit. Two different frequency-weighting curves are commonly used in rectifier studies, namely, the 1935[4] and the 1941[5] frequency-weighting curves.* The difference in weighting results from different characteristics of the telephone circuit including terminal equipment. The principal change in the 1941 weighting from the 1935 weighting results from the substantial elimination in the later telephone receivers of a resonant point in the vicinity of 1100 cycles. At the present time both types of telephone receivers are in service and both types of frequency-weighting factors are used in co-ordination studies. While the 1941 frequency-weighting curve is expected to supersede the 1935 curve, time is required for the transition, and this has been delayed further because of the war. The shape of the 1941 frequency-weighting curve has been established, but its arbitrary scale has been delayed in order to determine for it a numerical value of the same importance as the 1935 curve. For present purposes the comparisons, based on the relative shapes of these frequency-weighting curves, are sufficient.

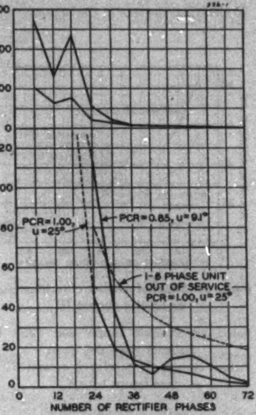

Figure 1. Effect of number of rectifier phases on relative current *TIF*

Based on 60-cycle supply and 1935 frequency-weighting curve

Actual current TIF=(relative current TIF)K' where

$$K' = \frac{I}{RI_{ac}}$$

I=D-c load current per secondary phase group

I_{ac}=Alternating current on primary side of transformer, rms value

R=Transformer ratio, primary to secondary line-to-neutral voltages

Insert: Replot of principal curves to smaller scale

An investigation, which is reported in the present paper, was undertaken to determine the relative influence of the number of rectifier phases on the inductive co-ordination problem, particularly for a number of phases beyond 12. Accordingly, calculations of the relative current *TIF* and relative voltage *TIF* for different numbers of rectifier phases from 6 to 72 were made by the method previously described. These calculations assume

1. Both 60- and 25-cycle supply.
2. Load and circuit conditions, represented by I the direct current per secondary phase group, X the commutating reactance in secondary terms, and E_o the crest value of anode-to-neutral voltage of the transformer secondary winding, of such values as to give a ratio of IX/E_o equal to 0.081, corresponding to an angle of overlap of 25 degrees.

In addition to the principal curves, which are based on no voltage reduction by phase control, that is, a phase-control ratio of one (PCR = 1.00), some results of calculations are given for a phase-control ratio of 0.85 (PCR = 0.85). The results of these calculations are plotted in the curves of Figures 1 through 6. Figures 1 and 2 give the curves of relative current—*TIF* for 60-cycle supply based on the 1935 and 1941 frequency-weighting curves, respectively. Figures 3 and 4 give the corresponding curves of relative voltage—*TIF*, respectively. Figures 5 and 6 give the corresponding curves of relative current—*TIF* for 25-cycle supply. Examination of the curves of Figures 1 through 6 shows important reductions in both the curves of relative current—*TIF* and voltage—*TIF* as the number of rectifier phases is increased. However, because of the high peak in the 1935 frequency-weighting curve in the vicinity of 1,100 cycles, the 18-phase rectifier with a 60-cycle supply and a 36- or 42-phase rectifier on a 25-cycle supply are less favorable than the next smaller number of rectifier phases. Because of these circumstances, it has been considered generally undesirable to use 18-phase rectifiers on 60 cycles and 36 or 42 phases on 25-cycle systems. However, the 1941 frequency-weighting curve does not have this sharp resonant point and there is a gain in increasing the number of phases throughout the practical range of rectifier application.

Effects of Supply-System Harmonics and Rectifier Load Balance and Their Interrelation

Harmonics in the supply system may have important effects on rectifier operation, particularly in producing unbalance in current between the various phase groups. Unbalancing load between phase groups also has an important effect on supply-system harmonics. Because of this interrelation between harmonics in the supply and load balance, it is convenient to consider the subject in three parts:

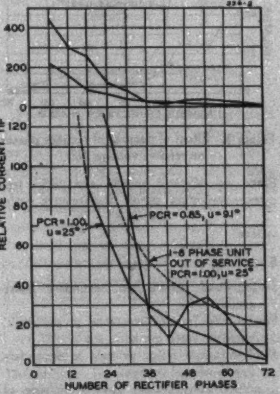

Figure 2. Effect of number of rectifier phases on relative-current *TIF*

Based on 60-cycle supply and 1941 frequency-weighting curve

Insert: Replot of principal curves to smaller scale

1. An infinite source with load unbalance.
2. An infinite source with harmonic voltages.

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* In addition, there is the 1919 frequency-weighting curve that remains in use for machine wave-shape guarantees. The 1919 curve is associated with the term "telephone interference factor;" the 1935 and the 1941 curves are associated with the term "telephone influence factor;" both terms have the same abbreviation of *TIF*.

3. A finite source with two or more rectifiers, such that the harmonics from one affect the voltage applied to another.

INFINITE SOURCE WITH LOAD UNBALANCE

If one six-phase unit of a multiphase rectifier* is removed from service, the harmonic currents in the supply are affected importantly, as pointed out previously. This unbalanced condition may be analyzed by the addition of

1. A fictitious six-phase load of such magnitude and phase relation when added to the actual loads as to provide a balanced multiphase rectifier.
2. A second fictitious load, the negative of the first.

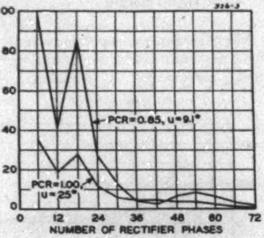

Figure 3. Effect of number of rectifier phases on relative voltage *TIF*

Based on 60-cycle supply and 1935 frequency-weighting curve *TIF*=(relative voltage *TIF*)*K''*

where

$$K''=\frac{K(\text{per cent }X)}{RI_{ac}}$$

I=D-c load current per secondary phase group

Per cent X=Transfer reactance in per cent, based on rated d-c kilowatts

R=Transformer ratio, primary to secondary line-to-neutral voltages

I_{ac}=Alternating current on primary side of transformer, rms value

Thus, this combination can be analyzed as

1. A multiphase rectifier of balanced positive load corresponding to the original number of phases.
2. A six-phase rectifier of negative load.

Both of the rectifier loads may be analyzed by the aid of the method previously described. The curves of Figures 1, 2, 5, and 6 also include some data giving the relative current telephone interference factor for a six-phase unit out of service in a multiphase rectifier. For convenience the curves have been plotted for a six-phase unit out of service with a current magnitude equal to that proportion of the total load. Actually, the harmonic currents should be increased because of the lower ratio of IX/E_0 that applies for such a condition.[5]

INFINITE SOURCE WITH HARMONIC VOLTAGES

The effect of harmonic voltages in a source, particularly at the lower-harmonic frequencies, is to increase or decrease during the conducting period the average voltage applied to the various phase groups. The practical result of this action is to unbalance the currents between the various phase groups. The phase position of the harmonic with respect to the fundamental is different for different phase groups derived by transformers from the same supply voltage. In considering the relative phase position of the harmonic voltage of a phase group with respect to its fundamental-frequency voltage, it is necessary to consider not only the higher frequency of the harmonic but also the difference in symmetrical-component phase sequence. It has been shown,[6] for the condition in which the crest of the harmonic voltage bears the same relation to the crest of the fundamental in each phase, and the sequence of the fundamental is positive, that the harmonics of order $(3m+1)$ are of positive sequence and of order $(3m-1)$ are negative sequence, where m is any integer. Thus, the 5th, 11th, and so forth, harmonics are of negative sequence, and the 7th, 13th, and so forth, harmonics are of positive sequence. The effect of harmonics on the average d-c voltage of a phase group can be understood more readily by reference to the diagram of Figure 7. If harmonics in the supply are such as to produce fifth-harmonic voltage applied to one rectifier phase group, as shown by the solid line curve of Figure 7, the effect is to decrease the average value of voltage on that phase group during the conducting period. If a second phase group is displaced 30 electrical degrees at fundamental frequency from the first phase group, as in a 12-phase rectifier, the phase relation of the fifth harmonic to the fundamental in the second phase group is displaced 180 degrees from the phase position of the fifth harmonic with respect to the fundamental in the first phase group. This circumstance results from the fact that the phase relation of the fundamental is shifted 30 electrical degrees in one direction while the fifth harmonic, being of negative sequence, is shifted five times 30 electrical degrees in the opposite direction. Thus, if the fifth-harmonic voltage in the supply tends to decrease the average voltage of one phase group, it tends to increase the average voltage of the other phase group in the 12-phase rectifier.

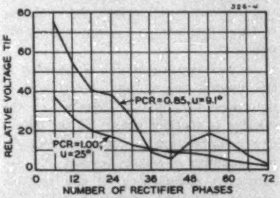

Figure 4. Effect of number of rectifier phases on relative voltage *TIF*

Based on 60-cycle supply and 1941 frequency-weighting curve

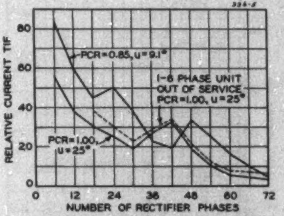

Figure 5. Effect of number of rectifier phases on relative current *TIF*

Based on 25-cycle supply and 1935 frequency-weighting curve

Actual current *TIF*=(relative current *TIF*)*K'* (see Figure 1 for *K'*)

If the harmonic voltage in the supply to a multiphase rectifier is shifted with respect to the fundamental, the harmonics in the secondary phase groups are shifted with respect to the fundamental, and the effects on load unbalance are altered. It is possible, therefore, that one transformer connection will be better from the standpoint of current balance than another, if harmonics of definite phase position with respect to the fundamental exist in the supply. In general, however, the phase position of the harmonic with respect to the fundamental will change at the rectifier location, and as a consequence there is little practical advantage in one transformer connection over another from the standpoint of current balance. The maximum effect of unbalancing between phase groups may be encountered in a 12-phase rectifier if fifth harmonics of appropriate phase relation exist in the power source. If rectifiers of more than 12 phases are supplied from a source with a fifth-harmonic voltage, the various rectifier units will carry loads between the two extremes for maximum load unbalance.

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* The term multiphase rectifier is used in this paper to mean rectifiers of more than six phases.

The maximum unbalance between displaced rectifier phase groups that can result from a fifth harmonic in the supply can be estimated from the equation:

$$\pm \text{ per cent } I_{un} = \frac{20 \text{ (per cent } E_5)}{\text{per cent regulation}} \qquad (1)$$

where

I_{un} = the unbalance current per rectifier in per cent of normal current per phase group

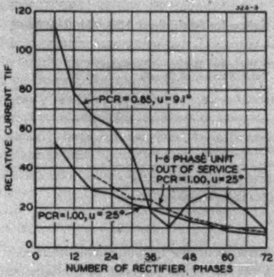

Figure 6. Effect of number of rectifier phases on relative current *TIF*

Based on 25-cycle supply and 1941 frequency-weighting curve

per cent E_5 = the fifth harmonic voltage in supply in per cent of fundamental

per cent regulation = the regulation of rectifier and transformer but not of common supply to the various phase groups, in per cent

Thus, the unbalanced current in per cent is two to five times the per cent fifth-harmonic voltage per cent in the source for the usual regulation conditions of from eight to four per cent.

The effect of a seventh harmonic in the supply is quite similar to that of a fifth harmonic. However, the seventh harmonic is usually of smaller magnitude and, therefore, has smaller effect on load balance. Furthermore, a greater magnitude of the seventh harmonic is required to produce the same amount of unbalance, because the uncompensated part of a shorter half-cycle of the harmonic must be averaged over the same conducting period.

Several years ago an analysis was made of a case where load unbalance was observed on a 12-phase, 3,000-kw rectifier. In this particular installation, four separate multianode tanks were supplied by a quadruple-zigzag 12-phase transformer connection. Harmonics in the supply voltage, both as to magnitude and phase position with respect to the fundamental, and the difference in regulation of the different phase groups were measured and the results compared with tests. The calculations based on the foregoing analysis indicated correctly which phase group should carry the heavier load. The magnitudes of the fifth-harmonic voltage and the unbalanced current are given in Table I.

The calculations were made from harmonic analyses of polar oscillograms of the supply-voltage wave, which gave both magnitude and phase position of the fifth harmonic with respect to the fundamental. For test 2, the phase position of the fifth-harmonic voltage was within four fundamental degrees of that position which theoretically should give the maximum unbalance for that magnitude of harmonic voltage.

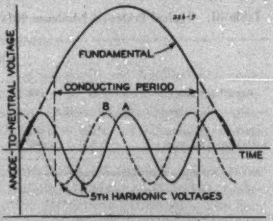

Figure 7. Diagram showing phase position of fifth harmonic to fundamental-frequency voltages for (*A*) maximum and (*B*) zero load unbalance of rectifier groups in 12-phase connection

Finite Source With Unbalanced Rectifier Loads and Harmonics

When multiphase rectifiers are operated from a supply system of negligible reactance, the unbalanced load resulting from the disconnection of one six-phase unit produces harmonic currents through the source corresponding to the fictitious negative six-phase load as previously pointed out. However, the outage of one six-phase unit does not affect the operation of the other six-phase units for the case of an infinite source. Consider now the case with a finite source. The effect of the harmonic current from the fictitious negative six-phase load corresponding to one unit out of service produces a harmonic voltage drop impressed on the other units which may be connected in parallel with the one that has been removed from service. Several effects may be noted. One effect of the harmonic impedance through the source is to divert part of the fictitious load current from the source to the other rectifier units. The effect may be viewed as being produced by the negative harmonic-voltage drops impressed on the remaining six-phase rectifier units as a consequence of the fictitious negative load. The result in any case is to reduce the harmonic currents flowing through the source to an amount that is less than would be expected from the greater equivalent reactance of the rectifier and supply system.[3]

Table I. Effects of Fifth-Harmonic Voltage on Rectifier Load Balance

Test	Fifth Harmonic Voltage	Unbalanced Current Per Phase Group—Per Cent	
		Calculated*	Measured
1	2.4	±6.5	±(6.5 to 7.6)
2	1.9	±5.6	±6.2

* From harmonic analysis of voltage wave.

From a practical standpoint the effect of these unbalances may be very important if they were allowed to exist. Thus, a 15 per cent fifth-harmonic voltage may produce sufficient unbalancing effect as to substantially remove the load from one six-phase unit and to nearly double the load on another. However, such conditions would normally not be allowed to remain because the voltage on the most heavily loaded six-phase unit would be reduced by phase control. This would have the effect of more nearly balancing the loads of the several six-phase units. The unbalancing effect, however, may be important if the momentary currents exceed breaker settings.

The effect of a unit out of service on a multiphase rectifier is, of course, to increase the wave-shape distortion of the supply and to increase the load unbalances. The results vary for the particular systems depending upon the number of units and reactance of the individual units and the reactance of the supply system at the harmonic frequency. The range of conditions is so great that it is scarcely practicable to generalize on the results that may be obtained. Reference, however, may be made to Marti and Taylor[4] and to the work of the Joint Subcommittee on Development and Research, Edison Electric Institute and Bell Telephone System, which has given considerable attention to this problem.[7-9]

They show that the increase in the $I \cdot T$ products for 30- and 60-phase rectifiers with a unit out of service varies from three to five times these factors for normal balanced conditions. Because of this, there is little advantage in using more than, say, 36 phases, unless the total rectifier load is large, or unless the possibility of resonance in the supply in the vicinity of 36th harmonic is anticipated.

Resonance in the Supply System

The harmonics produced by rectifier operation are of much greater importance if the supply system is resonant for one of the rectifier harmonics, the lower frequencies being more likely to be important from the standpoint of effect on apparatus and higher frequencies from the standpoint of inductive co-ordination.

Table II. Harmonics Present for Rectifiers of Various Phases

Harmonic	6-Phase	12-Phase	18-Phase	24-Phase
5	x			
7	x			
11	x	x		
13	x	x		
17	x		x	
19	x		x	
23	x	x		x
25	x	x		x
29	x			
31	x			
35	x	x	x	
37	x	x	x	
41	x			
43	x			
47	x	x		x
49	x	x		x

The harmonics that may be impressed upon the supply system for 6-, 12-, 18-, and 24-phase rectifiers under balanced conditions are shown in Table II for harmonics as far as the 49th. The number of frequencies of 49th harmonic and below that may be present for balanced operation are 16 for 6-phase, 8 for 12-phase, 4 for 18-phase, and 4 for 24-phase rectifiers. Thus, there is much less chance of resonance for rectifiers of the larger number of phases. However, if a six-phase unit is out of service, the harmonics that may be present are the same as for the six-phase rectifier, although the magnitudes are lower than for full six-phase operation. From the standpoint of chance for resonance when one unit is out of service, there is a difference between two 24-phase rectifiers if one is made up of four six-phase units or two 12-phase units.

Frequently capacitors are used in conjunction with large industrial rectifiers supplied over long transmission lines for the purpose of increasing voltage level, thus increasing the load power factor and the amount of power that can be handled. These capacitors may increase the opportunity for producing resonance, particularly at the time when a rectifier unit is out of service. Consequently, special attention should be paid to the harmonic problem for installations involving multiphase rectifiers and capacitors.

Capacitors

In themselves capacitors generate no harmonic voltages, but they affect harmonic voltages and currents from other sources only because of the relation of their harmonic reactance to that of the connected system. Large capacitors on the supply side of rectifier transformers tend to provide a short-circuit path for the higher harmonics incidental to rectifier operation. For the lower-frequency rectifier harmonics, large capacitors may form a resonant path with the source which may result in larger harmonic currents in the source and in the capacitor than in the rectifier transformers themselves. Increasing capacitor kilovolt-amperes beyond a certain value at a rectifier location will decrease voltage telephone interference factor. This is an effect with capacitors and harmonics from rectifiers that is analogous to that pointed out by Feaster and Harder[10] for the case of capacitors and harmonics from transformer magnetizing current.

Table III. Typical Preferred Minimum Kilowatt Rating for Multiphase 600-Volt Rectifiers*

Rectifier Units	Minimum Preferred Rating—(Kilowatts) Total Number of Phases						
	6	12	18	24	30	36	42
1,500 kw— 6 anodes	1,500	3,000	4,500	6,000	7,500	9,000	10,500
3,000 kw—12 anodes	3,000	3,000**	9,000	6,000**	15,000	9,000**	21,000

* Based on 6-phase transformers and phase shifters for the larger number of phases.

** Based on 12-phase transformers and phase shifters for the larger number of phases.

Depending upon conditions, it may be theoretically desirable to modify the harmonic-impedance characteristic of a shunt capacitor so as to make it

1. Provide a short-circuit path for harmonics originating in the rectifier.
2. Provide an open-circuit path to a particular harmonic originating in the system.

The short-circuit path can be obtained by adding a reactance in series relation with the capacitor of such value as to tune in series resonance for the desired frequency. Thus a reactance of four per cent at fundamental frequency on the capacitor-bank rating is required for series resonance with the entire bank at fifth-harmonic frequency. Such a reactor will increase by four per cent the fundamental-frequency voltage impressed across the capacitor, a circumstance that should receive consideration in the design and application of the capacitor. To make the shunt capacitor operate as an open circuit to a particular harmonic frequency, the capacitor may be divided into two parts with a reactor of appropriate value placed in series with one of them. Thus for a parallel-resonant combination at the fifth-harmonic frequency, a reactor of 16 per cent at fundamental frequency on the bank rating would be required for connection in series with one half of the capacitor bank. This reactor would increase the fundamental-frequency voltage applied to that section of the capacitor by eight per cent.

Where large capacitors are connected in parallel with large multiphase rectifiers supplied by transmission systems, it is particularly desirable to give careful consideration to the harmonic problem. The effects for the lower frequencies should be studied analytically, and by field tests which should give, if possible, the magnitude of the fifth-harmonic voltage in the supply and the harmonic-frequency impedances. With this information available, the capacitor application should be studied to determine what characteristic at harmonic frequency should be employed. It appears possible but unlikely that in a few installations the subdividing of the capacitor into sections tuned for various frequencies will be found desirable.

If the capacitor is supplied through a transformer (or autotransformer) and connected to the bus which supplies rectifier transformers, the effect is equivalent to putting a reactor in series with the capacitor and thus to tune it for some (not necessarily integral) harmonic frequency of the supply system.

Available Number of Phases

Standard ignitron rectifiers in the 600–700 volt class are available in ratings up to 10,000 amperes. Typical minimum preferred kilowatt ratings for a given number of rectifier phases for large loads, as in electrochemical service, are listed in Table III, which is based on the use of 1,500-kw 6-anode and 3,000-kw 12-anode assemblies. The 3,000-kw unit is, of course, more economical than the smaller unit and should be used where total load, spare capacity, and similar considerations permit.

In industrial applications using smaller ratings, fewer anodes are required, and it becomes correspondingly more expensive to provide many phases. The possible combinations depend upon the load to be carried, the requirements for spare capacity, and the additional expense that may be justified for additional anodes. Table IV has been prepared to indicate the range and characteristics of combinations for various loads and number of phases. In some cases combinations with different number of transformers are listed for a particular rating. Since the cost of the installation will increase with the number of transformers, Table IV also gives an order of preference for the various combinations from the operating point of view aside from wave shape.

Table IV. Combinations for Various Loads and Number of Phases

Rectifier Rating (Kilowatts) 600–700 Volt Class	No. of Transformers 6-Phase	12-Phase	24-Phase
750	1		
1,000	1		
1,500	1		
2,000	1	1*	
3,000	1	1*	
5,000	1	1*–2	3**
6,000	1	1*–2	3**

* 12-phase operation by the use of one quadruple-zigzag transformer.

** 24-phase operation by the use of two quadruple-zigzag transformers and phase-shifting transformer.

Summary

The harmonic and load-balance characteristics of rectifiers and their complicated interrelations have been analyzed. The effect of source harmonics on rectifier load balance has been shown. Curves giving the effect of a wide range of rectifier phases on wave-shape distortion have been presented. The factors which should receive consideration in the selection of the number of rectifier phases have been outlined.

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A Vertical-Flow Compressed-Air Circuit Breaker and Its Application on a 132-Kv Power System

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Synopsis: The compressed-air circuit breaker for indoor power-house service is becoming very popular. However, its use for high-voltage outdoor service is still in the field-trial stage. Several high-voltage porcelain-clad circuit breakers of American design are now in service, and their performance is being watched carefully. A 138-kv 1,500,000-kva three-pole breaker has been installed at the Kittanning substation of the West Penn Power Company. Before installation its interrupting ability was checked to 5,900 amperes with full 132 kv on a single-pole unit and on a circuit with a transient recovery rate of 3,100 volts per microsecond. The mechanical operation was tested with the breaker in a heavily iced condition at −14 degrees Fahrenheit. The air-supply system was tested at −4 degrees Fahrenheit, and both were found to operate satisfactorily.

The breaker was installed on the most active line of the West Penn Power Company system. During the three months following its installation it had 41 operations, five of which were on faults. Its operation to date has been satisfactory in every detail, and it is hoped that this new device will meet operating requirements and reduce materially the time outage for servicing such apparatus.

I. Introduction

THE circuit-breaker art, almost from the time of its beginning, has depended upon the use of oil as an interrupting medium. Its use has permitted this country to develop a power system whose expanse and kilovolt-ampere concentrations are equaled no other place in the world. However, during the past four years, the interest in and the acceptance of compressed air as an interrupting medium has increased at an accelerating pace.

A new type of compressed-air circuit breaker for powerhouse indoor service, utilizing a transverse blast, has been developed to cover the range of kilovolt-amperes from 500,000 to 2,500,000.[1-3]

Possibilities of the high-voltage compressed-air circuit breaker for outdoor service in America have been carefully considered and found attractive, but the advantages and disadvantages could best be definitely established by outdoor field installation and trial. Therefore, a breaker was designed, built, and tested in the Westinghouse high-power laboratory[4] and installed in August at the Kittanning substation of the West Penn Power Company.

II. Application

West Penn Power Company's 1940 plans provided for a 132-kv three-bay extension to their major switching station segregated from the existing structure. The use of three oilless breakers to eliminate fire hazard and reduce the time element for servicing was the objective. Such breakers were not available to meet the construction schedule. However, it was decided that operating experience should be obtained with such breakers; this resulted in the placing of an order in January 1941 for one vertical-flow compressed-air three-pole fully automatic 1,500,000-kva 138,000-volt breaker which was to be installed on a circuit where the greatest amount of operating experience would be secured which might be representative throughout the system. The circuit selected was already controlled by an oil circuit breaker equipped with impedance networks, provided with carrier line-current relaying and included in differential bus protection. The breaker specifications, therefore, included capacitors to replace the impedance networks, arrangement for mounting carrier wave trap, and double-ratio double-winding double-core current transformers. The inherent design located the current transformers on the line side of the breaker which therefore left the major portion of this breaker connected to the bus and included in differential protection, which was considered satisfactory. The design is such as to permit future automatic reclosing and single-pole operation.

The circuit controlled extends over a 30-mile wood-pole line to a switching station where it interconnects with the Penn-

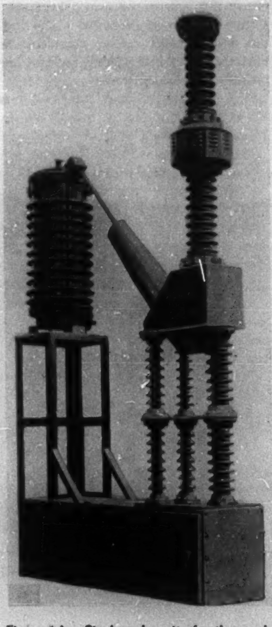

Figure 1A. Single-pole unit of a three-pole 138-kv 1,500,000-kva compressed-air circuit breaker

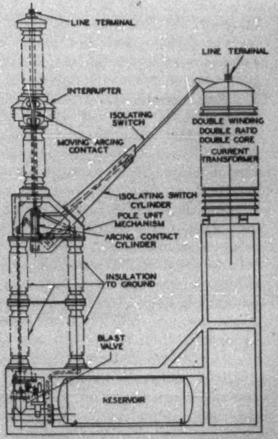

Figure 1B. Schematic drawing showing side elevation of the breaker in Figure 1A

Paper 43-51, recommended by the AIEE committee on protective devices for presentation at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943. Manuscript submitted November 27, 1942; made available for printing December 21, 1942.

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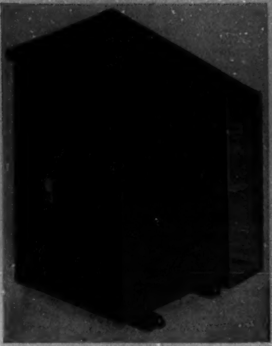

Figure 2A. Compressor house and 1,000 pounds per square inch storage system

Figure 2B. Compressors and driving motors mounted on the left side of the center aisle of the compressor house

Figure 2C. Motor controls and pressure governors mounted on the right wall of the compressor house

sylvania Electric Company's system. From that point the circuit extends over 43 miles of wood-pole construction to the Keystone system of the West Penn Power Company at Ridgway, Pa.; thence again over 38 miles of transmission line, where it extends and interconnects at Warren, Pa., with the Pennsylvania Electric Company's system.

The application of this breaker is generally in line with recommendations of the action of January 27, 1941 of the Joint Committee of the Edison Electric Institute, Associated Edison Illuminating Companies, and the National Electrical Manufacturers Association, which was later followed and agreed upon in their October 24, 1941 decision, recommending field service installations on a selected set of ratings. This development holds promise of effecting savings in critical materials.

III. Description

Figure 1 shows both a picture and a schematic diagram of a single-pole unit of the 138-kv breaker. The air-storage reservoir is mounted in a simple frame which also houses the blast valve and the valves which control air for opening and closing the breaker.

At one end of the frame is mounted a porcelain clad Inerteen-filled wound-primary current transformer. The double secondary, double winding, and double core provide for bus differential protection and standard line protection, using only one transformer unit per phase. The transformer forms one line terminal of the breaker and supports the stationary contact of the isolating switch. At the other end of the frame are mounted three tubular porcelains which provide insulation to ground and form a tripod support for the pole-unit mechanism. One of these porcelains carries the air blast; the other two carry air for opening and closing the contacts respectively.

The pole-unit mechanism consists essentially of two simple cylinders and pistons. The one mounted vertically operates the arcing contact within the interrupting device, and the other mounted at an angle operates the isolating switch. Both are very fast and pneumatically interlocked, so that the circuit is always opened on the arcing contact and closed on the isolating switch.

The double-orifice vertical-flow interrupting element described fully in another paper[5] is supported by the pole-unit-mechanism housing between two identical porcelain columns.

With the breaker in the closed position, the circuit is complete from the top of the upper interrupter porcelain through the interrupter, the pole-unit mechanism, the isolating switch, and current transformer.

Bleeder valves are provided to allow a continuous trickle of dry air through all three porcelain columns, thereby preventing breathing and keeping their inner surfaces in better operating condition.

Table I. Interrupting Tests on a Single-Pole Unit of a Three-Pole 138-Kv 1,500,000-Kva Compressed-Air Circuit Breaker

Tests June 5, 1942, at the East Pittsburgh High-Power Laboratory. Witnessed by Engineers of the West Penn Power Company

Test	Applied Voltage (Kilovolts)	Recovery Voltage (Kilovolts)	Amperes Interrupted (RMS)	Amperes Closed (Crest)	Reservoir Pressure (Pounds Per Square Inch)	Arcing Time (Cycles)	Circuit-Transient Recovery Rate (Volts Per Microsecond)	Frequency Cycles Per Second	Type of Operation
*1	120	114	1,410		350	0.8	1,100	60	O
2	120	116	1,150	1,560	350	0.7	1,100	60	CO
3	120	112	2,540		350	1.2	1,550	60	O
4	120	108	2,030	4,330	350	1.5	1,550	60	CO
5	120	110	3,160		350	1.0	2,000	60	O
6	120	107	2,820	7,150	350	0.8	2,000	60	CO
*7	120	109	4,830		350	1.7	2,600	60	O
†8	120	108	4,400	10,600	350	1.3	2,600	60	CO
9	44	41.5	2,400		350	1.4		60	O
10	88	81.0	5,650		350	1.6	2,300	60	O
11	88	80.0	4,800	11,800	350	1.0	2,300	60	CO
*12	88	82.0	6,750		350	1.6	2,500	60	O
13	88	80.5	5,840	8,050	350	1.7	2,500	60	CO
14	88	83.0	7,000		350	1.0	2,500	60	O
15	88	80.0	5,410		350	1.0		60	O

* Oscillograms shown in Figure 4. † Oscillogram shown in Figure 5.

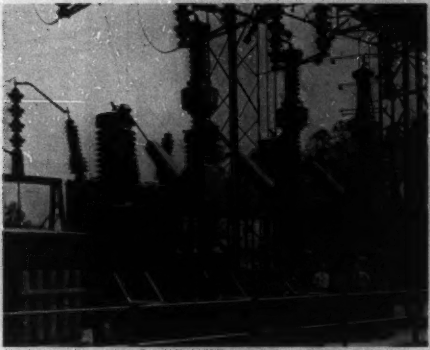

Figure 3. A three-pole 138-kv 1,500,000-kva compressed-air circuit-breaker installation at the Kittanning substation of the West Penn Power Company

The three poles are mounted separately and are mechanically independent but are simultaneously electrically controlled from a single housing which contains all relays, gauges, and so forth. Their operation is synchronized to within a cycle, and they may be converted easily to provide high-speed reclosing for either segregated or three-pole service. All control wiring is enclosed within the tank enclosure and supporting structure.

Figure 2 shows the air-supply system which consists of an insulated heated house containing two 1,000-pounds-per-square-inch compressors separately driven and operated independently or in parallel, each feeding through separate coolers into a battery of five 1.5-cubic-foot storage bottles. These bottles are connected through another cooler to a second battery of five bottles. All are used as storage capacity at 1,000 pounds per square inch from which air is supplied through a pressure-reducing valve to the breaker reservoirs where it is stored at 350 pounds per square inch. By compressing air to 1,000 pounds per square inch, cooling, and then expanding to 350 pounds per square inch, air sufficiently dry for satisfactory operation of the breaker is secured. Preparations are being made to measure periodically the moisture content of the air under varying atmospheric conditions.

The air-supply unit is mounted adjacent to the breaker, as shown in Figure 3, and connected by means of a one-inch copper air line to the middle pole unit from where it distributes through the respective control valves to each of the two outer pole units. From the central-pole control cabinet, air passes through a protective valve which is arranged to allow air to flow forward freely after the pressure has reached a predetermined value, but to limit the flow in the forward direction in case it exceeds a predetermined amount, which would be the case with a fouled valve or burst connection. A check valve is also incorporated in this protective scheme to guard against a reverse flow of air.

Three pressure-responsive switches are connected to the common air line to provide signals guarding against the functioning of the breaker with insufficient air pressure. One switch provides alarm when pressure drops below 250 pounds per square inch, another opens the closing circuit when pressure drops below 240 pounds per square inch, and the third opens the tripping circuit when the pressure drops below 225 pounds per square inch. These switches are so connected in the control circuit that, once an operation is initiated, it will be completed without interference from the pressure switches. Settings are such that, if there is sufficient pressure to allow closing, there will be also sufficient pressure to enable the breaker to open immediately in case of a fault.

IV. Factory Tests—Interrupting Tests

One pole of this breaker was tested in the Westinghouse high power laboratory[4] and witnessed by representatives of the West Penn Power Company. Table I shows this series of 15 short-circuit tests, including both opening and closing–opening operations with 120 and 88 kv respectively across the single-pole unit. The maximum current interrupted at 120 kv was 4,830 amperes, and 7,000 amperes at 88 kv. There was practically no visible disturbance during the opening operation of these tests, but a limited amount of arcing was noticeable at the final closing position of the isolating switch. The air discharge on opening operations was quite noticeable but not at all serious and did not increase with the increase of kilovolt-amperes interrupted. After the tests, the arcing and isolating contacts were all inspected and found to be in excellent condition.

Figure 4 shows three typical oscillograms of the opening operations.

Figure 5 shows a typical oscillogram of a closing–opening operation.

Table II shows a series of six opening operations with 132 kv across a single pole of the 138-kv breaker. The reactance of the circuit was adjusted to give 4,800 amperes with a circuit transient recovery rate of 2,800 volts per microsecond. The pressure in the reservoir was decreased after the first two tests, so that on the last test 4,400 amperes were interrupted with 225 pounds per square inch.

Figure 6 shows the magnetic and cathode-ray oscillograms of a single-pole unit interrupting 5,900 amperes with full 132 kv applied to its terminals. The circuit transient recovery rate was 3,100 volts per microsecond. The arcing time was 1.2 cycles.

These tests have not extended the limits of kilovolt-amperes interrupted on this type of interrupter.[5] However, they were made to demonstrate the adequacy of this breaker for 138-kv 1,500,000-kva service.

V. Mechanical Tests

The high-pressure air system was tested at 2,000 pounds per square inch, and the low pressure reservoirs at 800 pounds per square inch. The storage tanks met the state safety code and inspection requirements and carried their approval before assembly in the breaker.

At 350 pounds per square inch, the closing time for the isolating switch is approximately one-fourth second. The

Table II. Interrupting Tests With 132-Kv Initial Voltage, 60 Cycles, Applied to a Single-Pole Unit of a Three-Pole 138-Kv 1,500,000-Kva Compressed-Air Circuit Breaker

Test	Recovery Voltage (Kilovolts)	Amperes Interrupted (RMS)	Reservoir Pressure (Pounds Per Square Inch)	Arcing Time (Cycles)	Circuit Transient Recovery Rate (Volts Per Microsecond)
1	121	4,880	350	0.8	2,800
2	120	5,100	350	0.8	2,800
3	120	4,600	300	1.0	2,800
4	120	5,000	275	1.1	2,800
5	120	4,700	250	0.8	2,800
6	118	4,400	225	1.6	2,800

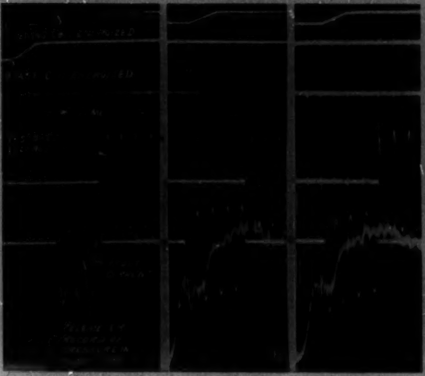

Figure 4. Magnetic oscillograms from Table I

Currents and voltages—rms (AIEE Standards)

A. Fault current 1,410 amperes; recovery voltage 114 kv

B. Fault current 4,830 amperes; recovery voltage 109 kv

C. Fault current 6,750 amperes; recovery voltage 82 kv

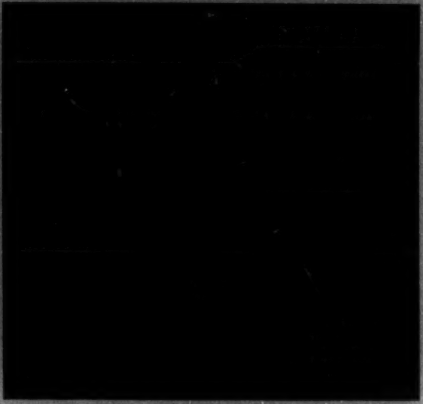

Figure 5. Magnetic oscillogram from Table I

three isolating switches reach the closed position within one cycle of each other. The time from rise of blast-coil current to parting of the arcing contacts is approximately six cycles. The three arcing contacts part within 0.6 cycle of each other. The spread is increased 0.1 cycle as the operating pressure is decreased to 270 pounds per square inch. The opening of a closing–opening operation increases the spread between the arcing contacts by another one- or two-tenths cycle.

VI. Low-Temperature Tests

The operation of the air-supply system at low temperatures was checked by placing it in a cold room with subzero temperature and operating it periodically for several days. Air was supplied to the compressor from outside the cold room, so that moist summer air at 85 degrees Fahrenheit was being compressed. The connection necessitated leaving ajar the door of the compressor cubicle; as a result, the temperature in the compressor cubicle reached 16 degrees Fahrenheit when the cold room temperature was −4 degrees Fahrenheit. At the end of the test, the air in the storage system was discharged through the pressure-reducing valve, with no tendency to foul its operation.

Everything functioned normally except a check valve in a cooler, which failed several times to prevent pressure from getting back to the compressor. This produced no evidence of trouble and was detected only by opening the drain valve on the compressor.

Figure 7 shows a single-pole unit of this breaker iced in a cold room at −14 degrees Fahrenheit. The tests at low temperatures extended over five days. However, in order to check its operation under the most severe conditions, the compressor was placed outside the cold room and moist warm summer air was pumped directly into the breaker, without going through the usual stages of compressing and then re-expanding in order to eliminate the moisture. Warm water

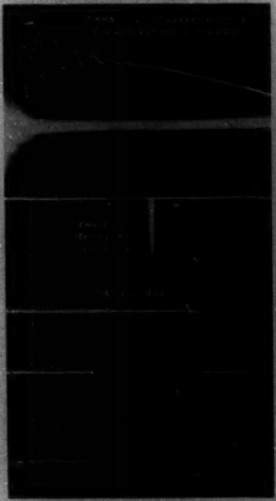

Figure 6. Cathode-ray and magnetic oscillogram of a single-pole 138-kv breaker operating with 132 kv across its terminals

Fault current 5,900 amperes; applied voltage 132 kv; circuit transient recovery rate 3,100 volts per microsecond

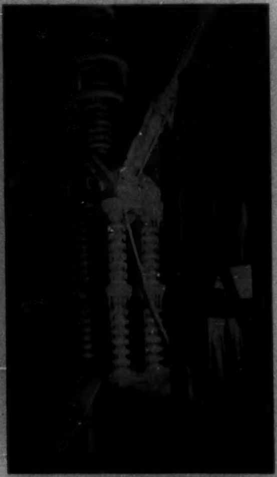

Figure 7. A single-pole 138-kv compressed-air circuit breaker iced for mechanical and electrical tests at −14 degrees Fahrenheit

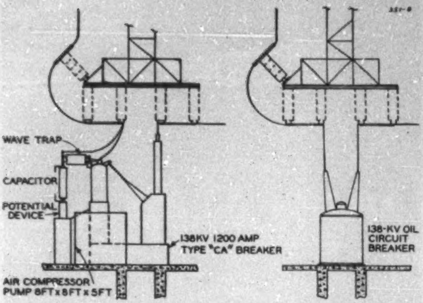
WAVE TRAP
CAPACITOR
POTENTIAL DEVICE
AIR COMPRESSOR PUMP 8FT x 8FT x 5FT
138KV 1200 AMP TYPE "CA" BREAKER
138-KV OIL CIRCUIT BREAKER

Figure 8. On the right is shown an oil circuit breaker located in the switchyard, and on the left is shown the way in which the compressed-air breaker and its accessories were arranged after the oil breaker was removed

was directed on the guide and packing gland through which the disconnecting blade slides, while the breaker was opened and closed. Thus the valves and all operating parts were thoroughly wetted. The temperature was then dropped to −14 degrees Fahrenheit and, by means of an intermittent spray, the breaker was iced as shown in Figure 7.

Upon energizing the opening valve, the operation of the contacts appeared normal, and the isolating blade apparently functioned as though no ice at all were present. Its normal average speed of 24 feet per second was recorded.

While the breaker was still iced, 132 kv was applied across the insulation ground for two minutes. Some corona was observed on the ends of icicles, but no flashover resulted.

VII. Installation

The complete breaker was trucked about 45 miles to the Kittanning substation on July 16, and, after assembly of the pole units in the vicinity of its permanent location, the oil breaker was removed, and within seven days the air-blast breaker was installed, miscellaneous changes were made, and it was tested and placed in service. Approximately 1,400 man-hours were spent on the entire job. The miscellaneous changes consisted of removal of the capacitors from the breaker assembly, rearrangement of controls, fittings, and so forth. The manufacturer's design met all general specifications, but the three capacitors were relocated and connected to the station bus to provide bus potential for all 132-kv relaying, synchronizing, and so forth. The original capacitor and carrier wave-trap installation was retained. Figure 8 shows an installation comparison of the oil breaker with that of the air-blast unit. The latter shows the capacitor with one wave trap in place as originally planned. All three poles of the breaker and the air-compressor unit have been installed within the oil-circuit-breaker bay, all of which are located on an elevated concrete platform.

The complete functioning of the entire unit was checked with the following results. With the air compressors out of service and the air-storage system normal, six opening operations and five closing operations were obtained in less than six minutes. It is recognized that this sequence should be used only in an emergency, since the charging of the breaker reservoirs from low bottle pressure decreases the effectiveness of the drying cycle. However, after operating the breaker with improperly dried air, its Megger readings had not decreased, and the breaker was placed on the 132-kv line without evidence of trouble. With the high-pressure system disconnected, the breaker completed an opening operation, followed immediately by a closing–opening operation, using air only from the pole-unit reservoirs.

It has not been found convenient to make short-circuit tests on the system because of the importance of the interconnection and because of the duty to which the breaker can be subjected being limited to about 1,000,000 kva. During the three months following its installation there was a total of 41 breaker operations, five of these being automatic as a result of line faults. All of these have been faults to ground with current values limited to not more than 3,000 amperes. All operations have been quite satisfactory with no noticeable system disturbance and without evidence of any breaker distress.

Included in the preceding operations there have been a number of cases where the charging current of the unloaded open end line has been interrupted with no noticeable disturbance.

The trickle of air through porcelain columns requires about one hour's operation per day for one compressor. A rather complete record is being kept of all work pertaining to the breaker, and no definite maintenance schedule has been formulated as yet. Air-compressor inspection will be governed by actual operating hours.

VIII. Conclusion

We are quite hopeful that this new device will meet operating requirements and reduce materially the time outage for servicing such apparatus. The West Penn Power Company's future breaker purchases will be governed to a large extent by the performance of this unit, and it is hoped that other utilities may benefit and take advantage of this experimental installation.

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Rural Electrification Engineering and Electroagricultural Engineering

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THE great interest shown recently by electrical engineers in rural electrification engineering and electroagricultural engineering is demonstrated by the fact that in the October 1942 issue of ELECTRICAL ENGINEERING there appeared a paper which was entitled "New Engineering and a New Industry," presented in December 1941 before the American Society of Agricultural Engineers. The statement made therein that "before 1935 there was no rural electrification engineering," which was misinterpreted by some readers, was not made to discredit the work of anyone in electrifying farms, but referred to the fact that there was no specific branch of engineering that could be called "rural electrification engineering." In fact, we all consider rural electrification engineering as a part of distribution engineering in general, and the latter is likewise a very young branch of engineering. As a matter of fact, very few spoke of "rural electrification." In most of the literature prior to 1935 you will find only the term, "rural line extension." The following statement is from the National Resources Committee report in "Technological Trends and National Policy," 1937, page 284:

> "Electric distribution grew as America grew. No one could have possibly predicted the rapid growth of American cities or American industries. During that rapid period of growth, any systematic planning of distribution for several years ahead became out of date on short order. Furthermore, during the rapid growth of the industry, when millions upon millions were spent on generation and transmission, practically all the attention of engineers and manufacturers was concentrated on these two fields. The engineers were so wrapped up in generation and transmission that many of them considered distribution as too lowbrow to merit their attention. Distribution was frequently left to the lineman, purchasing agent, and storekeeper, and many distribution systems grew up to give the impression of crazy quilts, without any apparent logic either in circuit sizes, voltages, or locations, and sizes of transformers."

Distribution engineering really started with the introduction of the a-c network. Rural electrification engineering is a branch of distribution engineering, but the very fine engineering developed for networks, could not be applied to it. Those who had to compute rural circuits had to, and, to a great extent, still have to, use the methods developed by that branch of engineering to which most of the engineering brains of the last 30 years or so has been devoted—transmission engineering. Now, when on a transmission system the short-circuit current, the voltage regulation, and the stability are computed, the results indicate where breakers are needed and what kind, where relays are needed and what kind, where voltage regulators are needed and what kind, where reactors or capacitors are needed and what kind. The equipment can generally be found in a catalogue, and, if you have the money to buy it, even if each breaker costs $130,000, it can be ordered. You can also assume that there will be operators and operating engineers, even specialized engineers who are qualified to operate and maintain the large breakers, the fine relays, the elaborate recording and metering devices, and so forth. When the same method of computation is applied to rural circuits, and when the results are obtained, what do you have? Probably just a fuse, and you hardly have enough money to put in a fuse every place you think there should be one. And in many cases you may not put in a fuse—you will take a chance on getting along without it, because it is practically impossible to co-ordinate fuses as accurately as to co-ordinate relays. And you know that there will be no one but a lineman to operate and maintain. The importance of continuity of service for farming may be far more pronounced than for domestic use. Previously it took two to three weeks to compute the short-circuit current of a rural system, and, after the computation was all done, most of the results could not be used advantageously, since you had to do the best you could with existing equipment. So we first proceeded to reduce the mathematical analysis to existing conditions, with the result that an engineer can now do the job in one day. Second, we were able to show the manufacturer that a good market exists for a good circuit breaker for use on rural distribution circuits. There was only one on the market, but the manufacturer of this breaker had vision and kept improving it to meet the specific requirements of the primary rural circuit, with the result that Rural Electrification Administration systems alone bought over 5,000 such breakers in one year, and practically all systems that have them report that they are satisfied with their performance, because the breakers have saved them many extended interruptions. Other manufacturers have developed small breakers, a few of which are already in operation, but the war stopped further activity. We will need better and cheaper primary circuit breakers, both single-phase and three-phase, with a greater degree of co-ordinating flexibility. Many additional improvements are still needed to give more perfect co-ordination. This is what we call the first chapter of rural electrification engineering. It is by no means complete. (In electrical engineering we are accustomed to being always a few years ahead of the textbook.)

We consider the work done on telephone co-ordination as the second chapter of this specific branch of electrical engineering.

The voltage problem which is now in the process of study, will probably form the third chapter. We have already learned a lot that we did not know before, and some of it, we hope, will soon be ready for presentation to the profession, but it is a long and difficult job, and the analysis no doubt will bring out the need for new equipment. In this whole field of engineering there are not enough operating data for establishing the foundation for all future designs based on Kelvin's idea of balancing fixed charges against variable charges. The systems are too young. But enough data are already available to indicate the need for changes in equipment design. For instance, we already know that rural distribution transformers should have lower core losses. There is hardly any secondary distribution in rural areas; the consumers are so far apart that practically every one must have an individual transformer. Most of the rural systems use purchased energy, and in most cases the cost of energy represents a very prominent item in the total annual charges.

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This is in general an entirely different picture as compared to an urban distribution system.

There are innumerable similar items of difference. But what has been said here will be sufficient to stimulate the electrical engineer to devote some time to this new specific branch of electrical engineering and convince the electric-equipment designer that different and new equipment is needed for this field. The market is now large enough to justify these specific studies. For example the following are a few things that manufacturers could devote time to developing: smaller transformer with very low core loss; cheaper, but reliable lightning arresters, based on the fact that the short-circuit current on rural systems is practically never over 1,000 amperes; cheaper meters, more rugged, even perhaps at the sacrifice of a bit of accuracy; simple, cheap, portable instruments to measure ground resistance, without bothering too much about extreme accuracies; insulators that will stay put on the pin and not give radio interference; portable testing instruments that are safe and simple to operate; a simple portable oil tester and filter; new voltage-regulating equipment will no doubt be needed, but the available data are as yet inadequate for definite functional specifications.

There are hundreds of thousands of miles of rural circuits, single-phase, Vee and three-phase, 7,200 volts, all with grounded neutral. Single-phase circuits without any neutral at all are in existence here and there, the earth serving as the return path for the circuit. Each transformer is connected from the phase wire to ground. If there should be a break in the grounding wire or wires any place between the transformer and the ground, there would be practically full voltage across the break. This is indeed dangerous. But we have not heard of any accident resulting from this type of failure. A neutral conductor, which is assumed to be at ground potential, may be more dangerous than a 220-kv line, since adequate precautions always are taken when handling 220 kv. But we must know more about it, and the only way to find out is first to exhaust analytical methods, based on reasonable assumptions that are again based on experience, and then to do a lot of testing. There may be a voltage gradient on the surface of the soil from the pole out, and it will be necessary to establish what that voltage gradient is, because 100 volts may kill a horse. I am not expressing any opinion for or against the use of such circuits; I merely want to emphasize the importance of ground testing on a large scale. Where there is a neutral conductor, it may carry only 30 or 40 per cent of the amperes, the earth carrying the rest. And where there are amperes, there are volts, and here too the voltage gradient should be tested. I know of course that such tests need a voltmeter and not a ground meter, but, if everyone had enough ground meters to establish the ground resistance in advance, the voltage gradients could almost be predetermined by computation. Besides, what are a few ohms of resistance in the life of a young lightning stroke that can jump across half a mile of space with the greatest of ease? Some tests will have to be carried on to determine ground inductance and capacitance. So you see how much engineering there is still to be done on the item of grounding alone before we are ready to say from an engineering viewpoint that we know the characteristics of a rural distribution circuit. This ought to be enough to satisfy those who think that "rural electrification engineering" is as old as Benjamin Franklin.

For supplying electric energy to farms in the less sparsely populated territory, beyond the economy of line extensions, there will be needed small self-contained simple rugged generating units, each to supply one or several farmers. It may be possible that plowed-in underground cable can be used, or that some other new method can be developed for serving a group of farms located in an isolated area.

Constant current primary with constant potential supply and constant potential load, as proposed by Percy H. Thomas and others, deserves serious consideration. Further study may prove its applicability to many conditions in rural areas, and, if it does, new equipment will have to be developed for the purpose. Conditions in the future will no doubt call for underground primary systems, and for these conditions a transformer will be needed which can be put right in the ground without any manholes or boxes. Such a transformer already has been tried out in Canada, but further development will be needed.

During the two years from the middle of 1940 to the corresponding period of 1942, joint development work has continued between the Rural Electrification Administration and the Bell Telephone System to determine the practicability of providing telephone service to rural communities by carrier channels superimposed on power distribution lines. Substantial progress has been made in solving a number of the technical problems encountered. Laboratory models have been made of apparatus which would be required at subscribers' locations and telephone central offices to render this service. Preliminary models also have been made of the apparatus which must be added to the power line, not only at the points where the connections are made to the subscribers' stations and the telephone central offices, but also at other points, in order to provide a satisfactory communication channel.

Considerable work remains to develop the apparatus into good commercial form and to determine from a field trial the answers to the many operating problems which would be encountered in rendering a service of this type. The status of the work recently has been reviewed jointly by the Bell Telephone System and the Rural Electrification Administration, and a decision reached that the work should be suspended for the duration of the war. This helps to meet the present need for concentrating on the war all possible resources, including technical men, critical materials, and manufacturing effort.

Problems of radio interference also occur in rural areas. A reasonably good locator has been developed by Rural Electrification Administration engineers in co-operation with a manufacturer. Several hundred such instruments are now being used with reasonably good results, but improvements are needed. As "telephone interference" was changed to "telephone co-ordination" by co-operative efforts of utility engineers and telephone engineers, so should "radio interference" be changed into "radio co-ordination" by co-operative efforts of the engineers of the electric utilities, the broadcasting companies, and the radio manufacturers.

So you see: The rural circuit is not just a pole, a guy, a little insulator, a little transformer, and a little fuse. It calls for the most painstaking engineering analysis. And when I speak of engineering, I will repeat that anything not based on a solid foundation of physics and mathematics is not engineering.

We now come to electroagricultural engineering. When the statement was made that there was no electroagriculture, it was not meant that no one ever used electricity on the farm. And when it was said that there was no electroagricultural engineering, it did not mean that no engineer ever tried to apply engineering to the use of electricity for farming. Again, it was meant that as yet there is no specific branch of engineering that could be called electroagricultural engineering, and there was no specific industry that could be called electroagricultural industry. As a matter of fact, these things are so new that it is difficult to say now what the subject of the first chapter of "Electroagricultural Engineering" should be. Even this will have to be developed as we go along. No more will therefore be said here about the engineering possibilities, and the discussion will limit itself to the industrial possibilities. Here we know of some definite needs, and this is addressed primarily to progressive American manufacturers who look ahead, find out what will be required, and proceed to build it and put it on the market.

Electroagricultural engineering is closely associated with rural electrification engineering, because, for example, the grounding problem is probably more important in connection with the fire hazard on the farm than in the city. A faulty ground may cause a fire, and, when a fire starts on the farm, the results are frequently disastrous. A simple cheap ground meter, suitable for the convenient use of every electrician, should be developed. Incidentally, the study of lightning protection of farm buildings and perhaps the development of more competent equipment for this purpose likewise invite some activity on the part of the electrical manufacturers.

While we are on the subject of hazards, we may as well consider the electric fence which is important in wartime, because less critical material may be used. The diversity of the types of control on the market is so great as to be bewildering, and many of the types are definitely hazardous. A committee of the National Electrical Safety Code has been engaged in the study of this subject, but, before quantity production is possible, some work by an AIEE committee and a National Electrical Manufacturers Association committee will no doubt be in order.

When large-scale rural electrification started, all efforts were concentrated on bringing into the farm the minimum amount of civilized comfort such as electric lights, radio, a flat iron, a water pump, a washing machine, perhaps a hot water heater, and here and there an electric range. This was as it should have been. The activity should continue, and improvements should be introduced. Illuminating engineers should study the problem of lighting for the farm, the farm home, the farmyard, the barn, and soforth, and the specific requirements of the circuit. This should possibly be made a project by the Illuminating Engineering Society. Lighting problems on the farm are different and call for different treatment.

An important item of comfort to which manufacturers should devote their attention is the development of a small, cheap, but very reliable water pump, in order to make it possible to bring running water into the poorest farm home. I consider this to be as important as the electric light. Public health authorities are unanimous in their opinion that running water in every home would reduce sickness in the countryside.

Recently there has come about a realization that every effort should be made to develop equipment to make it possible for the farmer who has electricity to use that electricity for real farming, to get more revenue out of his farm, to produce more or better food, to preserve food, and to make it unnecessary to let good food go to waste. This is real electroagricultural engineering. As was brought out in the paper previously referred to, some wonderful work has been done along this line by men of science in the colleges and in other laboratories, but it has not been possible as yet to make the result of their labor available to all the farmers in the country, because there is as yet no specific industry which can manufacture the items as quantity production. There is already some evidence of the beginning of such an industry. What already has been done only helps to demonstrate what we may expect in the future when a real electroagricultural industry is established, using quantity production methods for making low-cost but reliable equipment that the small farmer can use to get a little more revenue out of the electricity that he already has or will have. Most of the items already on the market are too expensive for the small farmer. They are intended for the more prosperous farmer, and our main interest should be to make the small farmer more prosperous.

At various times individuals have endeavored to list the electric equipment and appliances suitable for both the farm and the farm home. Some of these lists exceed the 350 mark. State Agricultural Colleges, the United States Department of Agriculture, and others have been responsible in large measure and have worked exceedingly hard for many years on the research and development of electric applications. It must be said that very few of these have found their way to the farm in large numbers. It is most difficult to get one's teeth into the real meat of these applications, for, although much work has been done, yet little specific information, either on statistics or accomplishments, has been assembled. At first thought this seems strange, because so many of these can be catalogued as income-producing equipment which has been proved profitable to the owner.

The year 1941 was an exceptional year in manufacturing and in the sale of all farm equipment, electric and nonelectric. It exceeded in volume any previous year, and the sales value of it was 36 per cent greater than 1940, and 34 per cent above the high mark set in 1937. Electric farm equipment sold for both the years 1941 and 1940, and the increase in favor in 1940 are shown in Table I.

It will be noted that the electric equipment manufactured and sold in quantity is confined to a few items. Those items assembled and released by the Bureau of the Census include only milking machines, cream separators, milk coolers, domestic pumps, water systems, and brooders. It should be remembered that the figures shown are for years when the sales of these units were exceptionally high. Considering this fact, it is obvious that with 2,337,160 farms electrified we still have a long way to go to reach saturation of this group alone. We will not reach it until we have a specialized industry with quantity production. Even on dairy farms where electricity can be used in so many ways it has been shown on an average that only 10 per cent of the connected load is used for farming operations; the other 90 per cent is used for lighting, household appliances, and water pumping. The former figure is exceedingly low when we think of electric service as being used to increase farm production and income.

An electric motor-driven cream separator will finish the job in half the time that hand work will. With the larger sizes of separator it is now a one-man instead of a two-man job.

Records of 25,000 acres in the state of Oregon which were irrigated show a gross increase of $40 per acre over nonirrigated acres. Although irrigation is practiced in every state of the nation, yet today it is only in its infancy in the humid regions.

Individual farmers report savings up to $100 per year by electric feed grinding, but here too we cannot as yet speak of

Table I. Electric Farm Equipment Sold*

	1941	1940	Increase Number	Increase Per Cent
Milking machines	53,463	33,930	19,533	57.6
Cream separators (hand or power operated)	138,785	105,254	33,531	31.8
Milk coolers	13,143	9,134	4,009	43.8
Domestic pumps and water systems	360,412	267,972	92,440	34.5
Brooders	111,689	91,340	20,349	22.3

* Source: MANUFACTURE AND SALE OF FARM EQUIPMENT. Bureau of the Census, United States Department of Commerce.

general use of electric feed grinders on the farm.

Numerous other activities could be quoted where electricity has been used successfully on the farm. But anyone who has had a chance to see the fine research work being done in the various colleges will at once realize that the surface has not yet been scratched and that manufacturers will find a ready market for many items now in the development stage in the colleges, or in the minds, or the cellars of individual inventors. Until recently there was very little incentive for manufacturers to spend money on the development of new devices for the farm. Such new items are hard to sell and they are expensive to sell. Farmers are far apart from each other, and the very sales effort required for introducing a new item on the farm makes the cost of the article in many cases prohibitive. The REA co-operatives use a group-purchase system, and they can arrange with manufacturers to cover a quantity purchase by one agreement. Since November 1939 the Rural Electrification Administration co-operatives purchased most of their meters under the group-purchase plan, totalling about $4,750,000 to the satisfaction of both the consumers and manufacturers. While all the service transformers for new construction have been furnished by the contractor, the co-operatives used the group-purchase plan for securing close to 25,000 transformers for extensions, totalling nearly $2,500,000. Quite a few other items have been handled successfully through the group-purchase plan, making it unnecessary for the manufacturers to try to sell a new item from house to house.

It is important to realize that whatever is manufactured for the farmer must be low in price, rugged, and simple in operation. Only quantity production can bring about such devices.

It is gratifying to be able to report that there is quite a number of progressive American manufacturers who have been giving this matter their careful attention and have been spending considerable sums of money on development of many devices suitable for quantity production.

One manufacturer developed a self-contained quick freezer and cold-storage unit having a capacity of 20 cubic feet, which means that it will hold about 1,000 pounds of meat, or 500 pounds of fruit or vegetables. It is sufficiently small in dimensions to be accommodated on the back porch of the house, and it is estimated that on a quantity production basis it would cost less than $300. There is no question that there is an enormous market for such an item.

Another manufacturer, to make it possible for the farmer to grind his own whole-wheat flour and cereals, making it unnecessary to ship his wheat to the factory and then get it back again in the form of flour, developed a small kitchen-type flour mill weighing only 25 pounds and able to grind 20 to 40 pounds of flour per hour. The cost of this little mill based on quantity production will not exceed $25, and it is hoped that eventually it will have an attachment so that the standard makes of mixer attachments in the kitchen can be attached to the mill, thus eliminating the necessity of buying a mixer. But this idea, along with the freezer, had to be put to sleep temporarily on account of the war.

Dehydration is another very important item. We know, of course, that there is a great deal of commercial dehydration going on in this country and that dehydration made it possible to send enormous quantities of food abroad which otherwise could not have been sent. But two problems on the farm are as yet unsolved.

1. Everyone who has traveled through the country knows that enormous quantities of apples, peaches, and some vegetables go to waste, because the farmer cannot afford to harvest them.

2. It should be made possible for the farmer to dehydrate such fruits and vegetables for his own use.

A small kitchen type dehydrator would solve these problems. One progressive manufacturer developed a small dehydrator weighing less than 100 pounds and being 28 by 20 by 23 inches in dimensions. This little dehydrator does not call for special wiring, because the consumption is so low that it can be attached to the house circuit. It can produce in 12 hours four pounds of dehydrated apples from 25 pounds of apples, and consumes only 2.4 kilowatt-hours per pound of dehydrated apples. This item is likewise kept in abeyance because of the war.

These are outstanding examples of the difference between homemade equipment and manufactured equipment. Numerous types of dehydrators have been designed by various schools, and the designs have been sent to the farmers with the advice that they produce those dehydrators themselves. It is not necessary to dwell upon the fact that during this critical wartime man power on the farm is now very scarce and that the farmer has no time to devote to the production of mechanical equipment, but even in peacetime very few farmers would be interested in producing such a device if a better commercial product were available at a reasonable price. Besides, all of the designs for homemade equipment show electricity consumption much greater than that of the one proposed by the manufacturer. Some of the homemade dehydrators require as much as 5 kilowatt-hours per pound of dried apples as against the 2.4 kilowatt-hours that the manufactured equipment would require. There is no question that there is a market for hundreds of thousands of such small dehydrators if produced in quantity, of high quality, and at a low price. The estimated price of this particular dehydrator is about $36, based on a quantity of 5,000. But other manufacturers likewise are working on the development of a small dehydrator, and no doubt when the market opens up the price will be lower.

The matter of chicken brooders is something for manufacturers as well as for an AIEE committee to study. Here too the number of designs is so great as to be bewildering and make it difficult for a farmer to select the type of brooder he should buy, to say nothing of making one himself. It should be possible now to crystallize a small number of standard designs that will meet all the requirements of the various agricultural schools and individual farmers and prepare some definite specifications under the auspices of the American Standards Association. This and only this will make it possible for reputable manufacturers to go into quantity production of brooders and for the farmers to get the best brooders for the lowest price.

Some studies have been made on soil treatment by application of electricity, and there is a beginning of promising results. In one case, for instance, electric soil treatment eliminated entirely the wild morning glory (bindweed). In another instance a better quality flax was produced after the soil was treated with an electric process. This, too, should be studied by manufacturers with a view toward producing eventually standardized equipment for electric soil treatment. Experiments have been carried on to use electric light for the control of the European corn borer. You know, of course, what a pest the corn borer has been. I have been told that in some parts of the corn belt farmers have stopped raising sweet corn solely on account of the corn borer. These experiments likewise had to stop, because the one who was outstanding in this work is now in the Army, but the work should be picked up again, and standard equipment should be developed that could be sold to every farmer who raises corn.

Some research men have been working on the development of a small pasteurizer

Transmission-Line and System Problems in Supplying Large Electric-Arc Furnaces During Wartime

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ELECTRIC-ARC furnaces are contributing materially to the war effort, and a great number of them, small, medium, and large, have been installed recently, and more are being installed all over the country. These furnaces inherently produce a violently fluctuating load with widely different values in the several phases during the early part of the heat, referred to in the trade as "the meltdown period," and the electric lines and systems to handle such loads must be carefully designed and installed so as to prevent such swinging loads from affecting adversely the system and other critical loads, particularly the lighting, and at the same time to provide sufficient capacity and voltage for satisfactory furnace operation. Also, such lines must be so arranged and connected into the system that the minimum of copper will be utilized—most important now.

This article will discuss some of the basic data necessary to have at hand in the solution of the problem of connecting such loads to an electric transmission system, together with the actual solutions brought about by the installation of 20 furnaces, medium and large, in ten plants of the Duquesne Light Company system.

Briefly, there are five major problems involved, and the last is the most important in time of war:

1. To provide adequate line and/or stepdown transformer capacity to carry the heating load that the furnace imposes thereon.

2. Similarly, to provide adequate line capacity to handle the violent one- and three-phase load swings and deliver usable voltage.

3. To connect such line extensions into the system at a suitable location to avoid: (*a*) subjecting other customers to objectionable flicker, and (*b*) causing objectionable operation of various equipment on the system, such as induction-feeder voltage regulators, governing equipment, tie-line controls, rotating equipment, and so forth. (In time of war, such limitations have been stretched of necessity and in some instances waived entirely.)

4. To reinforce the system and/or lines as required.

5. To use the absolute minimum of copper and other critical materials.

Nature of Furnace Loads

The load of a three-phase electric-arc furnace is of a violently fluctuating nature, as is shown in Figure 2, which is the oscillographic record of the amperes in each of the three phases of the 5,000-kva furnace transformer listed as item 7 in Table I. The section at the left was taken during the early meltdown period and, as can be seen, the arc strikes and goes out repeatedly with only a few cycles intermission, and this continues for an appreciable length of time, 20 to 30 minutes or more. The middle section was taken after the furnace had been in operation for a while and was approaching the end of the meltdown period. Note that the arc does not go out so often nor remain out so long, although in this oscillogram the middle phase does fluctuate quite widely. The section at the right was taken during the refining period and is approximately a balanced load, and during this time the arc does not go out.

There is no system problem in carrying the load during the refining period, but there is a very real problem in providing facilities to carry loads of this nature with reasonable expenditures and with minimum use of critical materials.

Figure 3 shows the frequency of instantaneous three-phase reactive megavolt-ampere swings for various-size furnaces, and in several tests made it has been disclosed that the highest swing occurs only once in a six-hour heat, and that the next ten high swings are less than 80 per cent of this one value. This case was on a 10,000-kva furnace transformer, and we have data on larger units which approximately bear this out. The objectionable zones of voltage flicker previously referred to are more likely to be in the latter range than for the one or two infrequent higher swings.

A study of Figure 3 will show the steepness of the curve for the infrequent swings, and the lower swings, or more infrequent ones, will probably not require so stiff a system and hence probably will require less material. We doubt the advisability of tooling up for the one or two maximum swings during a heat of even one or two hours, let alone for the one large swing occurring in a six-hour heat during wartime.

The larger swings produced by several furnaces in simultaneous operation in one plant occur very infrequently—in fact our investigation discloses that they are of less frequent occurrence than the one high swing of one furnace operating alone.

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for the small farm that could be had at low cost, and the prospects are very promising. This work should have the attention of electrical manufacturers. It should be made possible for the small farmer to pasteurize his milk and sell it in the community.

The possibility of a storage-battery-driven electric tractor has not yet been exhausted. Some experiments were made prior to the depression but were dropped. It is difficult to predict whether or not such a tractor would be a success. It is possible that if the cost of energy for charging the batteries at night is very low a market would be opened for small tractors to be used by hundreds of thousands of small farmers.

There is a decided need for simplification of motor starters for medium-sized motors for farm use and also for a reduction in the price of the starters.

When the war is over a great deal will be known about high frequencies that is not known now, and it is possible that high frequencies ranging from audio frequencies to ultrahigh frequencies may have a great many applications on the farm. It may be possible to apply high frequency for soil treatment, and supersonic waves may possibly be applicable for extermination of pests. This subject calls for much more study by electrical engineers.

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1. New Engineering and a New Industry, M. M. Samuels. Electrical Engineering, volume 61, October 1942, pages 517–20.

Table I. Transmission Supply Lines, Short-Circuit and Voltage Flicker for 20 Furnaces From 1,500 to 15,000 Kva in Ten Plants in Duquesne Light Company System

	Furnace Transformer Data			Supply-Line Data			At Customer's Substation				Volts Regulation on 115-Volt Base and Based on Instantaneously Established Single-Phase Swing		
							At Incoming Bus				At Customer's Substation		
								Reactance in Ohms					
Item No.	Quantity	Largest Transformer (Kva)	Supplemental Reactance Used	Voltage (Kv)	From Network or Separate Line	Figure No.	Three-Phase Short-Circuit (Kva)	Three-Phase	Single-Phase	At High-Voltage Furnace Bus (Reactance in Ohms—Single-Phase)	At Incoming Bus	At Furnace High-Voltage Bus	At Nearest Critical Station Bus
1	3	15,000	No	68	Network	6-*A*	1,179,000	3.925	7.85	24.72*	1.53	4.80	1.37
2	2	10,000	Yes	68	Network	6-*B*	749,000	6.18	12.36	37.02*	2.00	5.98	0.98
3	4	10,000	No	68	Separate**	6-*D*	304,000	15.22	30.44	46.64*	4.92	7.55	1.23
4	1	7,500	Yes	23	Network†	5-*B*	439,000	1.205	2.41	2.41	2.99	2.99	1.97
5	1	7,500	Yes	23	Separate	None	132,000	4.01	8.02	8.02	9.94	9.94	2.75
6	2	6,000	Yes	68	Separate	6-*C*	330,000	14.02	28.04	83.6*	3.52	10.50	1.78
7	1	5,000	Yes	23	Network†	5-*C*	251,000	2.11	4.22	4.22	4.12	4.12	3.74
8	2	4,000	Yes	23	Separate	5-*D*	103,000	5.12	10.24	10.24	8.70	8.70	1.76
9	1	3,000	Yes	23	Network	5-*A*	319,500	1.656	3.31	3.31	2.34	2.34	1.87
10	3	3,000	Yes	11.5	Separate (see text)	4-*A*	364,000	0.363	0.726	0.726	2.02	2.02	1.48

* Includes intermediate transformation between incoming supply lines and the high-voltage furnace bus.

† A separate isolated 22-kv line can be made available by the opening of a remotely controlled oil circuit breaker at the customer's plant if necessary when the furnace loads are swinging.

** A separate line or lines required for line loading and not for furnace load fluctuation.

Pure reactive used for overhead lines and 80 per cent of impedance for cables. All jobs are overhead lines, except item 10 which is all cable.

In specific tests on an installation of four 10,000-kva furnaces in the same plant, the increase of the maximum swings of two furnaces when operating simultaneously melting down over one alone was about 20 to 25 per cent. Similarly for the three similar furnaces over two, the increase was seven to ten per cent additional.

These records of Figure 3 were obtained from high-speed graphic meter charts at three inches or six inches per minute, and it is recognized that there is some error in these charts, because of the fact that the swings take place so rapidly the instrument cannot follow them accurately. The oscillograph is the proper measuring device to use to obtain such records, but it is believed that these curves are indicative of the general relation.

It is also recognized that the lamp flicker, the source of complaints, is the result of single-phase swings, but the single-phase data were not available. However, it is believed that the three-phase data will be indicative of the general relation between single-phase swings of different-size furnaces.

The swings are affected very materially by type of scrap and by how the furnace is operated. For instance, recently we have tested a large furnace which was charged with light scrap loosely packed in, with the resultant swing being about half or two thirds of those on similar furnaces when charged with heavy scrap carefully packed in. Also, the amount of supplemental reactance (the tap connection of the reactor) in use in the furnace transformer has a very material bearing on the magnitude of the swings. Therefore, caution is urged in attempting to use these curves as a criterion of furnace performance, but they will be helpful as a guide.

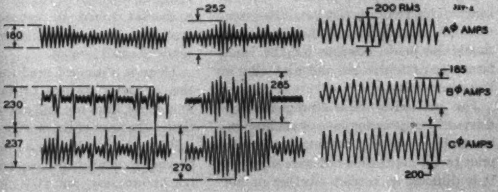
The number and sizes of furnaces and the conditions of the tests for the curves are given below:

Curve A. Four 25-ton 10,000-kva furnaces and one 36-inch slabbing mill supplied by a 37,500-kva 66/11-kv bank, in normal operation. Details are not available. The mill load is relatively steady and has a small effect on the shape of this curve. New furnaces of this size would probably have a normal rating of 35 tons. Data for curves *B*, *D*, *E*, and *F* were taken on same installation. Curve *A* was plotted from $3^1/_3$ hours high-speed meter record over several days in July 1942 and measured at the 66-kv bus at the power station. The customer is supplied by a 4/0 66-kv line 14 miles long.

Figure 1 (left). A typical large modern three-phase electric furnace "pouring"

Figure 2 (below). Oscillograms of the three-phase currents of 5,000-kva furnace during early meltdown, later in the meltdown, and during refining

Note the different degree of stability of the arc



A. Early part of meltdown B. Later during meltdown C. Refining

Curve B. One 25-ton 10,000-kva furnace supplied by a 37,500-kva 66/11-kv bank. This is an estimated curve at ten per cent above curve *D*.

Curve C. One 50-ton 12,000-kva furnace supplied at 24 kv by means of a 4/0 line one-half mile long from a very large step-down bank. Curve plotted from five hours ten minutes high-speed meter record for a full heat in January 1940. Heavy scrap used.

Curve D. One 25-ton 10,000-kva furnace supplied by a 9,375-kva 66/11-kv bank. Curve plotted from high-speed charts covering a six-hour run for one full heat in April 1940. Heavy scrap used.

Curve E. Two 25-ton 10,000-kva furnaces, each supplied by one 9,375-kva 66/11-kv bank. Curve plotted from a six-hour high-speed meter record in September 1940, with simultaneous melting down of the two furnaces for $1^{3}/_{4}$ hours of this time. Heavy scrap used.

Curve F. Four 25-ton 10,000-kva furnaces supplied by a 37,500-kva 66/11-kv bank with three furnaces melting down simultaneously and one refining. Curve plotted from 40 minute high-speed meter record in September 1941. Other spot tests show infrequent swings higher than these, even higher than for two furnaces, curve *E*. Low values—much higher expected.

Curve G. One 15-ton 7,500-kva furnace supplied by a two-mile 4/0 22-kv line which in turn is supplied from a 60,000-kva 66/22-kv bank. Curve plotted from two hours 15 minutes high-speed meter record of the early part of a heat in April 1942. Light scrap and restricted operation.

Curve H. Two 7.5-ton 6,000-kva furnaces supplied by one 9,375-kva 66/11-kv bank. Curve plotted from six hours 50 minutes high-speed meter record in August 1942. Light scrap and reduced operation.

Voltage Fluctuation Limits of Duquesne Light Company

We have peacetime voltage-fluctuation limits of 1.5 and 2.0 volts on the 22-kv and 66-kv systems respectively for fluctuating loads of arc-furnace characteristics, which we used prior to the war as a guide in connecting furnace loads, but these limits have been waived of course since the war started. The voltage flicker limits are more severe for the 66-kv system in certain zones of frequency of occurrence than for other parts of the system, for the reason that the cost to provide corrective measures would be greater than on other parts of the system. Of course, the same perceptible and objectionable zone will exist, but more consumers would be exposed to the voltage flicker, and hence presumably more complaints would result.

By adhering to these limits before the war, we encountered very little objection to voltage flicker from consumers, and the few have been mainly the result of welder installations which were greatly exceeding these limits, rather than furnace installations. In many cases we now permit fluctuations considerably in excess of these values, even though we realize that they are definitely perceptible and, in certain cases, may become objectionable.

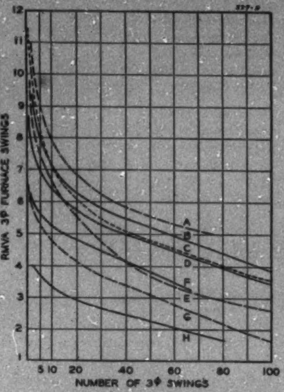


Figure 3. Frequency of instantaneous three-phase reactive-megavolt-ampere swings for various size furnaces, individually and in groups

A = 4–10,000 kva and mill
B = 1–10,000 kva
C = 1–12,000 kva
D = 1–10,000 kva
E = 2–10,000 kva
F = 4–10,000 kva
G = 1– 7,500 kva
H = 2– 6,000 kva

To some, these limits may appear to be rather severe, but our successful experience in applying them, we believe, justifies their retention after the war. We of course exercise judgment based upon experience and stretch these limits in certain cases, even in peacetime. We were considering revising these limits to be more lenient, even before the war, and our experience during wartime will be of considerable use to us in the next revision.

The matter of perception of and objection to voltage flicker is not, in my opinion, subject to exact scientific determination. Much research work has been done on this, and while some consumers will object strenuously to voltage flicker, others would not even be conscious of the same flicker. Some of the best work done on this is covered in a pamphlet entitled "The Visual Perception and Tolerance of Flicker," a report of the subcommittee for project 10, Utilities Co-ordinated Research, Inc., 1937, which, I believe, is available at a reasonable price.

System Regulation

The voltage flicker at the nearest adjacent substation is generally not the governing factor, for this nearest station may be an industrial consumer where considerable flicker can be permitted without encountering objections, whereas a distribution substation supplying lighting may be farther away (electrically) from the furnace than the industrial substation, and yet objectionable flicker may be rendered to the lighting consumers. However, the industrial consumer's substation may be the governing factor, for there may be critical drafting-room lighting requirements or fluorescent lighting in certain processes, or other manufacturing requirements.

The instantaneous swings lasting less than a second (of cycles duration or less) occur more frequently than swings lasting several seconds, the latter reaching the maximum value in several steps, each of which is generally smaller than the maximum swings lasting less than a second. The swings of the shorter duration being more frequent are the final criterion insofar as flicker is concerned, but there is, of course, more total regulation for the swings lasting several seconds, because they reach a higher value.

However, our experience has been, and that of some other power companies bears this out, that the regulation problems of loads lasting several seconds is not of any serious concern, for if you tool up for the instantaneous swings, the condition occurring less frequently causes no complaint.

11-, 22-, and 66-Kv Line Connections for Electric-Arc Furnaces From 1,500 Kva to 15,000 Kva

Table I shows data on the transmission supply lines, short-circuit kilovolt-amperes, and voltage flicker or regulation for 20 furnaces from 1,500 kva to 15,000 kva in ten plants on the Duquesne Light Company system, and there is also included the impedance in ohms, both one-phase and three-phase, at apropos locations.

In three instances, the peacetime limitations are or will be exceeded, and from none of these are complaints being encountered, nor are any expected. Also, it is believed that no serious complaints

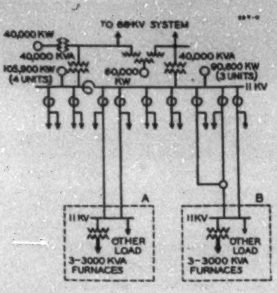


Figure 4 (left). Eleven-kilovolt line connections for electric-arc furnaces of 3,000 kva

Larger furnaces can be supplied by the methods shown in the *A* diagram of this figure, with of course the supply lines being reinforced to carry the heating load of the furnace

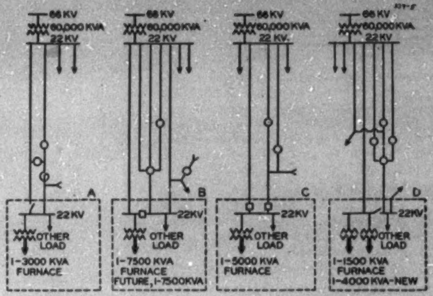


Figure 5. Twenty-two-kilovolt line connections for electric-arc furnaces of 1,500 kva to 7,500 kva

Sixty-six-kilovolt transmission system with 624,000-kw generating capacity

during wartime will be encountered from flickers considerably in excess of those shown. Further, it is felt that values appreciably in excess of those shown would not interfere with war production.

Item 7 in Table I will be much less severe when isolated temporarily as described in the note beneath the table.

11-Kv Line Connections for 3,000-Kva Electric-Arc Furnaces

Figure 4 shows two methods of 11-kv line connections for three 3,000-kva furnaces, and the data for sketch *A* of this figure are given in Table I as item 10. The two lines of 500,000-circular-mil cable, 1.5 miles long, feeding this furnace substation, connect to separate busses at the power station which are supplied through reactors from the main generating source, and the feeder bus also supplies other distribution substations as indicated by the short lines with arrows. The voltage flicker of 1.48 volts at one of these two substations is the limiting point and is less at the 11-kv feeder bus in the power station, that is, 0.44 volt.

The wartime cable rearrangement involved a very small amount of copper.

The corresponding voltage regulation at the power-station feeder bus for sketch *B* of this figure is 0.44 volt and is 1.72 volts at the nearest industrial consumer.

At the time of preparing this paper, one furnace only is in service.

22-Kv Line Connections for 1,500-Kva to 7,500-Kva Electric-Arc Furnaces

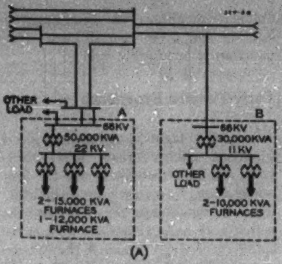
The larger number of our furnaces are connected to the 22-kv transmission network by the lines in that vicinity rather than by an exclusive line from a major transmission stepdown substation. Some of them, however, are connected to a separate exclusive line, items 5 and 8 in Table I being in this category.

Figure 5 shows several recent installations. Diagram *A* of this figure covers the supply to a 3,000-kva furnace and is item 9 in Table I, and the voltage drop at the nearest critical substation bus is 1.87 volts. It will be noted that there is another line normally open to this consumer, and, when one is operating on this second line with the first open, the voltage drop at the nearest critical substation bus will be somewhat greater. The 22-kv lines are 4/0, and the wartime line rearrangements and the waiving of peacetime flicker limits resulted in installing only 800 feet of 4/0 line.

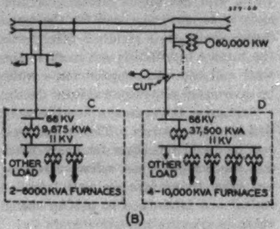
Diagram *B* of Figure 5 covers the supply to one 7,500-kva furnace now in service and another one authorized and is item 4 in Table I. The drop at the nearest critical substation is 1.97 volts. There are other substation busses getting larger voltage drops than this, but they are industrial consumers supplied at 22 kv and are not so critical. The exclusive line is 4/0 and is two miles long. Careful study developed a scheme of handling this furnace load, without any line copper being used, by rearranging the consumer's 22-kv bus in order to isolate the direct line at times of meltdown.

Diagram *C* of Figure 5 covers the supply to a 5,000-kva furnace which has been in service for a number of years and which creates much wider fluctuations than other furnaces of this size and larger. This is item 7 in Table I. The voltage drop on the basis of normal single-phase swings expected from such a size transformer at the nearest critical substation bus is 3.74 volts, and at the 22-kv bus of the nearest major-ring stepdown substation is 2.17 volts. With the abnormal swings that this furnace transformer produces at times because of its unusual impedance characteristics, these values are exceeded, and certain remedial steps were taken shortly after this furnace was installed. These remedial measures consisted of changing the adjustment in voltage-regulator relays on the induction feeder regulators in the nearby substations to permit the higher short-time voltage swings to filter through the relay without causing the regulator to operate, and placing a little time delay in the initiating devices of these induction feeder regulators; also, certain 22-kv line rearrangements were made in this vicinity at that time, and remote control facilities were added to the incoming line breakers at this substation, so that one of them could be opened and the furnace operated on a direct 4/0 line approximately three miles long from the stepdown transmission substation. These remedial measures have eliminated completely the complaints that we received in copious quantities when this furnace was first placed in service. Actually, the consumer is now operating radially on one direct line from the major stepdown transmission substation during the meltdown periods.

Diagram *D* of Figure 5 covers the supply to an old 1,500-kva furnace and a new 4,000-kva furnace. The latter furnace required a separate radial line. The final line arrangement in the district is shown. Pertinent electrical data are covered in item 8, Table I. The old furnace and the mill load were supplied by the 22-kv network in that vicinity, and the old furnace did cause an undesirable but not intolerable voltage condition. For this reason it was reconnected on the new radial line together with the new 4,000-kva furnace. With the furnaces supplied by an exclusive line (1/0 copper and six miles long), the voltage drop at the nearest critical substation bus is 1.76 volts. A bus tie switch is installed between the furnace bus and the bus which is supplied from the network. This was done in order to permit the furnaces to be operated at reduced voltage in case the radial line is out of service for an extended



A. Sixty-six-kilovolt transmission system with 624,000-kw generating capacity



B. Sixty-six-kilovolt transmission system with 624,000-kw generating capacity

Figure 6. Sixty-six-kilovolt line connections for electric-arc furnaces of 6,000 kva to 15,000 kva

period. If normal furnace voltages are used under this condition, the voltage fluctuation at the nearest critical bus in the network is intolerable.

A *new* exclusive furnace line would have required a large amount of critical materials. By consolidating loads in two districts into one district, a 22-kv line was released and extended for service to the furnaces. Actually, 53,000 pounds of copper were saved by the rearrangements as compared with a new exclusive line.

66-KV LINE CONNECTIONS FOR 6,000-KVA TO 15,000-KVA ELECTRIC-ARC FURNACES

The 66-kv method of supply is used for four plants, one of them having the largest furnaces on our system. Figure 6 shows these connections.

Diagram *A* covers the supply for three large furnaces—two 15,000-kva and one 12,000-kva. This is item 1 in Table I. One furnace is in service now, and the others are expected to be in service during the winter of 1942–43. This is an interesting connection, for the mill where the furnaces are located is supplied at 66 kv from a power-company substation approximately 1.0 mile away. A considerable portion of other load of this same consumer is supplied from the power-company bus and from the 66-kv furnace bus in the mill where the furnaces are located, all as shown in this diagram. As a matter of interest there are 60-cycle generators proposed for connection to the consumer's 66-kv lines in one of his mills, and we will operate in parallel with these generators which are scheduled for service sometime in 1943. The two power company 4/0 66-kv lines 0.5 mile in length were installed four years ago for mill load only then, and today for furnaces alone we would supply one line. The voltage drop at the nearest critical substation is estimated to be 1.37 volts.

Diagram *B* covers the supply for two 10,000-kva furnaces which are expected to be in service early in 1943. This is item 2 in Table I. The "other load" shown in this diagram is a very small part of the load and is merely for handling facilities and shop lighting, and this is purely an electric-furnace plant for producing steel ingots and will in no way fabricate or process them. This was a fortunate location as the 1/0 line extension from the main 66-kv transmission will be only approximately 1.5 miles. The voltage drop at the nearest critical substation is estimated to be 0.98 volt.

Diagram *C* covers the supply for two 6,000-kva furnaces. One of these was in service in 1941, and the other early in 1942. This is item 6 in Table I. This location was not so fortunate since it required 4.0 miles of 4/0 66-kv line added to 4.0 miles of existing 4/0 line from the transmission substation bus to reach this. The voltage drop at the nearest critical substation is 1.78 volts.

Diagram *D* is an interesting installation and supplies four 10,000-kva furnaces over a 4/0 line about 14 miles long. This is item 3 in Table I. This furnace load began with one 10,000-kva furnace connected at the point marked "cut" and supplied from the transmission network in this vicinity by a nine-mile line extension. Within five months a second furnace was installed and was carried without any line reinforcing. Then, about a year later two more 10,000-kva furnaces were installed, and this necessitated cutting the line, at the point shown in diagram *D*, from the network and extending it back to a stiffer source, which is now a generating-station bus but which at that time was at a point on the 66-kv transmission. The voltage drop at the power station 66-kv bus for the present connection is 1.23 volts. At the time of the initial installation the voltage drop at the nearest substation was higher, but no complaints were received.

Kilowatt Demand and Kilowatt-Hours

Table II gives these data for various sizes of furnaces, which data are incomplete, for in some cases the kilowatts and kilowatt-hours are totaled along with other mill load, and the remainder are on other systems, and only partial data are available to the writer. Experience and skill, together with other factors listed below, will affect the electrical performance very materially, as well as the tonnage output.

Production of steel by electric melting, and the electrical requirements—kilowatts, kilowatt-hours, and load swings—are governed by many variable factors, some of which are subject to some degree of control; among these important factors are the following:

Table II. Furnace Size, Kilowatt Demand- and Kilowatt-Hours Per Ton

Partial List for Representative Sizes

Item No. in Table I	Furnace Size Net Short Tons	Transformer Rating—40 C (Kilovolt-Amperes)	Kilowatt-Hours Per Net Short Ton Produced	Kilowatt Demand 15-Minute Integration
*		1,500		1,350
10*	3.0	2,500		2,300
9*		3,000		2,200
	6.0		827	
8*		4,000		3,630
	10.0		795	
6	10.0	6,000	604	6,240
4	7.5	7,500	608	7,500
	10		723	
3	25	10,000	580	8,400
	25		569	
	30		601	
	35		585	
	35		569	
	50	12,000	523	
	60		556	

* Foundries. All data not available—see text.

1. Type and size of scrap.
2. How charged—"packed in," and so forth.
3. Quality of finished production—mild steel, alloy, high grade alloy, and so forth.
4. Skill of furnace crew, particularly that of the turn foreman and melting superintendent.
5. Proper size of transformer.
6. Use of proper transformer taps and control adjustment.
7. Design of entire electric installation—furnace, transformer, wiring, and control, the latter contributing very largely to the success of larger arc furnaces.
8. Charging equipment and general layout.

The nominal tonnage rating of an electric-arc furnace used herein does not indicate necessarily the actual production capacity, for, as previously explained many variable factors affect the production, and this is merely a nominal rating for identification purposes, and actually the tonnage produced may be more. Usually, with the larger furnaces for steel making, the nominal tonnage rating, so-called, indicates the holding capacity of the furnace in net tons, whereas with some of the smaller furnaces in foundries the rating is the net ton produced per hour. One large manufacturer has within the last few years ceased to give his furnaces a rating and now identifies them by a type number.

The experience of a considerable number of plants using electric furnaces over a number of years has disclosed that in some installations the furnace transformer was replaced with a larger transformer, since we had had several such cases on our system—some within the last year. This comes about for such reasons as enlarging the furnace, more tonnage requirements, melting different materials, becoming more skilled in the operation, and so forth.

Other Power Companies' Fluctuation

Different power companies have different requirements for the connection of furnace loads. All of the power companies on which I have data indicate that the frequency of the furnace swings and their magnitude govern the amount of stiffness required in the system at the point of service, for these are the criteria of the flicker which will result in voltage complaints if any are incurred. For instance, one power company requires a short-circuit duty at that spot of 1,000,000 kva for connecting a 10,000-kva electric-arc furnace on the basis of limiting the voltage fluctuation to two per cent, assuming the instantaneous swings to be twice the transformer rating. Another power company feels that the short-circuit duty should be 750,000 kva or greater on the basis of a two-volt flicker on a 120-volt base. Some other power companies have installations in satisfactory operation with five to six per cent voltage swings on the consumer's primary bus, and they feel that 7.5 per cent would also be satisfactory, and they make reference to two per cent limits on the system at critical points.

One large power company claims that its test data disclose single-phase swings from approximately 1,500 kva to 6,500 kva for three-phase electric-arc-furnace transformers varying in size from 1,000 kva to 10,000 kva, with corresponding power-factor variations from approximately 40 to 65 per cent. The Duquesne Light Company test data for several comparable installations check fairly closely with these figures.

All and sundry advise caution in supplying lighting, particularly fluorescent X-ray equipment and other critical apparatus from consumer's primary busses and from lines and/or busses that supply such critical load.

One large power company is fortunate in having most of its heavy industrial load confined to a definite area and has provided a separate bus known as "flicker bus," from which all such loads are supplied. Such a bus can stand much greater voltage fluctuations than one carrying lighting and other critical load. Not many are so fortunately situated.

Early Furnace Experience

Duquesne Light Company has been supplying electric-furnace loads since 1908, and, it is thought, it was the first power company to pick up such load. Even in those days the necessary research work and calculations were made before connecting a furnace to the system, and a trial installation was actually made of the first furnace in the power-station yard, with one engine-driven generator isolated to carry the load for the tests. Subsequently, as a result of these tests, the furnace installation was made in the steel mill and was supplied for a while by an isolated generator and line. As the system grew, the generator and line were tied into the network. This installation was in use in this plant for many years and was scrapped just a few years ago, but other larger furnaces were later installed and have been in use for many years.

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Insulation Testing of Electric Windings

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I. Introduction

THE paper describes a new method for the testing of insulation of windings, the advantages of which are adequate stressing of turn and conductor insulation and certain detection of defects. The new tester consists of a repeating-type surge generator, cathode-ray oscillograph, and synchronously driven switching equipment. The surge generator provides a succession of voltage waves of controlled shape adjusted to give the necessary turn and conductor insulation stresses. The cathode-ray oscillograph is timed to give a stationary image of the repeating surge on the screen. By virtue of the special method of surge application and oscillograph connection, the waves on the screen indicate turn or conductor-insulation defects quickly and certainly.

In high-potential testing, voltages in all parts of a winding should be high enough to result in insulation failure at all points where insulation strength is not adequate to withstand service conditions. To this end, test voltages must be used sufficiently above normal excitation voltages to properly stress conductor insulation and sufficiently rapid in rate of change to stress turn insulation properly. Accordingly, power-frequency voltages, usually two times normal plus 1,000, are used for conductor-to-ground insulation and high-frequency voltages applied directly or by induction are used for turn insulation.

In service, insulation must withstand normal voltage excitation, switching voltages, and other transients such as lightning.[1] Switching surges range below 5.5 times normal but may, as in the case of lightning transients, have high rate-of-change characteristics. Time to crest may be as low as one microsecond. As nearly as practical these service voltage conditions should be reproduced in test.[2–13] The test method described in this paper is directed toward the ends of adequate stressing of all insulation and certain detection of weak spots.

The new method has been used for different types of coils and windings and has been applied to particular advantage in the testing of completed windings of three-phase low-voltage rotating machines. In this case surge voltages are applied alternately to each of two of the terminals of the machine with the oscillograph connected to the third terminal.

II. Wave Propagation in Machine Windings

Experimental work and practical testing experience on production machine windings have indicated definite improvements in line with the objectives already stated. As very little experimental work has been reported on wave propagation in windings, it seems advisable to include here a few oscillographic records of voltage wave conditions pertinent to the surge performance of machine windings.

A most significant characteristic of these windings from the standpoint of surge mechanism is that capacitance and inductance elements are necessarily well distributed as to physical position and, therefore, have very low over-all capacitive or inductive coupling. In this report the machine winding is the opposite of the transformer where over-all capacitive and inductive couplings are very high. Because of this distribution of capacitance and inductance elements, the electrical mechanism of the machine winding is that of the traveling-wave as contrasted with the standing-wave mechanism of the closely coupled transformer winding. The surge performance of a machine winding is illustrated in Figure 1 which shows voltage wave shapes measured at suitably located tap points along one leg of a three-phase winding of a large rotating machine. The voltage wave applied was unidirectional in polarity with a time to crest of one microsecond and to half-crest on the wave tail of 5.6 microseconds.

In Figure 1 in the left vertical column are shown the tap voltages for the case of the neutral end open. The voltage wave shape applied is at the top with consecutive taps toward the open end in order below. The unidirectional polarity impulse traveling through the winding sets up a standing-wave oscillation at the open end which has a frequency of some 34,000 cycles per second. Voltage amplitude along the winding first decreases, then increases, reaching a value some 20 per cent above that of the applied wave at the line end. The low-amplitude oscillation at the beginning of the neutral end wave is the result of direct coupling across the winding, as soon as the applied wave enters the winding at the line end. The winding time length, as indicated by this displacement, is just under 4.0 microseconds.

In Figure 1, center vertical column, are shown tap voltages for the case of the neutral grounded, through a resistance of 200 ohms equal to the experimentally determined surge impedance. This series of oscillograms shows the voltage wave traveling through the winding without major reflections and with appreciable amplitude attenuation so that the neutral end wave is about 60 per cent of the applied wave. This attenuation is accompanied by some distortion resulting in a slight lengthening of the wave tail. The loss in voltage amplitude is mainly on the sharp front of the wave, and this has the effect of increasing the time to crest from one microsecond at the line end to five microseconds at the neutral end.

In Figure 1, right vertical column, are shown tap voltages for the case of the neutral end directly grounded. In this case the polarity of the reflected voltage wave is reversed, and a frequency of 77,000 cycles or something more than twice the open-ended winding frequency is obtained. The voltage amplitude decreases at each tap point and at the neutral end is, of course, zero across the ground connection.

These oscillograms show the nature of the voltage variations in a rotating machine winding under conditions of applied surges such as are obtainable with switching, lightning, or other circuit variations. Further experimental work has shown that steeper and short-duration waves are attenuated more rapidly, and more sloping and longer-time waves less rapidly. This type of information can be used as a basis for consideration of voltage stresses and detection of weak spots in winding-insulation testing.

III. Winding-Insulation Testing Method

Experimental work was conducted on improvements in winding-insulation testing, particularly as regards across-winding stresses and weak-spot detection in view of the surge mechanism already described. As a result of this work, the circuit arrangement and method of connection derived from the oscillograph electric transient analyzer[3] and shown in Figure

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Figure 2. Schematic diagram of winding-insulation tester

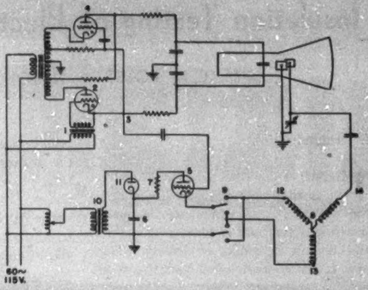


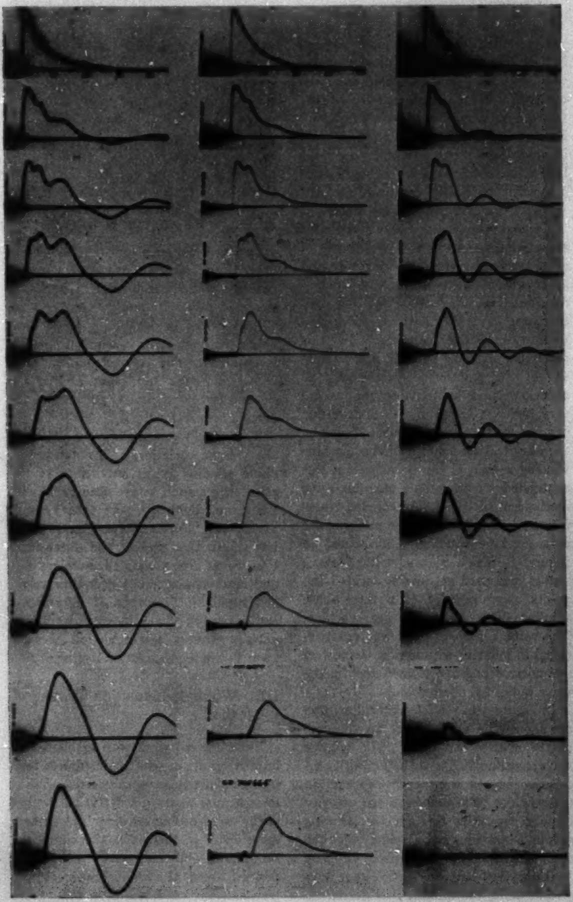
Figure 1. Wave propagation in machine windings

Applied line surge voltage at top. Neutral surge voltages at bottom

Left—Terminus open

Center—Terminus grounded through surge impedance

Right—Terminus grounded directly

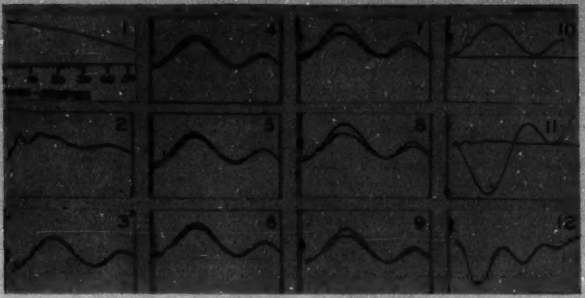


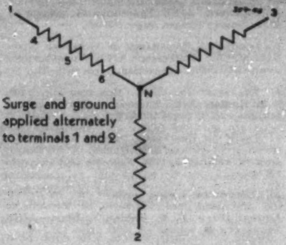
2 was devised. The principal elements involved are the repeating surge generator at the bottom left, a cathode-ray oscillograph at the top right, the time-co-ordinating and beam time-axis circuit at the top left, and a synchronous reversing switch shown at the bottom right, along with a typical wye-connected winding of rotating machine. The surge generator consists of supply transformer 10, charging tube 11, main capacitor 6, and discharge resistance and tube 7 and 5. This circuit provides a capacitor-discharge voltage wave which is applied through a synchronously operated reversing switch 9 to the machine windings 12 and 13 under test. Time-co-ordinating circuit 1, 2, 3, and 4 supplies time-axis voltage for the cathode-ray oscillograph and operates the surge generator once for each cycle of power-supply frequency, which results in a stationary image of the fault-indicating wave on the oscillograph screen. For practical testing purposes, this equipment is mounted in a cabinet as shown in Figure 3 with the oscillograph at the top and all controls below.

In detail the whole circuit operates as described in the following paragraphs.

The peaking transformer 1 trips the sweep thyratron 2 when its plate is on the crest of the positive half-cycle, thereby

Figure 3. Winding-insulation tester, 10 kv





Record	Fault	Location	Oscillograph
1	None		1
2	None		N
3	None		3
4	One turn short circuit	4	3
5	One turn short circuit	5	3
6	One turn short circuit	6	3
7	One coil short circuit	4	3
8	One coil short circuit	5	3
9	One coil short circuit	6	3
10	Ground	1	3
11	Ground	5	3
12	Ground	N	3

Figure 4. Winding-insulation tester records, single winding

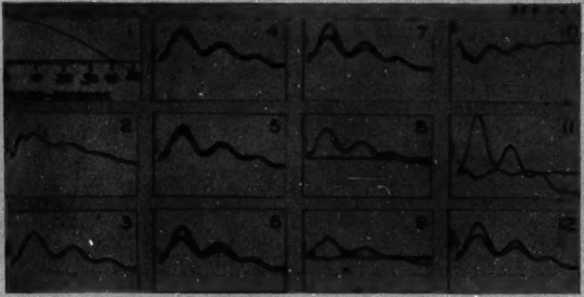
applying a positive impulse to point 3. This positive impulse, in addition to providing one-half the sweep voltage, also trips the tube 4 (which gives the negative half of the sweep), and the large thyratron 5. Tripping of 5 discharges capacitance 6 through resistance 7 into the test winding 8 by way of the synchronous reversing switch 9. Capacitance 6 was charged on the previous half-cycle from transformer 10 through rectifier 11.

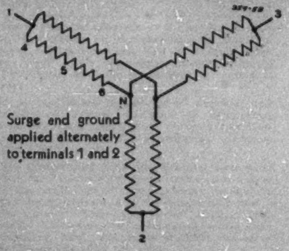
The synchronous switch acts to send alternate surges in opposite directions through the winding 8 between terminals 12 and 13. Terminal 14 is connected through a capacitance voltage divider to the oscillograph deflection plates. The wave shape shown by the oscillograph is, therefore, a resultant of the voltage at the neutral and the voltage induced in the phase to which it is connected.

If the winding is perfectly symmetrical, the wave at the neutral and on the oscillograph will be the same for either direction of surge travel, so that a single standing wave will be shown on the oscillograph screen. If the symmetry is disturbed by a shorted turn or coil, in legs 12 or 13, a ground or improper connection, the two directions of surge travel will give different results and, consequently, two waves of different shape on the screen. Experience has shown that this connection arrangement is extremely sensitive to winding defects and that, to a considerable extent, the relative shapes and amplitudes of the two waves can be used to indicate the nature of the defect.

IV. Specimen Winding-Insulation Tester Records

The oscillograms of Figure 4 show winding-insulation tester performance on a three-phase rotating machine winding. The tester circuit was arranged to give a voltage wave of (1 x 40) microsecond shape. This wave, shown at 1, was recorded by connecting the oscillograph deflection circuit to the impulse input terminal 1 in the diagram. Oscillogram 2 is the wave shape at the neutral of the motor winding obtained by connecting the oscillograph deflection at the motor neutral or point *N* on the diagram. It will be noted that the wave front at the neutral is considerably more sloping than the applied wave of 1, requiring some six microseconds to crest value. Only one trace is shown, indicating identical wave shapes, whether the wave is applied at 1 with 2 grounded, or applied at 2 with 1 grounded. Oscillogram 3 of Figure 4 shows the wave shape at the end of winding 3 with the characteristic standing-wave voltage oscillation agreeing with oscillogram of Figure 1 at the bottom of the left-hand column. Oscillogram 3 also shows only one wave, thereby indicating symmetrical conditions in phases 1 and 2. For oscillograms 4 through 12, the oscillo-





Record	Fault	Location	Oscillograph
1	None		1
2	None		N
3	None		3
4	One turn short circuit	4	3
5	One turn short circuit	6	3
6	One coil short circuit	4	3
7	One coil short circuit	6	3
8	Ground	1	3
9	Ground	5	3
10	Ground	N	3
11	Reversed phase	1 to N	3
12	Reversed coil	N	3

Figure 5. Winding-insulation tester records, double winding

graph deflection is connected to terminal 3, which is normal for production-line testing of windings.

To illustrate the detection of winding faults, turn and coil short circuits and grounds were made at several points in leg 1 and oscillograms taken of the standing waves on the oscillograph screen. Oscillograms 4, 5 and 6 show the two wave traces produced by one-turn short circuits at different points in leg 1, as indicated on the diagram at 4, 5, and 6.

The two waves in each case are offset from each other sufficiently to constitute an unmistakable indication of the winding faults applied. In the same manner oscillograms 7, 8, and 9 show the offset waves obtained with a single-coil short circuit the differences in waves in each case being greater than for the single-turn short circuit. By stopping the synchronous motor and sending all impulses into terminal 1, and viewing the wave, and then into terminal 2 and viewing the wave, it has been determined experimentally that these waves are above and below the no-fault wave of oscillogram 3.

Oscillograms 10, 11, and 12 of Figure 4 show wave shapes corresponding to applied line-to-ground insulation faults at points 1, 5, and *N* of the motor winding. Oscillogram 10 taken with the ground on terminal 1, of course, shows a zero deflection line for the wave applied to that terminal. The single wave of 10 produced by the surge entering terminal 2, corresponds to the double wave of oscillogram 3.

Oscillogram 11 also shows one low-amplitude wave corresponding to the impulse applied at 1 with point 5 grounded and a high-amplitude wave corresponding to the impulse applied at terminal 2. For oscillogram 12 the neutral was grounded, and the waves are superimposed because of the symmetry of this connection arrangement.

These oscillograms and those of Figure 5 which show wave shapes for a double-circuit wye connection illustrate the wide range of surge-voltage variations corresponding to possible insulation faults and connections. A detailed treatment of these is not possible within the scope of this paper. However, stated generally, these wave shapes are determined by the propagation characteristics of the impulse and the magnitudes and directions of induced voltages in the leg connected to the oscillograph. The significance of all this in insulation testing of windings is to be found in the clear-cut manner in which insulation and connection faults are disclosed by the two-way method of surge application and the standing waves on the cathode-ray oscillograph screen.

V. Conclusions

1. A new winding-insulation testing method for completed machine windings has been described.

2. The new method applies suitably controlled voltage wave shapes to give the required turn and conductor insulation stresses for adequate testing practice.

3. The new method provides for immediate detection of insulation faults and winding dissymmetries.

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Generation of Electric Charges by Moving Rubber-Tired Vehicles

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Synopsis: The current flowing to ground from an automobile, whose rear wheels were turning on a dynamometer, was measured. The magnitude of the constant current measured, in the order of microamperes, shows that the vehicle is electrically charged by conduction through the tires. The experimental results are consistent with the conception of a simple equivalent circuit. These results were checked by road tests. Calculation shows that the tires are usually conducting sufficiently to discharge a car in a short time after it has stopped moving.

COMMON experience demonstrates that a rubber-tired vehicle in motion often accumulates a charge of electricity of sufficient magnitude to shock a person who touches the vehicle immediately after it comes to rest.

It is known that this electric charge is produced when the rubber tires contact pavement and then separate from it. This phenomenon has recently been studied by Beach,[1] Cadwell, Handel, and Benson[2] and by Liska and Hanson.[3] These investigators studied the potentials developed and potential distributions around the tire. Most of our experiments were confined to the measurement of currents generated, rather than voltages.

Theoretically there are two methods by which a vehicle can acquire a potential above ground caused by tire static. One method is by induction from the charges produced on the rubber tires. The other method is by the conduction of this charge through the rubber tires to the body of the vehicle. We have measured continuous currents, as high as four microamperes, flowing to a grounded conductor from a car, under normal driving conditions. This continuous current can be produced only by conduction through the tires. It is well known that any charges produced on the body of the car by induction from the charges on the tire, will produce only a *momentary* current when the vehicle is grounded, and cannot possibly maintain a steady current to ground.

Inductive effects due to the existence of charges on the surface of the tires can produce the distribution of charges on the body of the vehicle that is necessary to maintain the body at one potential. The potential, however, is determined entirely by current flowing through a resistance.

Experimental Results

It is difficult to make accurate measurements of currents generated by moving vehicles on the road. Accordingly, in the attempt to determine the currents generated, the first experiments were conducted on an engine-testing dynamometer stand. This device is essentially a large Prony brake. The rear wheels of the automobile rest on a large metal drum that is free to revolve at a rate corresponding to any desired road speed.

In making measurements of currents generated, the body of the car was grounded through a microammeter, the currents being large enough to come within the range of an ordinary instrument.

The rubber tires of a moving vehicle in contact with the pavement produce a difference of potential between the rubber and pavement, and a separation of electric charges occurs. The tire always acquires a negative charge. As the tires move from the contact position on the pavement, or on the dynamometer, the potential of the charges must increase. As the potential increases, the charges seek a path to ground. Our experiments have led us to the conclusion that two paths to ground are followed:

1. Directly across the tires to the wheel hub and the car body, and from the car body to ground through the ammeter.
2. Back along the surface of the tire to the grounded dynamometer, or, in the case of a moving vehicle, to the ground plane existing under the pavement.

Thus, the equivalent circuit of a car on a dynamometer stand may be represented by Figure 1. The current I generated at the tire tread has two paths to ground:

1. To the wheel through the resistance of the tires, R_w, to the car body and through the ammeter to ground.
2. Back to ground across the surface of the tires, through a shunt resistance, R_s.

Figure 2 is a typical curve showing the measured current plotted against speed of the rear wheels. This current flows from the body of the car to ground when the rear wheels revolve on the drum of the dynamometer. The data were taken with a 1941 Chevrolet having the standard 6:00–16 tires. Various pressures were used, and a wide range of tractive loads was applied. In general, we found only a small increase in current with a considerable increase in load and only a small increase in the current with a large decrease in tire pressure. These results indicate that the charges produced are not determined primarily by the amount of

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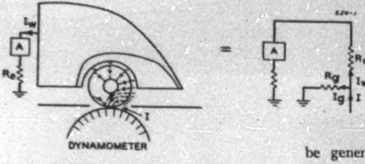


Figure 1. Equivalent circuit of a car on a dynamometer stand

friction, but rather by the speed and separation of contact.

The curve of Figure 2 is not a straight line but shows some decrease in the rate of production of the measured current at higher speeds. We believe that the total charge produced at the surface of the tire is directly proportional to speed at all the speeds indicated and that this falling off of current measured at high speeds is due to changes in the equivalent resistances, R_g and R_w, shown in Figure 1.

With increased speed it is to be expected that the tire temperature will increase. Rubber, as compounded and used in tires, has a negative temperature-resistance coefficient. Therefore, the tread and the surface of the tire near the tread, due to its higher temperature, will decrease in resistance more than the main body of the tires. Since most of R_g resides in the tread and in the portions of the tire near the tread, R_g decreases more than R_w as the speed increases. A greater portion of the total current generated thus goes to ground through the shunt path, and the readings of the ammeter flatten out.

Figure 3 indicates that an increase in temperature will change the ratio of resistance R_w to R_g. This curve shows the current measured at a constant speed, plotted against time. Before this run, the tires were allowed to cool. There is a decrease in measured current for about a minute, and then a substantially constant value is reached. This manner of change of current measured is explained by assuming that the ratio of R_g to R_w decreases as the temperature of the tires increased. It is difficult to explain the observed change in current by anything other than the change of tire resistance with time.

The experiment that indicated the existence of two paths to ground, as illustrated in Figure 1, was the following: A conducting mixture of lampblack and Karo syrup was applied to the side walls of the tires. The conducting film extended from the metal wheel hub to within an inch of the outer edge of the side walls of the tires. The tread was not coated, so that the charge-producing mechanism was not affected, and the same total currents as previously produced would be generated at the tread of the tire. When the wheels were now set in motion, much higher currents to the car than previously encountered, were measured.

On the assumption that the total current generated at the tread of the tire is unchanged at a given speed, these results indicate that the total current must split into two paths, as shown in Figure 1, and that the ammeter reads only a portion of the total current generated.

Currents as high as eight microamperes at 40 miles per hour were obtained with a relatively thick coating of the conducting mixture. The resistance of the side walls was so decreased that two electrodes spaced three inches apart, indicated a resistance of approximately 100,000 ohms.

Furthermore, it is impossible to determine directly the total current generated at any particular speed. This can be seen if the circuit elements of Figure 1 are considered to constitute a Π network. From four-terminal network theory, it is known that three independent measurements must be made to determine the characteristics of the circuit. This means that two measurements must be made on one side of the network and one on the other. This is physically impossible, because no meters can be connected to that side of the circuit where the constant current I is being injected into the system.

However, if external resistances are inserted in series with the ammeter, and the current change measured, certain interesting results may be obtained. It is readily determined that

$$\frac{1}{I_w}=\frac{1}{I}+\frac{R_w}{IR_g}+\frac{R_e}{IR_g},$$ where R_e is the unlabeled external resistance in Figure 1.

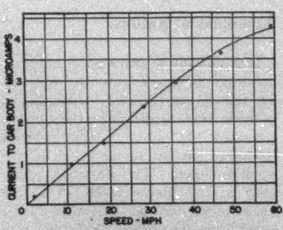


Figure 2. Typical curve of current measured from body of car to ground, as speed of rear wheels is changed

Thus, the reciprocal of the measured current for a given total current I, that is, for a constant speed, should vary linearly with the external resistance. The slope of the line is

$$\text{slope}=1/IR_g$$

and the intercept is given by

$$\text{intercept}=\frac{1}{I}\left(\frac{R_g+R_w}{R_g}\right)$$

and therefore

$$\frac{\text{intercept}}{\text{slope}}=R_g+R_w$$

Figure 4 shows the results of readings taken at a wheel speed corresponding to 40 miles per hour. Extremely high resistances had to be added in the ammeter circuit to obtain these data. It is seen that a straight line, predicted by analyzing the equivalent circuit, is actually obtained. This curve gives a value of $R_w + R_g = 1{,}325$ megohms. Further data, taken with the side walls made conducting, also yield a straight line; in this case, $R_w + R_g = 34.6$ megohms.

Without a knowledge of the total current I, it is not possible to determine the exact value of R_g and R_w. However, an idea of the order of magnitude can be determined in the following manner: as mentioned, with a relatively thick conducting layer, currents as high as eight microamperes at 40 miles per hour have been measured. We may assume that this is approximately the total current generated at this speed, since without any conducting layer, and with no external resistance, the measured current is 1.74 microamperes.

The slope of the curve of Figure 4 is $1/IR_g = 0.43 \times 10^{-3}$. Substituting the value of $I = 8 \times 10^{-6}$, we get $R_g = 290$ megohms. Since $R_w + R_g = 1{,}325$ megohms, then $R_w = 1{,}035$ megohms.

These results give the resistance of the two rear wheels in parallel. Consequently, the equivalent resistances for one tire would be $R_g = 580$ megohms, and $R_w = 2{,}070$ megohms.

This determination of R_w is in the range of the resistance between the hub of a wheel and a metal plate on which a tire is resting. The authors have made such measurements,[4] as have Cadwell, Handel, and Benson.[2]

If we assume that the total current generated by the tires is proportional to speed, we obtain, on the assumptions previously stated, a "proportionality constant" of approximately 0.1 microampere per mile per hour for each tire.

It will be noted that the value shown on

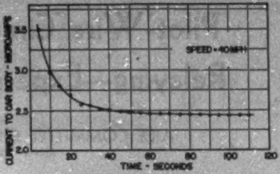


Figure 3. Variation of current to car body with time, wheel speed being held constant at 40 miles per hour

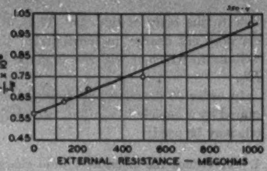


Figure 4. Reciprocal of current measured versus external resistance in ammeter circuit

Speed—40 miles per hour

Figure 4 for the current measured at 40 miles per hour with $R_e = 0$, is 1.7 microamperes, or approximately one half of the current for this speed shown on Figure 2. Such changes were encountered over a period of time, and were probably caused by changes in the surface conditions of the tires.

Voltages Encountered

The slope of Figure 4 is given by $1/IR_g$. Accordingly, the reciprocal of the slope is the voltage to which the vehicle would be raised if all the current were dissipated to ground through the shunt resistance. This situation is approximated when extremely high external resistances, R_e, are used. For the data taken with the normal tire, this value is

$$V=(1/0.43)\times 10^3=2{,}320 \text{ volts}$$

During these experiments voltages of this size were measured, and Beach[1] likewise encountered voltages in this range, although somewhat higher. If an electrostatic voltmeter is applied to the car while on the dynamometer, instead of an ammeter, and one side of the voltmeter is grounded, then R_e will be the resistance of the front tire and the surface leakage over the floor to ground.

We made tests in which a layer of tinfoil was placed under the front tires, the tinfoil being insulated from the floor by rubber mats. The tinfoil was then grounded through the microammeter. As soon as the rear wheels started to turn, the microammeter indicated a current of about half the size previously encountered and a voltmeter connected between the car body and ground read several thousand volts. This, as stated above, is equivalent to inserting a very high resistance R_e in the ammeter circuit of Figure 1.

Those who previously made measurements of voltage, in attempting to analyze the problem of the tire static, were performing this experiment, without realizing that the voltages measured could be correlated directly with an *IR* drop through the front tires.

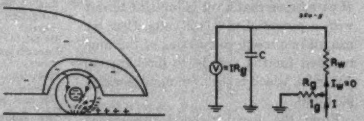
Table I. Data Obtained on Road

Speed (Miles Per Hour)	Current Measured (Micro-amperes)	R_e Meg-ohms	Remarks
10	3.0	0	All tests were made on an asphalt pavement
20	4.8	0	
10	2.5	100	
10	4.5	0	Conducting carbon layer on all four tires
10	1.5	0	Both right tires on grounded railroad track
10	2.0	0	Conducting carbon layer on right two tires, on railroad track
10	1.5	60	Conditions as above

Road Tests

Data were also taken with vehicles actually in motion. However, it was rather difficult to get good readings. First of all, other cars passing nearby would induce temporary currents of the order of magnitude of those being measured. Mechanical meters, sensitive enough to read the current generated, were not rugged enough to stand the inevitable shocks received in a moving vehicle. Electronic meters were too susceptible to outside influence, such as power lines, and so forth. Despite this, the data obtained checked well with what might be anticipated from the dynamometer tests.

Figure 5. Equivalent circuit of a car moving on a highway



In the road tests, a chain about ten feet long was dragged in a railroad track, serving as a constant ground. The chain was insulated from the car by means of a wax-impregnated rope which also served as the strain absorber. The wire leading to the ammeter inside the car was insulated from the metal of the car by vacuum-held rubber cups. Accordingly, measurements could be made corresponding to I_w of the dynamometer experiments.

The most reliable data for the operating conditions detailed are listed in Table I. It is seen from this table that the order of magnitude of currents generated is that obtained on the dynamometer. Closer correlation is scarcely to be expected. The materials producing the charges were different: that is, on the dynamometer the rubber contacted metal, while in these tests the rubber contacted asphalt paving.

We may see further, from the data taken with the right wheels in the grounded track, that reducing the value of the leakage resistance to ground will reduce considerably the amount of current going to the car. On the dynamometer stand, the path R_g, as mentioned previously, was the rubber between the charges and the grounded dynamometer, while on the road, the pavement resistance is added. Therefore, driving on the tracks should reduce the current to the car, since the leakage path to ground is now one of lower resistance. With the wheels on the track, additional resistance in the grounded part of the circuit has a much greater effect than if the wheels are not in the track, as may be seen from entries 3 and 7 in Table I. Figure 1 shows why this should be so. With the wheels in the track, R_g is relatively small; hence putting in additional resistance R_e will force more of the generated current through the leakage path to ground, thus decreasing the ammeter reading.

It is believed that the potential of a car in motion is determined primarily by the current produced by tires leaving the road. Other effects such as wind resistance and so forth, are believed to play only a minor role.

Conclusion

Figure 5 shows the equivalent circuit for a car moving on a road with uniform speed. In this circuit the capacitor *C* represents the capacitance of the car to ground. Obviously, once enough current has flowed through R_w to charge *C* up to a voltage equal to IR_g, then all the current will flow back to ground, and no more will flow to the body of the car. Thus, the car does not charge up indefinitely; the voltage to which it will rise is given by IR_g.

In order to decrease the potential acquired by a vehicle in motion, two methods may be used:

1. To reduce the amount of total charge produced. A method of doing this has already been proposed.[2]

2. To use tires with conducting tread and side walls. This would reduce the resistance R_g and, consequently, decrease the vehicle's potential.

Experience has shown that very seldom is a car sufficiently charged to give a static spark when driven into a service station. Even if we assume that a vehicle is brought to rest with a relatively high charge, this charge quickly leaks to ground. We have measured the resistance of the tires of a number of cars when at rest and found this resistance to be approximately 3×10^9 ohms for four tires in parallel.[3,4] The capacitance of a car is approximately 750 micromicrofarads. If we assume that a car is brought to rest with a potential of 3,000 volts, then we can make a rough calculation of the time required for the vehicle to discharge to 300 volts, which is the minimum sparking potential.

From the usual capacitor-resistance discharge equation

$$V = V_o e^{-\frac{t}{RC}}$$

Substituting $V = 300$, $V_o = 3{,}000$ $R = 3 \times 10^9$, $C = 750 \times 10^{-12}$ we find $t = 5.2$ seconds.

Furthermore, we have found in the laboratory that a capacitor of 750 micromicrofarads will produce a spark capable of igniting a combustible mixture of gasoline vapor and air, only if charged to a potential of several thousand volts. A spark may occur at a lower voltage, but it lacks the characteristics necessary to ignite a combustible mixture of gasoline vapor and air.

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Pilot-Wire Circuits for Protective Relaying—Experience and Practice

AIEE COMMITTEE ON PROTECTIVE DEVICES
RELAY SUBCOMMITTEE

BECAUSE of the increasing use of pilot-wire relays, particularly for longer lines, the relay subcommittee initiated an investigation of the operating experience with the pilot channel itself. From this investigation it was hoped to determine the requirements for a reliable pilot channel. Unfortunately, the information obtained to date is not conclusive, and under war conditions the completion of this investigation appears to be delayed unavoidably. On the other hand, plant expansion necessary to the war effort frequently has called for quick decisions on relay schemes, and pilot wires have been used in many cases. Because the pilot-wire channel is the least known item in the pilot-wire relay scheme, the committee has decided to publish the information now available to indicate the trend, so that prospective users of pilot-wire relaying may have the benefit of this preliminary work. Investigation will be continued as actively as conditions permit and another report issued as soon as sufficient data are available.

Table A

Protection at Terminals	Privately Owned Circuits	Leased Circuits
Carbon blocks	6 circuits of 2 companies	41 circuits of 9 companies
Air gaps	10 circuits of 2 companies	1 circuit of 1 company
Thyrite	7 circuits of 3 companies	1 circuit of 1 company
Protective tubes	17 circuits of 2 companies	None
Vacuum gaps	4 circuits of 2 companies	None
Fuses	16 circuits of 6 companies	19 circuits of 4 companies
Heat coils	2 circuits of 1 company	6 circuits of 2 companies
None	320 circuits of 10 companies	10 circuits of 2 companies
Not specified	1 circuit of 1 company	1 circuit of 1 company
Protection at Intermediate Point		
Carbon blocks	2 circuits of 1 company	48 circuits of 10 companies
Air gaps	7 circuits of 1 company	None
Fuses	6 circuits of 2 companies	15 circuits of 3 companies
Heat coils	2 circuits of 1 company	24 circuits of 7 companies
None	96 circuits of 6 companies	None
Not specified	273 circuits of 14 companies	1 circuit of 1 company

It should be remembered that the recent increased use of pilot-wire relay schemes has been occasioned by the development of new relays to give both phase and ground-fault protection over only two pilot wires. Because of the short history of these new developments, this report necessarily gives a rather incomplete picture of their performance, since it is influenced disproportionately by the much longer period of use of the older schemes. Further, it is probable that, in some of the cases where phase or ground-fault protection only is reported, the circuits would have been protected for both, had the newer schemes been available at the time. Also, the use of the newer schemes probably will increase the average length of pilot-wire circuits, because of their ability to operate over longer distances.

This report is based on the replies of 32 companies covering 436 circuits used for protective relaying. The information thus obtained covers 1,351 route miles of pilot-wire circuits and a period of 5,570 circuit years.

The following information, some of which is elaborated on later, would appear to be particularly significant.

1. Eighty-seven per cent of the circuits are privately owned, nearly all of which are in lead-sheathed cable. The average length is three miles. The longest circuit is 26 miles.

2. Two thirds of the circuits are installed on the same right of way or in the same duct

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Paper 43-27, recommended by the AIEE committee on protective devices for presentation at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943. Manuscript submitted November 12, 1942; made available for printing December 29, 1942.

Personnel of the working group on pilot wires of the relay subcommittee: W. K. Sonnemann, sponsor; J. H. Neher, former sponsor (now with the United States Navy); J. C. Bowman, W. R. Brownlee, R. B. Cordray, R. F. Davis, L. L. Draper, E. E. George, S. Goldsmith, E. L. Harder, H. F. Lindemuth, W. E. Marter, P. N. Sandstrom, A. W. Walton, E. M. Wood.

Table I. Circuit Details and Performance of Leased Pilot Circuits

	Company Designation														
	4	5	9	13	15	16	19	21	22	23	24	26	28	29	32
Number of pilot circuits	4	7	11	4	1	1	1	1	2	1	8	10	1	4	2
Average length (miles)	6.4	2.4	6.3	4.6	1.5	7.4	8.0	12.1	2.4	4.6	10.6	15.1	6.0	5.0	*NS*
Maximum length (miles)	10.5	3.5	13.0	7.6	1.5	7.4	8.0	12.1	3.5	4.6	17.8	26.9	6.0	6.0	*NS*
Pilot-circuit performance															
Mile-years of operation	68	15	103	17	0	7.4	0	0	28	0	527	121	12	8	1
Total number of outages	15	0	79	23		1			12		59	9	2	2	0
Total hours interruption	15	0	140	142		4			55		167	28	500	2	
Caused by Short circuits			5	2					*NS*		*NS*	5	1	1	
Caused by Grounds	4					1			*NS*		*NS*		1		
Caused by Open circuits	10		2	4					*NS*		*NS*	3			
Caused by Circuit changes		[c]	51	16					*NS*		*NS*				
Caused by Other causes	1		[d]21	[f]1					*NS*		*NS*	[f]1		[f]1	
Protective features[a]															
At terminals	*F*	*C*	*C*	C and *F*	*T*	*C*	*C*		*C* and *H*	*C* and *F*	None	*C* and *F*	*AG*	*C* and *H*	None
Voltage level		350	300	300		350	1,000		350	350			4,000	300	
At intermediate exchanges	*C* and *H*	*C*	*C*	*H*	*C* and *H*	*C* and *H*	*C*		*NS*	*C* and *F*	*C* and *H*	*C* and *F*	*AG*	*C, F,* and *H*	*H*
Voltage level	350	350	300			350	1,000			350	350		4,000		
Gradient protection[b]	None	4-*I* 3-*C*	11-*N* 4-*C*	4-*C* 2-*D*	*N*	*C*		*N*	*I*	*N, C,* and *I*	8-*I* 2-*C*	3-*N* 9-*C* 1-*I*	*C*	*I*	*C* and *I*
Insulation level (volts)		1,500 10,000	5,000	1,000	15,000				600		1,500 10,000		High		
Automatic supervision	Yes	No	Yes	2	Yes	No	Yes	Yes	Yes	Yes	Yes	Yes	No	No	Yes
Special circuit marking	Yes	Yes	Yes	Yes	Yes	Yes	Yes	?	Yes	Yes	Yes	Yes	Yes	Yes	Yes
Relay system															
Directional comparison	3	4	7					1			5		1	4	
Transferred trip					1				2		1				
Blocking	1											1			
2-wire a-c		3		4		1	1			1	2	9			2
3-wire a-c			4												
Phase faults only	2														
Ground faults only					1						2				
Phase and ground faults	2	7	11	4		1	1	1	2	1	6	10	1	4	2

(*a*). Symbols for protective features
AG—Air gaps
C—Carbon blocks
F—Fuses
H—Heat coils
PT—Protector tubes
T—Thyrite
VG—Vacuum gaps

(*b*). Symbols for potential gradient protection
C—Coupling transformers
D—Drainage equipment
I—Insulated relays
N—Neutralizing transformers

(*c*). Some planned interruptions.

(*d*). Twenty-one outages due to testing.

(*f*). Due to terminal protective equipment.

Table II. Circuit Details and Performance

	Company Designation											
	1	2	3	6	6	7	8	6	10	11	12	12
Number of pilot circuits	2	5	2	42	15	218	9	6	2	10	18	4
Average length (miles)	2.0	3.0	0.3	5.2	2.3	1.5	1.7	0.8	3.7	1.8	2.5	1.0
Maximum length (miles)	2.0	3.0	0.3	15.6	3.5	15.0	2.0	0.8	4.0	2.4	8.8	1.9
Pilot-Circuit Performance												
Mile-years of operation	0	208	1.0	529	147	6,000	57	21	7.4	292	178	31
Total number of outages		1	0	49	0	2,910	*NS*	4	0	0	18	6
Total hours interruption		5		1,418		11,640	*NS*	24			104	48
Cause												
Short circuits				30							1	4
Grounds		1					4					
Open circuits												
Circuit changes				19				4		*f*	16	2
Other						*d*					1	
Cable Details												
Conductor size (American wire gauge)	16	12	14	19	12	12	12	12	19	8	19	10
Insulation[a]	3/64 inch *R*	3/64 inch *R*	*R*	*P*	*R*	3/64 inch *R*	3/64 inch *R*	3/64 inch *R*	*P*	3/64 inch *R*	*P* and *R*	Open
Protective features at terminals[b]	*PT*	None	None	None	None	None	*PT*	*PT*	*C-H-F*[e]	None	4-*F* 2-*T*	None
Voltage level (60 cycles)							450 volts	450 volts	354 volts		*f*	
Potential gradient protection[c]	*C*	None	None	17-*C* 25-*I*	11-*C* 4-*I*	*I*	None	None	None	None	17-*C* 1-*I*	4-*C*
Insulation level (volts)		600		600	600	1,500						
Automatic supervision	No	No	No	14	1	No	Yes	Yes	Yes	1	6	No
Special circuit marking	No	*NS*	Yes	Yes	Yes	*NS*	*NS*	*NS*	Yes	*NS*	1	No
Relay system												
Directional comparison		3		19			15			1		
Transferred trip		5		10								
Blocking									2		1	
2-wire a-c	2			18		203					20	
3-wire a-c			2	10								
4-wire a-c						15				9	1	
Phase faults only				30						9		
Ground faults only		5				15						
Phase and ground faults	2		2	27		203	15		2	1	22	

(*a*). *R*—Rubber, *P*—Paper } thickness as specified

(*b*). Symbols for protective features
AG—Air gaps
C—Carbon blocks
F—Fuses
H—Heat coils
PT—Protector tubes
T—Thyrite
VG—Vacuum gaps

(*c*). Symbols for potential gradient protection
C—Coupling transformers
D—Drainage equipment
I—Insulated relays

(*d*). Over 95 per cent of outages are "planned interruptions."

(*e*). *C*, *H*, and *F* at intermediate stations also.

(*f*). Some planned outages for circuit changes.

system as the power cables. The rest are leased circuits or privately owned telephone cables which generally are located at a distance from the power circuits.

3. All but two of the circuits are metallic. In these two cases ground return circuits are employed. In six cases the circuits are used for other purposes besides relay protection.

4. Although automatic supervision is used only on 18 per cent of the total installations and by 50 per cent of the companies reporting, nearly every company reporting indicated that it would use automatic supervision on future installations.

5. The service continuity reported based on 5,570 circuit years indicates an average out-of-service time of less than 0.0335 per cent. It appears that the interruptions on the leased circuits are more frequent but of shorter duration than on the privately owned circuits. An analysis indicates 1.32 interruptions per circuit year, with an average of 3.54 hours outage per circuit year for the leased circuits, while the privately owned circuits have an outage once in 1.74 years, with an average outage time of 2.92 hour per circuit year. The out-of-service time includes "planned interruptions," or circuit rearrangements.

6. Carbon block and fuse or heat-coil protection is applied to most of the leased circuits, both at the terminals and en route, and special tagging is employed in the various exchanges. Only 20 per cent of the privately owned circuits are provided with terminal protection. Seventeen circuits are reported as being equipped with neutralizing transformers.

Circuit Details

Ownership. Fifteen companies reported 58 leased circuits, while 20 companies reported 378 privately owned circuits. Only three companies reported having circuits of both types.

Construction. As previously indicated a large majority (367) of the privately owned circuits and apparently all of the leased circuits are in cable. The size of the conductor in the privately owned cables ranges from number 6, where the cable is used exclusively for pilot-wire relaying, to number 22, where the cable carries telephone and supervisory circuits as well as pilot-wire circuits. The larger wire sizes are generally rubber covered. In most cases lead sheathing is employed.

Electrical Characteristics. Although the average length of the circuit reported is three miles, in several cases circuits of 15 to 26 miles are in operation. The resistance ranges from a few ohms to as high as 2,675 ohms.

In certain cases involving leased circuits, loading coils are employed. It appears that these loading coils are an inherent part of the circuit facilities leased and are not introduced specifically for the pilot-wire system. None of the

Company Designation												
14	15	17	18	19	20	25	27	27	28	28	31	30
2.0	2.0	13	3.0	3.0	1.0	3.0	1.0	2.0	6.0	3.0	1.0	5.0
1.0	1.0	2.5	4.3	1.5	3.2	4.3	0.5	2.5	4.3	10.3	17.9	5.6
1.0	1.5	3.5	5.0	2.2	3.2	6.0	0.5	3.5	9.0	16.0	17.0	12.5
2	20	0	16	8.0	0	16.5	1.0	13.5	150	151	6.0	180
0	0		0	1.0		0	0	0	[l]8	[m]12	0	102
				74.0					1,048	449		1,034
												20
												18
												51
				[j]1								13
19	6, 10	22	19	19	12	16	8	6	8, 16	Cable and open	Open wire	8
600 volts[h]	*P*	[i]*P*	*P* and *R*	[k]*P*	$^4/_{64}$ inch *R*			Open	600 volts 1,500 volts			Open wire
None	*T*	None	*F*	None		*VG*	*T*	*T*	3-*AG*	3-*AG* 1-*F*	*VG* *F*	4-*AG*, 4-*C* 5-*F*
									4,000 volts	4,000 volts		
C	1-*C*	*C*	None	*I*	*C*	*C*	None	1-*I*	None	*C*	*D*	4-*I* 1-*C*
		1,400				1,800			High	High		45
No	No	*NS*	Yes	No		Yes	No	Yes	No	No	No	No
NS	No	No	*NS*	Yes		Yes	No	*NS*	3	Yes	No	4
	2		3				1		6		1	
							1					
												4
2		13		2	1	3						1
				1			1		3			
	2			1			3					
2		13	3	2	1	3			9		1	5

(*g*). Four circuits with fuses at intermediate stations.

(*h*). Special tests showed maximum induced voltage to ground of less than 565 volts.

(*i*). One mile rubber insulated. Some cable 1,500 volts to ground, and some 3,500 volts to ground.

(*j*). One outage due to flood conditions.

(*k*). 4.13 miles number 19 double paper insulation. 0.35 mile number 4 rubber insulation.

(*l*). All cable failures. Present condition believed to be capable of better performance.

(*m*). Three outages due to some cable troubles as in *l*. Remainder mostly sleet on open wire sections

(*n*). Ground return circuit.

NS. Not specified in reply to questionnaire.

relay schemes were reported as actually requiring the use of loading coils for their proper operation.

Terminal and Intermediate Protection. It appears that, in most cases, standard telephone protective equipment such as carbon blocks, fuses, and heat coils is applied to leased circuits at the various telephone exchanges through which they pass and also at the line terminals in the power stations. Some of the privately owned circuits are treated similarly at the line terminals. Air gaps, vacuum gaps, and Thyrite are employed. The reported terminal and intermediate protection is shown in Table A.

At times of ground faults on power systems, a considerable difference in potential may exist between the power-station ground as represented by the primary relay equipment and true ground as represented by the pilot-wire circuits. Certain questions were asked with the intent of determining if this was a serious problem and how it was cared for. Unfortunately the intent of the question was in many cases misunderstood, and the replies received lead to the belief that in many cases this problem has not been given due consideration. In certain cases, however, neutralizing transformers have been installed and in other cases highly insulated relay assemblies or insulating transformers have been indicated. Although insulating transformers constitute a component part of many of the pilot-wire protective systems employed, nevertheless, in some cases the insulation value of this equipment has been nullified with respect to the supervisory equipment installed on the line side and connected to the power-station ground.

The reported potential gradient protection and grounding practice is shown in Table B.

Circuit Performance

Service Continuity. Table C indicates the record of pilot-wire outages of 26 companies.

Causes of Pilot-Wire Outages. The cause of pilot-wire outages reported by nine companies covering 409 circuit-years is shown in Table D.

Trouble Due to Induction. Trouble due to induction or surges was indicated in 20 per cent of the circuits reported. Apparently the majority of these troubles have been corrected.

Protective Relay Systems

As a matter of incidental interest, information was obtained in regard to the relay systems utilized.

Types. The following general types of pilot-wire protective systems were

Table B

Neutralizing transformers	17 circuits of 5 companies
Line-coupling transformers	102 circuits of 19 companies
Insulated relay equipment	278 circuits of 14 companies
No protection	50 circuits of 9 companies
Ungrounded	298 circuits of 9 companies 14 circuits of 2 companies with neutralizing transformers 21 circuits of 5 companies with coupling transformers 7 circuits of 2 companies with both neutralizing and coupling transformers 256 circuits of 6 companies with neither neutralizing nor coupling transformers
Grounded to pilot cable sheath	3 circuits of 2 companies
Grounded through station battery equipment	4 circuits of 1 company
Grounded to station ground	65 circuits of 13 companies 30 circuits of 6 companies with coupling transformers 11 circuits of 2 companies grounded at one end only
Grounded to a remote ground	17 circuits of 5 companies 6 circuits of 3 companies with coupling transformers 6 circuits of 1 company grounded at one end only

Table C

Ownership	Company	Circuit Years	No. of Outages	Total Hours	Outage Per Circuit Per Year	Average Time Per Outage (Hours)	Outage in Percentage of Service
Private	2	68	1	5	0.0147	5.00	0.00084
	3	8	0	0	0.0		0.0
	5	4	0	0	0.0		0.0
	6	166	49	1,418	0.295	28.9	0.0975
	7	4,701	2,910	11,640	0.619	4.0	0.0282
	8	56	0	0	0.0		0.0
	10	2	0	0	0.0		0.0
	11	162	0	0	0.0		0.0
	12	119	24	152	0.202	6.35	0.015
	14	2	0	0	0.0		0.0
	15	13	0	0	0.0		0.0
	18	4	0	0	0.0		0.0
	19	5	1	74	0.2	74.0	0.169
	25	5	0	0	0.0		0.0
	27	6	0	0	0.0		0.0
	28	68	20	1,497	0.294	74.9	0.251
	30	28	102	1,034	3.64	10.12	0.421
Total	17	5,417	3,107	15,820	0.57* average	5.08 average	0.0333 average
Leased	4	12	15	15	1.25	1.00	0.014
	9	50	81	130	1.62	1.605	0.0297
	13	4	23	142	5.75	6.18	0.405
	16	1	1	4	1.00	4.00	0.046
	22	22	12	55	0.545	4.59	0.0285
	24	52	59	167	1.135	2.83	0.037
	26	7	9	28	1.285	3.11	0.046
	29	4	2	2	0.50	1.00	0.006
	32	1	0	0	0.0		0.0
Total	9	153	202	543	1.32 average	2.68 average	0.0355 average
Grand total	26	5,570	3,309	16,363	0.594 average	4.94 average	0.0335 average

* This item was reported as over 95 per cent "planned interruptions." Companies with new installations with less than one year of operating experience are not included in this table.

reported covering 436 circuits of 32 companies.

Directional comparison	76 circuits of 16 companies
Transferred trip	20 circuits of 6 companies
Blocking	9 circuits of 5 companies
Two-wire—a-c	288 circuits of 18 companies
Three-wire—a-c	21 circuits of 6 companies
Four-wire—a-c	25 circuits of 3 companies

These circuits furnish protection as follows:

Phase faults only	48 circuits of 7 companies
Ground faults only	22 circuits of 3 companies
Phase and ground faults	366 circuits of 31 companies

Backup protection is employed on the majority of circuits.

Performance. The performance record based on 406 circuits of 25 companies having 5,500 circuit years experience indicates a total number of 381 faults on protected lines with 368 (96.5 per cent) correct relay operations. In addition, a total number of 89 incorrect trippings was reported. Information available, however, indicates that a considerable number of these occurred during the first few months of service.

Table D

Circuit rearrangement	118
Short circuits	87 (6 of which were grounds)
Defective terminal equipment	46
Open circuits	37

Testing Practice. A total of 30 per cent of the circuits are tested yearly, 66 per cent semiannually, 2 per cent every three months and 2 per cent every month. It appears that the presence or absence of automatic supervising equipment has no influence on the frequency of test. In addition certain circuits not equipped with automatic supervision are checked for continuity, either daily or on every shift.

Conclusions

In conclusion it may be stated that an analysis of the replies received indicates that pilot-wire circuits have performed in a satisfactory manner and that the use of this form of protection is increasing. Certain difficulties experienced initially have been overcome successfully in practically all cases. Only one company reported the discontinuance of this form of protection because of unsatisfactory experience.

The replies to the questionnaire as received from the various operating companies have been tabulated and are given in Tables I and II.

Cooling Power Transformers by Forced Circulation of Cooling Medium

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Synopsis: The paper discusses the various methods that have been used in cooling power transformers by forced circulation of the cooling medium, and points out the benefits

1. To the war effort in the saving of critical materials.
2. To the user in first cost, shipment, installation, station layout, and mobility.

The selection of proper control equipment is analyzed from the standpoint of protection and reliability of forced cooling.

The relation between hot-spot and average winding temperatures and the future for forced cooling after the war is discussed.

General

A FEW years ago a study made of the service experience with transformers indicated that a satisfactory level of reliability had been reached. It was decided then to direct the major efforts of developmental work towards greater economy of transformer application by making savings in material, space, and weight and at the same time maintaining the level of reliability demanded by industry. To accomplish this aim many novel ideas were incorporated into a new transformer design. A developmental single-phase transformer of 14,500 kva, 135,000 to 11,000 volts was built. Figure 1 shows the core and coils with cover and bushings attached. In this novel unit radical savings in weight and dimensions were made as compared with a conventional self-cooled transformer of the same rating having the same electrical characteristics. Data obtained on this transformer demonstrated that such savings can be realized.

Further saving would have resulted from the use of the high-permeability cold-rolled silicon steel used in wound-core transformers.

This transformer was metal-sealed and designed to be shipped completely assembled, filled with insulating liquid, and with bushings in place.

The windings were made concentric cylindrical type which are ideally adapted to forced cooling. A cross section of these windings is shown in Figure 2. Forced cooling was one of the most important features of this new design.

I. Benefits to the User of Forced-Cooled Transformers

1. Significant reduction in the first cost of the transformer due to great reduction in total material.

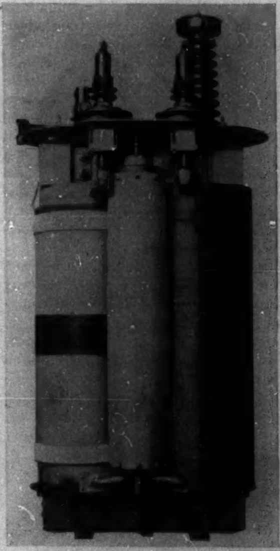

Figure 1. Internal view of a single-phase 14,500-kva 135-kv grounded-wye power transformer cooled by forced-air and forced-oil flow

2. The greatly reduced weight results in:

(*a*). Saving in transportation cost.

(*b*). Saving in installation expense, both in the foundation and handling.

(*c*). Almost doubling the maximum kilovolt-amperes that can be shipped completely assembled, where weight is the limiting feature.

3. A reduction of about 50 per cent in over-all volume of transformer means:

(*a*). A saving in floor space and head room of installation.

(*b*). Easier handling operations.

(*c*). Raising to about three times the maximum kilovolt-amperes of a transformer that can be shipped assembled, where size is the limiting feature.

4. Shipping the transformer completely assembled with all cooling equipment attached means:

(*a*). Economy of time and money at installation which may amount to several thousand dollars per transformer, for a very large unit.

(*b*). Greater security of operation because of elimination of field work.

5. Shipping transformers completely assembled permits metal sealing *of practically all oil joints*, such as bushings, cover, and so forth, and means:

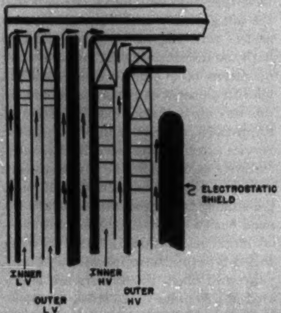

Figure 2. Cross section of top end of windings of transformer in Figure 1 showing directed oil flow

(*a*). Elimination of practically all gaskets.

(*b*). Permanence of tightness of joints, with greater strength.

(*c*). A reduction in the chances for oil contamination by moisture and oxygen from the air.

6. In transformers where the watts per square inch dissipated are low, or where the turn and coil insulation is relatively thin, there is a reduction in the difference between the average temperature of the winding and the hottest spot. This permits overloading of the windings without exceeding 65 degrees centigrade hot-spot rise, thereby gaining in economy of transformer design without a reduction in reliability.

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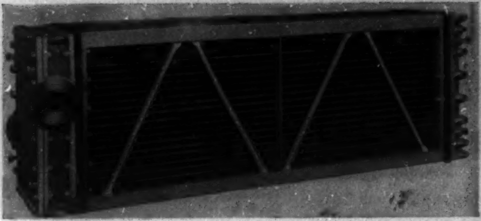

Figure 3 (above, and right). One of the first applications of oil air coolers with finned tubes used to cool transformers by forced circulation of oil and air

7. Remote installation of cooling equipment may be applied to either new or old transformers; this introduces new possibilities in station layout with the opportunity of utilizing the heat dissipated by the cooler.

II. Reliability

For some decades large transformers have been built with forced circulation of the oil, generally cooled by an oil-water cooler, with the oil circulated through both the transformer tank and the cooler by means of a pump. Several million kilovolt-amperes of transformers built in this way are in service today. Each unit is provided with its pump and cooler. Service experience has been entirely satisfactory, except for the inconvenience occasioned by the leakage of oil through the stuffing box of the pump and also the maintenance of the stuffing box. Instances of large installations of the oil-water cooled-type transformers built by the company with which the authors are associated are 28 units of 26,667-kva 161-kv single-phase transformers and 10 units of 27,500-kva 230-kv single-phase transformers.

An application of the oil-air cooler type made about a decade ago includes several 25,000-kva transformers which were cooled by forced circulation of oil through the transformers and through the fin-type coolers with a blast of air forced rapidly across the external surface of the fin tubes (Figure 3).

During the past several years, a large number of forced-cooled transformers have been built for use on electric locomotives as shown in Figure 4. This transformer design was the result of a joint undertaking by the Westinghouse Electric and Manufacturing Company and the General Electric Company.[4,5] In this transformer, the Askarel,* cooled by a rapid blast of air, was not only forced through the cooler, but also directed and forced through the coil ducts in the windings at a velocity much higher than normally created by the thermal head produced by the losses in a transformer winding. Also, it is many times the velocity used in the transformers previously mentioned, where the oil-water and the oil-air coolers were used. An excellent service record has been maintained in this locomotive application.

Service experience with small cooling fans for over a decade has been unusually successful, since over 15,000 of these fans have been installed on air-pressure cooled transformers, *Type OAP, totaling millions of kilovolt-amperes and to the best of our knowledge without a failure of one fan or its motor.*[6,7,8] Furthermore, much experience with fans and fan motors was secured with air-conditioning devices used for industrial purposes.

Based upon this extended field experience in the application of oil pumps, motors, and fans, we felt justified in presenting this type of station and portable transformer, which utilizes all of these accessories (Figures 5, 6, 7). The interest shown in this new development during the last few years confirms this viewpoint.

While in the present emergency the scarcity of materials gave strong impetus to the use of forced-cooled transformers,

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* Noninflammable synthetic insulating and cooling medium.

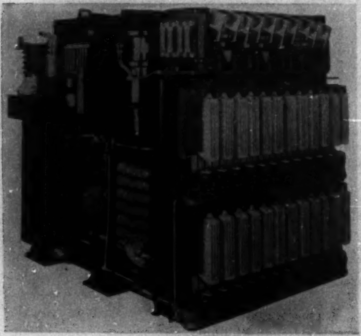


Figure 4. A 4,850-kva 12-kv electric-locomotive transformer cooled by forced-air and directed forced-oil flow

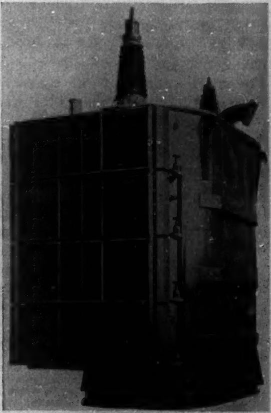


Figure 5. A 40,000-kva 135-kv power transformer cooled by forced-air and forced-oil flow

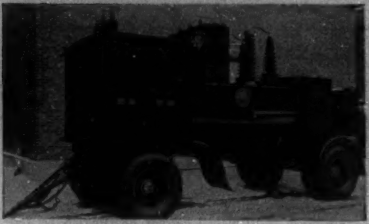


Figure 6. A 2,500-kva 33-kv mobile substation with transformer cooled by forced-air and directed forced-oil flow

the development and application of this type of transformer was undertaken before the emergency and was justified by normal conditions.

III. Nondirected- and Directed-Flow Forced-Cooled Transformer

There are two types of forced-cooled transformers: those with *nondirected* and with *directed* flow of oil through the windings.

Whenever the oil from the pump flows freely inside the tank, with the flow of oil through the windings produced by the thermal head or natural convection, the transformer is a *nondirected*-flow type (Figure 3).

On the other hand, if by the use of oil stops or baffles, the principal part of the oil from the pump is forced to flow through the windings of the transformer, it is a directed-flow type (Figures 1, 2, 4).

With nondirected flow, the advantage gained by forced cooling is that the oil is cooled to a predetermined value, which permits the use of a shorter tank and a reduction in size, weight, and cost of the cooling means (see item 4 of Table I). Some reduction in the amount of material used in the core and windings is possible.

With directed flow, additional savings are possible in the material used in the core and winding, resulting in a further reduction in the total weight of the transformer, as shown by items 5 and 6 of Table I.

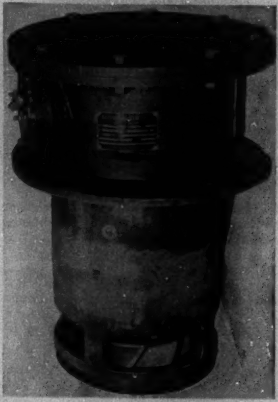
In both of these cases, the cooling equipment alone occupies about ten per cent of the volume and 25 per cent of the weight of radiators required by a self cooled transformer to dissipate the same loss with the same oil rise.

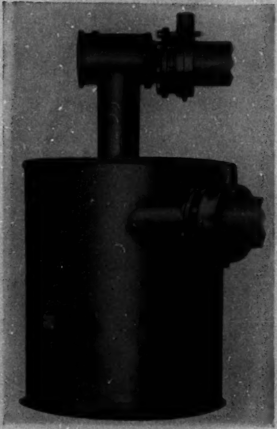
Nondirected flow may be applied to transformers already installed, either to increase the output, or to eliminate the use of water in water-cooled transformers.

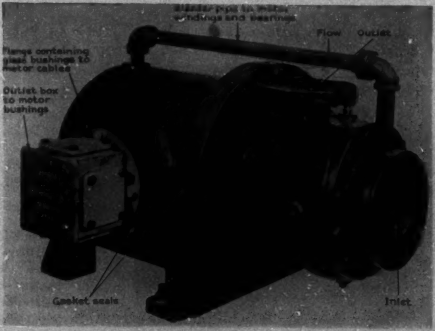
For application of directed flow, a standard line of very economical cooling equipment has been developed and covers the entire range of power transformers. To obtain minimum cost, advantage was taken of mass production of these forced-oil forced-air coolers.

The fan motors are protected by overload relays built into the motor to prevent damage, in case of accidental stopping or blocking of the fan (Figure 8).

To eliminate the troubles with pump stuffing boxes, and to further increase reliability of the pump, a variety of oil-immersed motor pumps has been developed to serve transformers for the various applications (Figure 7). The motor is immersed and rotates in the transformer oil, which circulates continuously through the motor jacket, to cool the motor windings and to lubricate the bearings.







Bleeder pipe to motor windings and bearings
Flow
Outlet
Flange containing glass bushings to motor cables
Outlet box to motor bushings
Gasket seals
Inlet

Figure 7. External views of oil-immersed motor pumping units

IV. Cost of Forced Cooling

The cost to the user of coolers with forced flow is not limited to the cost of the cooling equipment alone but must also include the cost of the capitalized power input to operate the fans and pump motors. In addition, the cost of the cooling water must be considered whenever forced-oil forced-water coolers are used (Figure 9). Figure 10 shows the relation between the relative cost of the cooler and the power input to the cooling mediums—also the combined cost of the power and the cooler. Various pronounced economical balances exist between the cost of the cooling equipment and the cost of power input.

Wherever capitalization of power is high, further economy in operation may be obtained by sectionalizing the cooling equipment, controlling it by the temperature of the windings or the oil, and using only that portion of the cooling equipment necessary to maintain the desired temperature. It should be noted, however, that this practice causes a reduction in insulation life.

V. Hot-Spot Temperature of the Winding

The life of a transformer winding depends upon the temperature of its hottest spot and not on its average temperature. A margin of ten degrees centigrade between the average and the hot-spot temperature is specified by AIEE rules, because the measurement of the temperature of hottest spot during the commercial acceptance test is impractical.

The heat generated in a winding conductor, depending on the position of the conductor in the winding, passes consecutively through from 9 to 12 thermal regions before it reaches the ambient cooling medium, whether it be water or air. The temperature drop through each one of these regions is governed by a different law, but in spite of this our developmental and commercial tests indicate that the average temperature of a transformer winding cooled by thermal oil circulation can be computed within one to three degrees centigrade.

Table I

	Oil in			Air Outside Radiator	Water Outside Radiator	Total Weight
	Winding	Tank	Radiator			
1	Thermal flow	Thermal flow	Thermal flow	Thermal flow		100
2	Thermal flow	Thermal flow	Thermal flow	Forced		82
3	Thermal flow	Thermal flow	Thermal flow	Thermal flow	Spray	79
4	Thermal flow	Forced	Forced	Forced		73
5	Forced	Forced	Forced	Forced		55
6	Forced	Forced	Forced	Forced	Spray	50

Water-cooled and forced-oil forced-water transformers are not included.

The causes for differences in temperatures in various parts of the windings may be grouped in four classes; solid insulation, distribution of losses, velocity of oil flow, and oil temperature gradient.

Solid Insulation. From the standpoint of temperature, solid insulations between copper and adjacent oil can be subdivided as follows:

1. Insulating tape on coils.
2. Turn insulation.
3. Spacing strips and channels.
4. Insulating collars.
5. Layer insulation.

Distribution of Losses. Heat generated in the copper of the various parts of the winding may vary because of:

1. The variation in eddy loss caused by the difference in location of the coil or turn in the leakage flux field.
2. Differences in eddy loss due to variations in the size of conductor and its width and thickness.
3. Unequal current distribution between parallel conductors in the case of imperfect transpositions of these conductors.

Velocity of Oil Flow. Variations in velocity of oil flow may be caused by:

1. A change in cross section of an oil duct.
2. The distribution of oil between parallel oil ducts of unequal hydraulic resistance, caused by difference in length or cross section of the duct, and disposition of the spacers and insulating collars.
3. The interference of the core leg in interleaved winding construction.

Oil Temperature Gradient. The temperature of the oil in its course through the winding increases continuously as the oil absorbs heat from the coils.

VI. Comparison of Temperature Distribution in Self-Cooled and Directed Forced-Cooled Windings

Since one of the main advantages of directed forced cooling of transformers is the radical reduction in weight of the

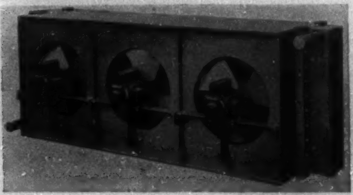


Figure 8 (left). Three-fan section of oil air cooler

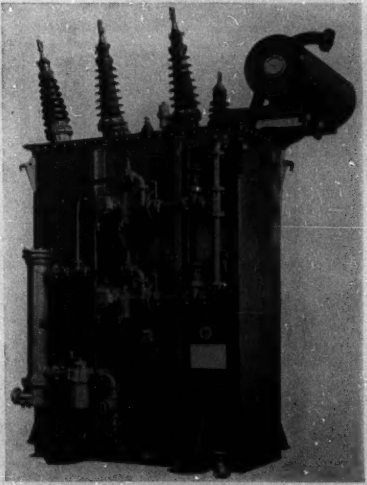


Figure 9 (right). A 7,250-kva 69-kv power transformer cooled by forced water with forced-oil flow windings, the copper loss per pound of winding is higher in forced-oil cooled than in self-cooled transformers. On the other hand, in forced cooling, where the heat transfer from the winding to the oil is more effective for a given temperature rise, relatively smaller winding surface needs to be exposed to the oil. For these two reasons, the watts per square inch of winding surface exposed to oil in a directed forced-oil cooled transformer is usually considerably greater than that used in a self-cooled transformer.

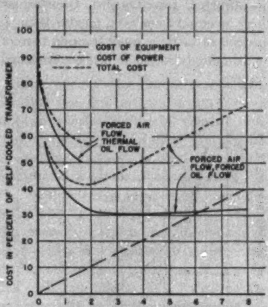


Figure 10. Relative cost of cooling transformer oil as a function of the power input to the cooling equipment

Furthermore, because of improvement in the space factor of the windings and the use of a smaller core section, the leakage flux density may be substantially higher than in self-cooled transformers.

Therefore, the temperature variations produced by nonuniform distribution of solid insulation and of losses are greater in directed-flow forced-cooled transformers than in self-cooled transformers.

In self-cooled transformers the motive-force causing heat-bearing oil to move away from hot surfaces increases with the amount of heat dissipated. Therefore, every part of the winding exposed to the oil and dissipating heat provides its own local motive force to carry away the heat. This tends to reduce the difference in temperatures produced either by restrictions to oil flow or by variation in heat dissipated per square inch of winding area.

On the other hand, in directed forced-cooled transformers, the oil circulation is produced by the oil pump, and at times its direction may be even contrary to the thermal flow of the oil. Therefore, the self-equalizing effect that plays such an important part in minimizing temperature differences in self-cooled transformers is essentially absent in forced-cooled transformers. For this reason it is very important that the complete structure be designed so that the forced oil flow distributes in a manner that produces predetermined temperature distribution.

The oil-temperature gradient, that is, the difference in temperature of oil in contact with the winding at the entrance and exit of the oil path, is proportional to the heat absorbed by the oil and inversely proportional to the velocity of oil flow.

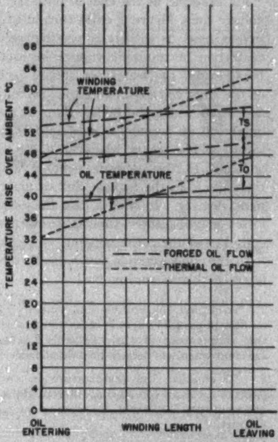


Figure 11. Comparison of oil temperature gradient and the resultant winding gradient in self-cooled and directed forced-cooled transformer

In both self-cooled and forced-air-cooled transformers, this oil gradient is of the order of 10 to 15 degrees centigrade. In directed forced-cooled transformers, the amount of oil forced through the winding is many times greater than would flow naturally because of winding heat, and therefore the gradient is proportionately smaller, being only a few degrees. This is one of the very important thermal characteristics of directed flow, forced-cooled transformers, that is responsible for the reduction in difference between the hot-spot and average winding temperature (Figure 11).

For example, with a gradient of 15 degrees centigrade for thermal oil flow, and three degrees centigrade for directed forced-oil flow (meaning that five times as much oil is forced as would flow naturally), and assuming the average oil temperature and all other factors influencing the temperature to be the same, part of the winding at the exit end of the oil path in the first case will be approximately six degrees centigrade higher than in the second case. It also follows that the difference between average and hot-spot winding temperatures, contributed by oil-temperature gradient alone, in the forced-flow case is only $1^1/_2$ degrees centigrade. However, it would be a fallacy to assume that this represents the entire difference between the hot-spot and the average temperature, since it would require neglecting all other contributing factors.

VII. Types of Oil Flow in Windings

Commonly, two types of coils are used in power transformer windings; pancake or disk coils, and cylindrical or barrel coils. Through these two types of windings, three directions of oil flow are possible: axial, radial, and transverse, as shown by Figure 12. In the axial and radial flows, the oil stream is parallel to the spacers. In the transverse flow, most of the spacers cross the oil stream and cause it to follow a complex path.

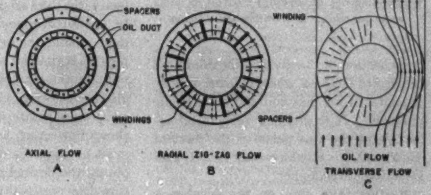


Figure 12. Three types of directed forced-oil flow through windings

In forced-cooled transformers, the temperature drop through the oil film at the coil surface depends upon the oil velocity in the duct, as shown by Figure 13. Therefore, when it is desired to maintain uniform temperature rise of copper over adjacent oil with thickness of solid insulation and uniform watts per square inch on the coil surface, it is necessary that the oil velocity be constant along the winding surface.

In concentric cylindrical coils the spacers are axial and the ducts are uniform in cross section; therefore, the oil flow is parallel to the spacers, and its velocity is constant (Figure 12A).

With radial flow, the oil velocity at the inner edge of the disk coil is higher than at the outer edge, in the ratio of the perimeters (Figure 12B). In power transformers, with the high- and low-voltage windings located concentrically, the radial width of the disk coil is a small fraction of its mean radius; therefore, the difference in the oil velocities is very small.

With transverse flow, the core leg or the insulating barriers inside the coil causes considerable variation in cross section of the oil duct. The ratio of the maximum cross section of the duct to the minimum is equal to $D/D-d$, where D is the outside diameter of the disk coil and d the inside diameter. It results in a ratio of oil velocities in the two parts of the duct of $V_2/V_1=D/D-d$, which may be 2:1, and sometimes even higher.

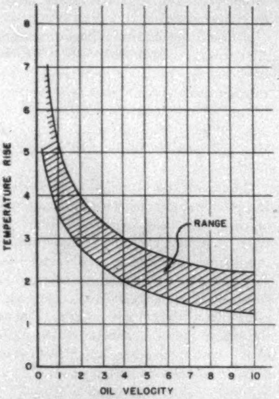


Figure 13. Relation between velocity and temperature rise of windings over oil for same watts per square inch on the coil surface

Further, the core or the insulating cylinder inside the coil "casts a shadow" on the downstream side, so that part of the coil surface may have an oil velocity, V_3, only one half of the entrance velocity, V_1. On account of this unequal distribution, the ratio between the maximum and the minimum velocity V_2/V_3 may be as high as 4:1. This unequal distribution of oil flow may cause a temperature drop across the oil film in the "shadow" to be a fraction of a degree to several degrees higher than in that part of the winding where the velocity of the oil flow is the maximum.

Uniform oil velocity is desirable where losses per square inch of winding surface and the thickness of the solid insulation between the copper and the oil are uniform. However, wherever these two factors vary, it may be desirable to modify the oil velocity to counterbalance the increase in temperature caused by either greater thickness of insulation or higher watts per square inch dissipated by the oil surface.

For example, at the ends of both high-voltage coils, Figure 2, extra turn insulation is used to withstand possible concentration of transient voltage stresses. In addition, the watts per square inch on the inner coil adjacent to the low-voltage winding may be higher than in the outer coil on account of increased eddy losses. This occurs wherever a coil is adjacent to the opposite winding and therefore is in the leakage flux of maximum density. In such a case the eddy losses may be three times the average eddy loss of the entire winding.

In the given example two radically different problems are involved:

1. To adjust the volume and velocity of oil flow in parallel ducts in order to equalize the temperature of the two coils having different watts per square inch of surface.

2. To equalize the temperature in two parts of the same coil having different thicknesses of turn insulation, by varying the oil velocity.

PARALLEL DUCTS

To compensate for the increase in watts per square inch due to the greater eddy loss, by means of an increase in oil velocity, the duct around the inner coil must be made correspondingly larger than the duct around the outer coil. The reason for this is that for any given oil pressure, not only the total oil flow in the duct, but also the velocity of oil flow increases with an increase in size of the duct. Figure 14 shows how much larger this inner duct must be than the outer duct in order to obtain the desired reduction in temperature drop across the surface film.

However, this method of balancing temperature by oil flow in parallel ducts is not always practicable because of the following considerations.

At the present time, to conserve copper in the directed forced-flow transformers, the watts per cubic inch of copper and watts per square inch of the winding surface exposed to oil are much greater than are used in self-cooled transformers. On the other hand, the thickness of solid insulation, such as turn insulation, is naturally the same in both self-cooled and forced-flow transformers. Therefore, the temperature drop, T_s (Figure 11) across the turn insulation in forced-cooled transformers is greater than in self-cooled in the ratio of watts per square inch used.

In forced-cooled transformers the temperature drop T_o across the oil film is reduced radically by the high velocity of the oil flow, and therefore, in spite of the high watts per square inch, this temperature drop may be even smaller than in the self-cooled transformer. On this account, T_s in directed forced-cooled transformers may be equal to or even greater than T_o instead of being a small fraction thereof, as commonly found in self-cooled transformers.

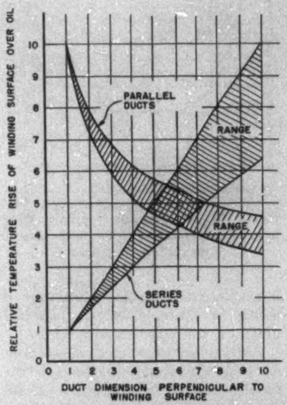


Figure 14. Relation between the size of oil ducts and temperature of the winding surface for a given watts per square inch

For example, let $T_s=T_o=7.5$ degrees centigrade. The winding rise over adjacent oil will be $T_w=T_s+T_o=15$ degrees centigrade. Assume that the inner coil (Figure 2) on account of greater eddy loss, has 20 per cent more watts per square inch than the outer coil. Its temperature rise over oil will be 15 degrees centigrade × 1.2 = 18 degrees centigrade. To reduce the rise to 15 degrees centigrade, by increasing the oil flow in the duct, means that the temperature drop across the oil film must be reduced from 7.5 to 4.5 degrees centigrade because obviously the temperature drop through the insulation cannot be decreased. To accomplish this reduction, the duct around the inner coil must be made at least four times greater than the duct around the outer coil (Figure 14).

Such an increase in oil duct is entirely impractical, not only because additional copper and iron would be necessary to avoid increasing the reactance, but also because it would result in an inefficient use of oil flow.

Therefore, it is not practicable to equalize the temperature in two coils having greatly different watts per square inch, merely by the adjustment of the relative size of the parallel oil ducts.

Fortunately, in modern concentric-winding transformers the eddy losses generally are so small a percentage of the I^2R loss that no difficulty is encountered from this cause.

Series Ducts

To compensate for the increase in temperature rise, due to the addition of extra end-turn insulation, the oil velocity over that section of the winding may be increased by *reducing* the size of the duct in accordance with curves given on Figure 14. This can be accomplished in several ways without affecting adversely either the amount of active materials or any of the transformer characteristics. Since the length of these smaller ducts will be a few per cent only of the total length of the coil, the increase in the resistance to the oil flow is insignificant.

In directed zigzag flow in concentric windings, as shown in Figure 15, the utmost flexibility exists in the control of the oil flow, which is both vertical and radial and is obtained by alternating fillers of insulation placed on the inside and outside of the disk coils. In this manner, the velocity and amount of oil and the pressure required can be varied widely without changing the winding. Whenever extra insulation is put between the copper and the oil stream, the ducts can be reduced correspondingly to increase the velocity and to compensate to some degree for the increased temperature drop across the solid insulation.

Because of the above structural characteristics of concentric windings, the effective use of directed flow of the oil does not require a departure from structures that have been used for a great many years.

From the preceding analysis it follows that the structural features of concentric-cylindrical as well as concentric-disk-type transformers naturally harmonize with the requirements for the effective use of directed-flow forced-cooling:

(*a*). The eddy losses are so small that they do not require disproportionately great increases in oil ducts.

(*b*). Increase in temperature rise through extra end-turn insulation can be compensated for by increase in oil velocity without waste of space or material.

(*c*). The oil flow is always parallel to the spacers, and, since the ducts are naturally uniform in cross section, the velocity of the oil may be uniform when desired.

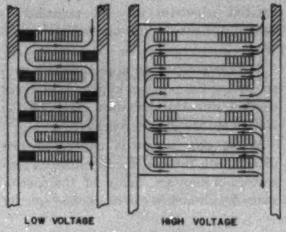


Figure 15. Directed zigzag oil flow through concentric windings

(*d*). The insulation structure is such that it does not interfere with the oil flow or with the division of oil between the windings.

Thus, it is interesting to observe once again the extraordinary structural harmony that exists in concentric-winding transformers. This harmony, as has been described to the Institute before, is the main cause behind the evolution of transformer structures from interleaved to concentric windings.[9]

VIII. Emergency Load

To prevent injury to directed-flow forced-cooled transformers, in case of an emergency created by an interruption of the power supply to the pump and fans, these directed-flow transformers may be designed to continue to carry 100 per cent load for one hour or more after the pumps and fans have stopped. The temperature rise of the winding hot spot at the end of this period will not exceed the limit recommended by the AIEE transformer subcommittee interim report, AIEE Transactions, 1942 September section.[10,11]

IX. Conclusion

It is obvious that, with so many methods of cooling now available to the designer, a great variety of combinations of cooling methods is possible. Each one of these combinations has its own advantages under certain conditions of operation, but the greatest economy in space and materials is attained by the use of the directed forced-oil method of cooling. The over-all gain in materials used in the core and coils can be illustrated by the following simple example: The core and coils of a typical self-cooled, high-voltage 10,000-kva transformer, with 55 degrees centigrade average winding rise and 65 degrees centigrade hot-spot rise, when arranged for directed, forced oil flow, will carry continuously 16,667 kva, and meet the same temperature limits.

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Shunt Capacitors

AIEE COMMITTEE ON POWER TRANSMISSION AND DISTRIBUTION
General Systems Subcommittee

THE Institute at present is fostering a program encouraging discussion of means of obtaining greater loading out of existing equipment. As part of this program the general systems subcommittee of the power transmission and distribution committee has undertaken the task of analyzing what can be done with generators of reactive power in reducing the kilovolt-ampere demand upon system elements, thereby releasing more of the system capacity for useful kilowatt loading.

A discussion of this subject is greatly simplified by using the expressions kilovars, to refer to the reactive power and kilovolt-amperes, to refer to the product of volts and amperes. It is recommended that greater use be made of this distinction which has been recognized by the American Standards Association.

The breakdown of kilovolt-amperes into its two components, kilowatts and kilovars, is receiving greater and greater acceptance in the industry. Each component is being considered as a separate and distinct entity, even to the extent of not using necessarily the same direction of flow in the system. Thus an overexcited generator is usually considered as feeding both kilowatts and kilovars into the system. An overexcited synchronous motor is usually considered as taking kilowatts from the system but feeding kilovars into the system. In the former case, the directions of flow are the same, whereas in the latter case they are different. A shunt capacitor is thought of as feeding kilovars into the system. Thus, without attributing a sign to kilovars, the operator dispatches this quantity and provides for reversal of the quantity by the device of referring to the flow in or out at that point of the system. For some purposes it is desirable that the positive sense of flow of kilovars be in a certain direction. This makes it necessary that a distinction be made between a positive and a negative kilovar, and therefore a sign must be added before the number representing the kilovar. In order to be consistent, the foregoing type of kilovar will be regarded as positive, and, when occasion arises to reverse the flow of kilovar without reversing the positive sense of flow, a negative sign should be used. Thus an overexcited generator may be regarded as drawing negative kilovars from the system.

It is interesting to note that this conception of the positive kilovar is in direct conflict with the definition given by the American Standards Association.

While this report is concerned primarily with the use of capacitors as generators of kilovars, many of the conclusions drawn herein can be applied equally to other generators of kilovars, such as overexcited synchronous machines.

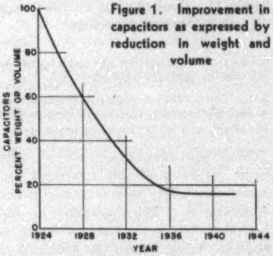

Figure 1. Improvement in capacitors as expressed by reduction in weight and volume

Development

Capacitors for power-factor correction first made their appearance as early as 1914. By 1928 the total kilovolt-amperes installed was approximately 260,000. At the end of 1938 the total had risen to 1,400,000 kva, and by the end of 1941 the estimated total installed capacity was 3,000,000 kva. In 1942 the total kilovolt-amperes of capacitors which will be produced has been estimated at 2,500,000, which would bring the total estimated installed capacity as of January 1, 1943, to about 5,500,000 kva.

Prior to 1932 all power capacitors were of the oil-type construction. The use of chlorinated aromatic hydrocarbon impregnating compounds and other advances in the art brought about sharp reductions in size and weight. Today, complete outdoor capacitor installations weigh five pounds or less per kilovolt-ampere, as contrasted with 15 or 20 pounds per kilovolt-ampere in 1925. Prior to 1937 practically all capacitors were installed in industrial plants. The development of outdoor-type units eliminating steel housings and other accessories resulted in a further increase in the use of capacitors.

The improvement in capacitor design is best reflected in the size and weight. Figure 1 illustrates the results of development in the period between 1924 and 1942. The general acceptance of capacitors has been due to the following:

1. Reduction in selling price.
2. Improvement in design and manufacturing methods resulting in decrease in size and weight.
3. Development of outdoor pole-type units and standardized mounting brackets.
4. Reduction in failures.
5. Better understanding of system benefits which accrue from their use, such as release of system kilowatt capacity and low loss as compared to other means of supplying kilovars.
6. The fact that they require the least critical material to supply kilovars.
7. The fact that, for the war emergency, manufacturing facilities are more available than for other means of supplying kilovars.

General Features

Capacitor banks are usually made up of standard 15 kva-units. A test voltage of somewhat over twice rated voltage is applied from line to line. The normal service voltage should not exceed 115 per cent of the rated voltage for standard voltage ratings of 575 volts and below and 110 per cent for standard voltage ratings above 575 volts. Capacitors should not be applied to circuit voltages in excess of five per cent of their rating. The line-to-ground test voltages vary with indoor or outdoor type and range from 43 times rated voltage for the 230-volt outdoor capacitor to twice for the 13,800-volt indoor capacitor.

PROTECTION

Capacitors connected to ground reduce the magnitude of surge voltages to ground that occur on systems and, with some risk, may not require arresters for their protection. Ungrounded capacitors should, however, be protected by arresters. Discharge devices are usually built into the capacitor, so that, when the capacitor is de-energized, any remaining charge is dissipated as a safety precaution.

Short-circuit protection is required primarily with capacitors for detecting and

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Personnel of general systems subcommittee: C. F. Wagner, chairman; J. W. Butler, S. B. Crary, J. E. Hobson, E. K. Huntington, A. A. Johnson, A. A. Kroneberg, M. C. Miller, H. K. Sels, H. M. Trueblood.

removing a faulty unit without causing other protective devices to operate, and for removing the unit before the internal arc generates sufficient pressure to burst the case which might cause additional damage. The requirements for wye-grounded and delta banks as contrasted with those of the wye-ungrounded banks, warrant a separate discussion for each.

For wye-grounded or delta banks a faulted capacitor unit requires that the fuse—individual or line—interrupts the full-system short-circuit current. Thus, on large systems, the fuse requirements can become very heavy, and, because of fuse limitations, these type banks have limitations.

For wye-ungrounded banks the current through a faulted unit is only three times the normal current of the capacitors connected to that particular phase and is neutral, since it is limited by the capacitors in the two sound phases. Thus, the current to be interrupted is sensibly independent of the system available short-circuit current, and the interrupting requirement of a fuse is under quite complete control since the bank is divided into sections. Thus, a system with large short-circuit requirements can have capacitors applied to it with low-capacity fuses of the individual or line type. Grouping several capacitors under a single line fuse offers many useful arrangements.

In these floating neutral equipments the short-circuit current generally can be reduced to such a value to allow circuit breakers controlled by relays to disconnect the bank before a faulty unit bursts. Three such schemes are:

1. A current transformer is placed in the neutral between the two groups to energize a current relay for initiating the tripping of the circuit breaker when objectionable unbalance occurs.

2. A voltage transformer is placed between the neutrals of the two groups to energize a voltage relay which initiates the tripping of the capacitor circuit breaker when objectionable unbalance occurs.

3. A voltage transformer is connected across each capacitor phase with the secondary windings in series with a voltage relay which initiates the tripping of the circuit breaker when objectionable unbalance occurs.

HARMONICS

The impedance of a capacitor varies inversely as the applied frequency. Therefore, relatively small harmonic distortion in the applied-voltage wave form may produce very large current-wave distortion. To allow some harmonic voltage, the capacitors being built today, according to National Electrical Manufacturers Association standards, permit combined 60-cycle and harmonic kilovolt-amperes to equal not more than 135 per cent of the capacitor rating. To obtain the kilovolt-ampere loading at which a capacitor is operating, expressed as a fraction of its rating, with harmonic voltages E_3, E_5, E_7, and so forth applied, it is only necessary to insert the voltage values in the following expression:

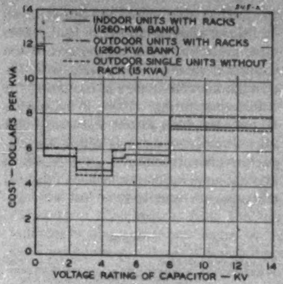


Figure 2. Cost of capacitors as a function of rated voltage

Use lowest cost figure indicated for all standard voltage classes

——— Indoor units with racks (1,260-kva bank)

— - — Outdoor units with racks (1,260-kva bank)

- - - - - Outdoor single units without rack (15 kva)

$$\text{kva} = E_1^2 + 3E_3^2 + 5E_5^2 + \ldots\ldots \qquad (1)$$

where all voltages are expressed as a fraction of the rated voltage. If only one harmonic is present in addition to the fundamental, the foregoing rule limits this harmonic to the following:

$$E_n = \sqrt{\frac{1.35 - E_1^2}{n}} \qquad (2)$$

where n is the order of the harmonic.

LOSSES AND AMBIENT TEMPERATURE

The losses of present-day capacitors in kilowatts are about one third of one per cent of their kilovolt-ampere loading as determined by equation 1. The operating ambient temperature should not exceed certain figures which vary with the type and the application. For outdoor open mounted units this value is 50 degrees centigrade and for housed units between 40 and 50 degrees centigrade, depending upon the type of rack.

COSTS

Costs of capacitors vary with the operating voltage, being higher at the low and high voltages and minimum at intermediate voltages. Figure 2 shows the variation of cost with voltage.

Application of Capacitors

The three principal benefits that accrue from the application of capacitors are:

1. Reduction in current for the same kilowatt load, or reduction in the required kilovolt-amperes of equipment for the same kilowatt load through the equipment.

2. Improvement in voltage level, and, if properly switched, improvement in regulation.

3. Reduction in system I^2r and I^2x losses

All of these benefits usually go hand in hand but have different limitations beyond which it is uneconomical to install additional capacitors. While certain benefits can be shown to result by reducing the current passing through isolated pieces of apparatus, the benefits are usually more far reaching. Thus, supplying the kilovars at or near the utilization point not only reduces the current in the distribution circuit feeding the load at that point, but the effects are reflected all the way back to the generator in that the current, the system voltage drop, the I^2r and the I^2x losses are less throughout the entire system. In the following discussion consideration will be given first to the effect of capacitors on isolated equipment, and this will be followed by consideration of how the entire system is affected. These discussions are very closely identified with system economics.

Current Limitation

When the load on a transformer exceeds its rating, two alternatives present themselves:

1. Additional transformer capacity can be added.

2. Capacitors can be connected in parallel with the load, until the current in the transformer or the vector sum of the kilovolt-amperes of the load and the capacitor falls within the rating of the transformer, provided, of course, the kilowatts of the load do not exceed the kilovolt-amperes of the transformer.

Naturally the proper choice depends upon transformer and capacitor cost and upon the amount of capacitors required.

Figure 3 illustrates how the application of capacitors increases the amount of real power that can be carried by a transformer. In Figure 3a is shown a transformer of unity kilovolt-amperes supplying a load of 80 per cent power factor. Assume first that the capacitor is not connected. From Figure 3b it can be seen that only 0.8-kw load can be supplied without exceeding the rating of the transformer. If the load is increased to 0.9 kw, and the same power factor is maintained, then the kilovolt-amperes of the load is 0.9/0.8 or 1.125, and the kilovar of the load is (0.9/0.8) 0.6 or 0.675. To pass a load of 0.9 kw through the transformer and still keep within its rating, the kilovar as shown by the circle, must be limited to 0.435. This can be accomplished by placing a capacitor across the load which will consume the difference between 0.675 kilovar and 0.435 kilovar or 0.24 kilovar. The useful load has thus been increased from 0.8 to 0.9 kw or 0.1 kw at the expense of the addition of a 0.24-kva capacitor.

The curves in Figure 4c express more generally the same results and show the total kilowatts that can be passed through the transformer as a function of the kilovolt-amperes of the capacitor connected across the load. The transformer is kept loaded at rated kilovolt-amperes at all times. The curve in Figure 4d gives the increase in kilowatt load as a function of the capacitor kilovolt-amperes and the curve of Figure 4e the increase in load kilovolt-amperes. The curve in Figure 4f, on the other hand, obtained by dividing the abscissa of curves of Figure 4e by the corresponding ordinate, gives the capacitor kilovolt-amperes per increase of load kilovolt-amperes again plotted against capacitor kilovolt-amperes required. This curve is of particular interest, since when one multiplies this quantity by the cost of the capacitor per kilovolt-ampere, the product gives the cost of supplying an additional kilovolt-ampere of load. This cost, neglecting other advantages of the capacitor, should be compared with the cost per kilovolt-ampere of increasing the transformer rating. Thus, if the load is 70 per cent power factor, and a capacitor kilovolt-ampere of 0.4 is added, the capacitor kilovolt-amperes per increase in kilovolt-amperes of the load is 1.65. If the capacitor cost is $7.00 per kilovolt-ampere, then the increase in ability to supply load is obtained at a cost of 1.65 times $7.00 or $11.55 per kilovolt-ampere. If the transformer cost per kilovolt-ampere is $10.00, then considering the other advantages of the capacitor, the latter may be more economical.

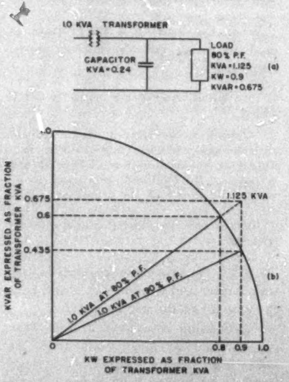


Figure 3. Illustrating the effect of adding capacitors upon the ability of a transformer to carry increased real power with load of constant power factor

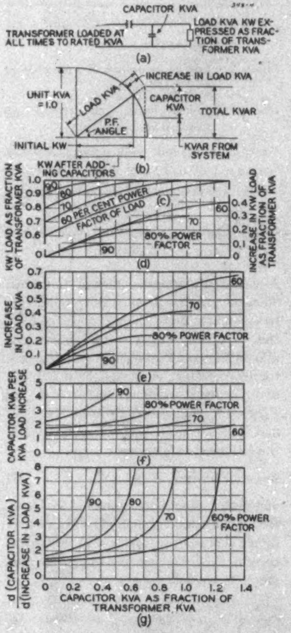


Figure 4. Effect of shunt capacitors upon loading of a transformer

From Figure 4b it is apparent that, as the load through the transformer approaches unity power factor, smaller and smaller incremental gains in load are obtained for incremental increases in capacitor kilovolt-amperes. Figure 4g gives the incremental capacitor kilovolt-amperes required for an increment in kilovolt-amperes of the load. Expressed mathematically, the ordinate in this curve is equal to

$$\frac{d\text{ (capacitor kva)}}{d\text{ (increase in load kva)}}$$

This curve shows that the final increment is attained at much greater expense than the initial increment.

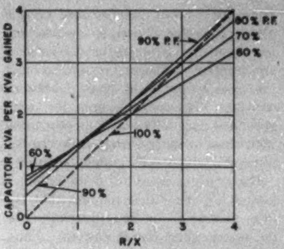


Figure 5. Gain in load kilovolt-amperes by adding capacitors, maintaining constant voltage drop

In the foregoing a transformer was used for illustrative purposes only. The same arguments apply equally well to any equipment in which current might constitute a limiting factor such as generators, cables, regulators, and occasionally overhead transmission and distribution lines. Consider for a moment a generator. Modern 0.8 and 0.9 power-factor generators can, in general, be operated at full kilovolt-amperes at unity power factor. In most cases the turbine is capable of delivering this increased power above its rating. However, there may be other elements in the electric and steam systems that would make it undesirable to operate under these conditions. The power generation aspects of the problem are being presented in a symposium on this subject by the power generation committee. The present shortage of critical materials for the construction of generators makes this question particularly important at this time. Since the prime function of generators is to deliver kilowatts, they may, where additional power is needed, be retained for this purpose and the required kilovars supplied by other means at or near the load. In applying the cost data obtained through the use of the curves of Figures 4f and 4g, it is necessary in this case to compare capacitor costs with generator costs. In almost all cases it will be found that capacitors, to bring the operating power factor of the generator to near unity, will be justified. Other considerations, such as stability of the generator at low excitation, might dictate the extent to which the power factor can be corrected.

As will be shown subsequently, additional advantages, such as reduction in losses and improvement in regulation, can be obtained by locating the capacitor close to the load. Since the system investment increases per kilovolt-ampere delivered as the load is approached, and since capacitors decrease the required system kilovolt-amperes per kilovolt-ampere delivered, it becomes easier to justify the application of capacitors at points closest to the load.

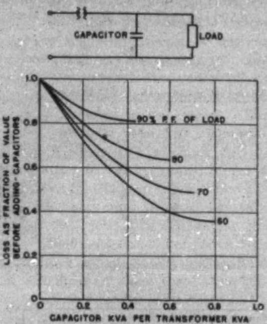


Figure 6. Effect of adding capacitors upon the loss in a feeder, transformer, or other equipment supplying a load

Voltage Drop

The voltage drop in feeders on short lines can be expressed approximately by the relation

$$\text{Voltage drop} = RI_r + XI_x \tag{3}$$

where R is the resistance, X the reactance, I_r the power component of the current, and I_x the reactive component. If a capacitor is placed in shunt across the end of the line, the drop becomes approximately

$$\text{Voltage drop} = RI_r + XI_x - XI_c \tag{4}$$

where I_c is the current drawn by the capacitor. Thus it is evident that if I_c be made sufficiently large, both the RI_r and the XI_x drops can be neutralized. This expression also shows that, if the voltage drop is compensated at full load, then at light loads, as I_r and I_x become smaller, the line is overcompensated, because I_c is dependent only upon the voltage and not upon the load. Thus it may be said that the regulation of the line is practically unchanged by the presence of the capacitor. The capacitor effects an increase in voltage both at light load and at full load. At light loads the voltage rise might well be so much in excess of the normal value as to represent an undesirable or even intolerable condition.

Because of its lack of voltage control, a permanently connected capacitor has limitations. However, with suitable control for switching capacitor banks or sections of banks in and out, the capacitor can be made to control the voltage. The control element can be operated either by a time switch, from the line voltage, by the load power factor or by the load kilovars. To date the voltage control has received greatest acceptance, because in general, it affects directly the quantity which it is desired to control, although in industrial applications power factor and load kilovars have been used quite frequently. Switching presents new problems unique in capacitor applications which, however, are susceptible to ready solution. These are:

1. When a capacitor bank is connected to a system, the system impedance is usually of sufficient magnitude to limit the initial inrush to a reasonable value. If, however, a section of a bank is switched in while another section of an adjacent bank is already connected to the system, the low impedance between the sections can result in large currents of high frequency. Experience has indicated that this need not be given consideration in the general case.

2. Opening of the capacitor charging current—under the proper conditions—can result sometimes in restriking of the arc within the breaker, with resultant high transient overvoltages. This is essentially a switching problem and can be handled adequately with properly applied available switching equipment.

The switches need not, however, have sufficient interrupting capacity to handle the system short-circuit current. Breakers are usually supplied for this purpose which can also be used as disconnecting devices, providing there is a backup breaker. For outdoor service suitable fuses are also used for this purpose. In industrial applications capacitors are sometimes connected directly across the terminals of a motor and switched on and off with it.

The number of steps to be used in capacitor banks depends entirely upon the system characteristics, the total drop which it is desired to correct, and the increment in voltage change which the system can absorb. In this respect the switched capacitor is in a position similar to the step voltage regulator as contrasted to the continuous variation of the induction-type regulator.

To apply capacitors most effectively it is desirable that wattmeter, kilovar meter and voltmeter records be taken at several points throughout the system. These will serve as a guide to determine the amount of capacitors that should be installed and also the necessity for and number of steps in switching equipment.

To compare the benefits of line-drop compensation by means of capacitors with other methods of accomplishing the same result, the curves shown in Figure 5 are provided. These curves show the capacitor kilovolt-amperes required per kilovolt-ampere increase in load that can be supplied without increasing the line drop. It will be observed that this quantity is a function only of the power factor of the load and ratio of resistance to reactance of the feeder, transformer, or other device. By multiplying the ordinate by the cost of a kilovolt-ampere of installed capacitor, the cost of supplying an additional kilovolt-ampere of load without increasing the drop is obtained. This figure should be compared with the cost to produce the same result by other means, such as additional cost per kilovolt-ampere of transformer or feeder.

Reduction in Losses

When current flows through a circuit consisting of resistance and reactance, there is in addition to the I^2r loss an I^2x loss. The generator must, therefore, in addition to supplying the load requirements, supply the I^2r loss in the form of kilowatts and the I^2x loss in the form of kilovars. If the current through the system is decreased, then the I^2r and I^2x losses which the generator must supply will decrease. In the case of reduced I^2r losses, the useful kilowatt loading of the generator can be increased by practically this amount without requiring more fuel. The smaller I^2x losses reduce the kilovolt-ampere loading on the generator and thereby release generator current-carrying capacity which may be used to supply kilowatts.

Under the present emergency, reduction in kilowatts (I^2r) and kilovars (I^2x) losses has taken on new importance and significance. So long as the system has adequate reserve, there is some justification for those who argue that the kilowatt losses should be evaluated in terms of the fuel required. However, as the reserves become depleted, and the system is operated closer and closer to its theoretical capacity, losses must be evaluated in terms of both the total equipment and the fuel required. Under present conditions when generating capacity is difficult if not impossible to obtain, reduction in losses becomes still more important.

Capacitors are effective in reducing

the current and, consequently, the I^2r and I^2x losses. Of course, in the case of a transformer or a feeder in which the current-carrying capacity is limited by thermal consideration, this factor is only another facet of the same problem. When capacitors are installed near the load, the reduction in current affects the loss throughout the entire system. Calculations of reduction in loss under peak load conditions have shown that capacitors of reasonable amounts, in addition to other benefits, might also reduce the system generator output by the extent of two to four per cent. Figure 6 shows the reduction both in I^2r and I^2x losses that result by improving the power factor of the total load on a transformer or feeder by the addition of capacitors, maintaining all the while the same load.

Over-All System Performance

While the foregoing discussion illustrates the advantages of the application of capacitors from the standpoint of isolated equipment, the full advantages of

Figure 7. System performance with application of shunt capacitors

Base load at each point *C* (load center of single-phase laterals)
At time of peak load—100 kw 75 kilovars 125 kva at 80.0 per cent power factor
At time of light load—20 kw 30 kilovars 36 kva at 55.5 per cent power factor

Total load at points C for 15 circuits
At time of peak load—13,500 kw 10,125 kilovars 16,875 kva at 80.0 per cent power factor
At time of light load—2,700 kw 4,050 kilovars 4,868 kva at 55.5 per cent power factor

Base load on each 240-volt secondary line
At time of peak load—7.83 kw 5.68 kilovars 9.67 kva at 80.9 per cent power factor
At time of light load—1.59 kw 2.29 kilovars 2.79 kva at 57.0 per cent power factor

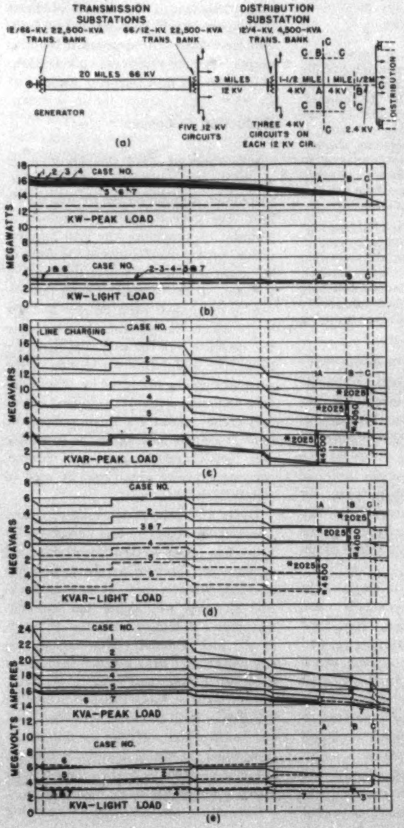
Total load on secondary lines
At time of peak load—12,683 kw 9,208 kilovars 15,670 kva at 80.9 per cent power factor
At time of light load—2,578 kw 3,707 kilovars 4,526 kva at 57.0 per cent power factor

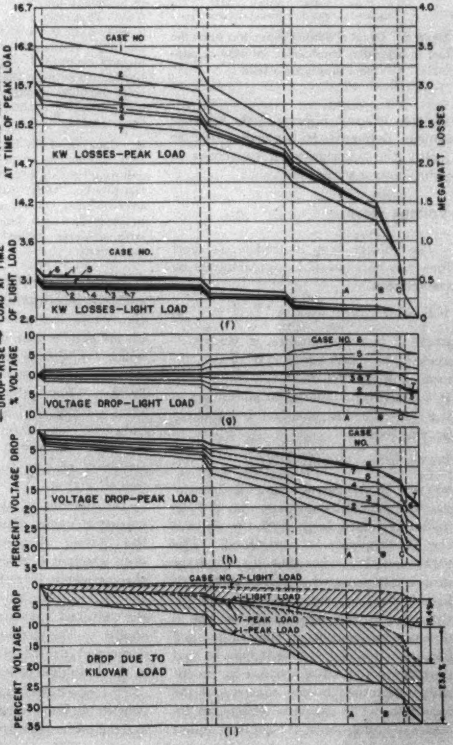
Cases Assumed for Calculations

Kilovars of Capacitors on Each of 15 Circuits

Case No.	At *C* (×9)	At *B* (×3)	At *A* (×1)	Total Per 4-Kv Circuit	Total on 15 Circuits
1	0	0	0	0	0
2	15	0	0	135	2,025
3	15	45	0	270	4,050
4	15	90	0	405	6,075
5	15	90	135	540	8,100
6	15	90	300	705	10,575
7	*	0	0	0	0

* Loads at points *C* unity power factor at peak and light loads.





the capacitor cannot be appreciated without analysis of the entire system. For any particular system this analysis can be made best on a network calculator or a network analyzer. In this case, the capacitors can be spotted throughout the system and the net result determined readily and quickly for both full-load and light-load conditions. To illustrate the nature of these advantages, M. C. Miller has chosen a hypothetical system which is sufficiently simple to analyze mathematically. The results of this analysis were presented before the Edison Electric Institute, and a portion of the paper is included in this report. The chosen system is shown in Figure 7a. Briefly, it comprises a generator, step-up transformer to 66-kv transmission, a step-down transformer to five 12-kv circuits, each serving a 12-kv to 4-kv substation with three 4-kv distribution circuits. This makes a total of fifteen 4-kv distribution circuits. Emanating from the terminals of these three-phase circuits indicated by the point *B* are 2.4-kv single-phase laterals of which there are nine for each 4-kv three-phase feeder. Power is stepped down on these lines to 120/240-volt utilization voltage. The loadings for peak and light loading conditions are also given in Figure 7a. Installations of shunt capacitors throughout the system were assumed, which are identified by the case numbers assigned to them in the table of Figure 7a. Cases 1 and 7 assume the same kilowatt loading with no capacitors installed; the loads at the points *C* for the former being at 80 per cent power factor and for the latter at unity power factor.

The curves in Figure 7 are self-explanatory, as they depict the kilowatt, kilovar, kilovolt-ampere, and voltage at different points throughout the system for both peak-load and light-load conditions. A number of details are worthy of special comment.

The kilowatt increases as the generator is approached, a fact shown in magnified form by the loss curves in Figure 7f. For a customer load of 12,700 kw with no capacitors, the generator output is 16,500 kw, representing a system loss of 3,800 kw or 30 per cent. Of course, the larger portion of this loss is unavoidable and represents a loss resulting from the transmission of real power only. Note that for case 7 for the peak load the kilovar is very small, and for practical purposes the losses for this case represent the ultimate to which they can be reduced; that is, they correspond to within one tenth of a per cent with the losses that would result if the kilovars were supplied at the points of origin by capacitors distributed along the lines.

However, the amount by which the losses are reduced is still quite substantial. Thus the first increment of capacitors (2,025 kilovars) reduces the losses by 360 kw. The effects upon the kilovolt-ampere output of the generator are very much greater. The first increment of capacitors (2,025 kilovars) at the load reduces the kilovolt-amperes (not kilovars) by 2,200 kva at the generator. Figure 8 shows the effect of additional increments of capacitor kilovars upon both the kilowatt losses and amount of generator kilovolt-amperes released. The points should not necessarily form continuous curves, because they represent increments of capacitors at different locations throughout the system. The difference in the losses for cases 6 and 7 represent the benefits, as can be seen from Figure 7f, of locating capacitors at *C* instead of *B*, thus decreasing the losses in the circuit between these two points. Even under normal national economic conditions it appears desirable to relieve the generator of the duty of supplying a portion of the kilovars and to transfer that function to the capacitor. Under present conditions, with the increasing difficulty of obtaining fuel, the reduction in losses becomes even more important. The restriction in the availability of generating equipment is fortunately relieved to a very considerable extent by apparatus which is relatively low in cost and has few restrictions as to availability. Considering the generating capacity of the entire country, which is something in excess of 25,000,000 kw, it may be seen that releasing even as small an amount as two per cent makes available 500,000 kw of generating capacity. Of course, the foregoing considerations are predicated upon the ability of the generator to supply full kilovolt-amperes at higher power factor than rated, which is not always the case. To a lesser extent, similar considerations permit the installation of smaller transformers if the power factor is corrected by capacitors.

Miller's curves illustrate also what he terms the "snowball" or cumulative effect upon the reduction in kilovars by locating the capacitors as close as possible to the load. To produce the same reduction in kilovars at the generator as obtained by 2,025 kilovars of capacitors at the load would require 3,000 kilovars of capacitors connected directly across the terminals of the generator. This constitutes a further argument for placing capacitors near the load.

In applying capacitors, consideration should be given not only to the peak-load condition but also to the light-load condition. If the capacitors are not switched off at light loads, the disappearance of the lagging kilovars produces an excess of loading kilovars. This produces two results:

1. The generator becomes underexcited and this may be dangerous if the system has a tendency toward instability.
2. The voltage rises near the load end of the system.

Provision, of course, can be made to switch capacitors off automatically at times of light load. Figures 7g and 7h illustrate the variation of voltage between peak load and light load for the different cases just discussed. A voltage drop in a system is not so serious if it is the same at peak and light loads, since the voltage level can be corrected by merely changing a transformer tap. In the particular system under consideration it will be observed that a voltage drop of 35 per cent occurs at the time of peak load and 11 per cent at the time of light load. Therefore, the voltage regulation itself is 24 per cent, a magnitude which would be intolerable and must be corrected either by switching of capacitors, tap changing under load, or induction regulators. If, however, at peak load and light load the kilovar component of the load is compensated by the installation of capacitors (case 7), the regulation is only 15 per cent. This of course, involves switching of the capacitors between full load and light load. Verification of these values can be obtained from Figures 7g, h, and i.

Idle generators are sometimes floated on the system to supply kilovars. This has the disadvantage that the kilovars are not usually supplied at the most advantageous location and is not available when needed most, that is, at peak loads when the generator is delivering real kilowatts.

Comparison Between Capacitors and Synchronous Condensers

In large units synchronous condensers constitute a competitor of the capacitor and may be more desirable when all factors are evaluated. The following points should be considered in comparing these two types of equipment:

1. A standard synchronous condenser is capable of supplying kilovars equal to its rating to the system as well as of absorbing them to an extent equal to 50 per cent of its rating. For those applications requiring these characteristics, the comparison should be on a basis of the synchronous condenser

against the capacitor at full kilovars plus a shunt reactor of 50 per cent kilovars.

2. The fineness of control of the synchronous condenser cannot be duplicated by the capacitor unless a large number of switching steps are used.

3. An instantaneous drop in terminal voltage, within practical limits, increases the kilovars supplied to the system in the case of a synchronous condenser, whereas a similar change in the case of capacitors decreases the kilovars supplied to the system. In this regard the synchronous condenser has greater stabilizing effect upon system voltages and likewise tends to maintain synchronism between machines. Its mechanical inertia, in general, has a further stabilizing effect upon the other synchronous machines comprising the system. By reason of these same characteristics, a synchronous condenser reduces the effects of sudden load changes or rapidly varying loads, such as drop in system voltage occasioned by starting of a large motor or operation of large welders.

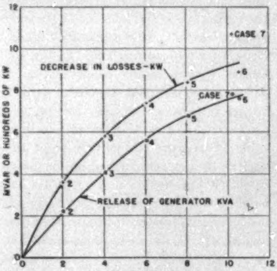


Figure 8. Reduction in system losses and reduction in kilovolt-ampere loading of the generator resulting from the addition of capacitors to the system of Figure 7

System before addition of capacitors represented by Case 1

4. For short periods of time the synchronous condenser can supply kilovars in excess of its rating at normal voltage, whereas this is not the case for capacitors.

5. The losses of synchronous condensers are very much greater than those of capacitors. For synchronous condensers the full-load losses vary from about 3 per cent for 3,000-kva units to about $1^1/_2$ per cent for very large units of 50,000 to 100,000 kva, and for capacitors the full-load losses are about one third of one per cent. The no-load losses of air-cooled synchronous condensers are about 60 per cent of the full-load losses, and for hydrogen-cooled synchronous condensers about 40 per cent; therefore, at fractional loads the losses of the synchronous condenser are not in proportion to the output in kilovolt-amperes. For a capacitor, however, the losses are proportional to the kilovolt-amperes connected to the system. Thus, the greater the load factor, the greater the benefits of the comparison of losses in favor of capacitors.

6. A comparison of the cost of synchronous condensers and capacitors involves an evaluation of the losses. Figure 9 compares the cost of air-cooled outdoor synchronous condensers and capacitors. Three evaluations for losses were assumed 0, $150, and $250 per kilowatt. The low losses of the capacitors should not be evaluated so highly as those for the synchronous condenser, because, as just mentioned, at fractional loads the losses decrease more rapidly than for the synchronous condenser. Figure 10 brings out the comparison still more clearly in that the ordinate is plotted as a ratio of the cost of the capacitor to the cost of the synchronous condenser. When losses are evaluated at $150 per kilowatt, the costs are equal at 35,000 kva, and, when evaluated at $250 per kilowatt, they balance somewhere around 100,000 kva. The balancing point between capacitors and synchronous condensers will vary with voltage and with the type of synchronous condenser.

7. Capacitors lend themselves to distribution throughout the system at several locations much more economically than synchronous condensers. Thus they may be located at points closer to the load and be more effective in their purpose.

8. The kilovolt-ampere rating of an installation can be increased or decreased as the loads and system requirements dictate much more readily with capacitors than with synchronous condensers and can be installed with greater ease. By moving capacitors from point to point as required, the installation of other equipment such as transformers may be deferred. Foundations are less important, and auxiliaries are fewer.

9. A failure of a single unit comprising a bank of capacitors affects only that unit and does not jeopardize operation of the entire bank. On the other hand, failure of insulation in a synchronous condenser is less likely to occur than failure of a single unit in a bank of capacitors.

10. Synchronous condensers add to the short-circuit current of a system and may increase the size of breaker required. On the other hand, switching of large banks of capacitors may involve large currents. In general, however, these currents fall within the ratings of circuit breakers.

11. During the present emergency critical materials require consideration. Synchronous condensers require between two and three times the critical material required by capacitors. There is the further consideration that the manufacturing space ordinarily used for synchronous condensers is now being used for the manufacture of equipment having higher priority rating. Thus, under present conditions, comparison between synchronous condensers and capacitors may be purely academic because of the difficulty of obtaining synchronous condensers.

Harmonic and Coordination With Telephone Circuits

The principal cause of harmonic voltages and currents in capacitors is the magnetizing requirements of transformers. Because of the lower impedance of capacitors at higher frequencies, the harmonic currents may become so high as to endanger the life of the capacitor or to cause excessive amounts of fuse blowing or overheating of breakers and switches. The standard margins built into capacitors, which were mentioned previously, are usually sufficient so that, for the amount of harmonic voltage present in most systems, no undue amount of trouble is experienced. For the transformer magnetizing current, the third-harmonic components and their multiples are usually supplied by circulation around the delta-connected windings. The higher harmonics are usually so small that they give no appreciable trouble so long as the transformers are operated near their rated voltage.

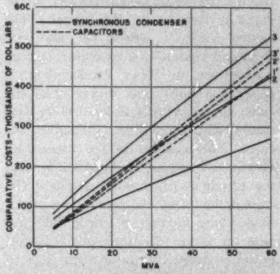


Figure 9. Air-cooled synchronous condenser and capacitor costs for a 13.8-kv outdoor installation

Capacitors connected in wye and switched in five steps. Costs do not include main circuit breaker, land space, foundations, or spare parts, but do include freight, automatic control erection, capacitor fuses, coolers on synchronous condenser, and so forth

1-1′—Cost of equipment installed

2-2′—Cost plus losses capitalized at $150 per kilowatt

3-3′—Cost plus losses capitalized at $250 per kilowatt

An unbalanced fault on a system supplied by water-wheel generators without damper windings may produce harmonic voltages. By resonance or partial resonance with capacitors, these voltages can be magnified. While the duration of the fault might not be sufficiently long to injure the capacitor, it may result in blowing of a large number of fuses in connected capacitors all over the system. This hazard is reduced by properly designed damper windings and system arrangement.

A considerable amount of study has been given to the effects of shunt capacitors on the inductive co-ordination of power systems and exposed telephone circuits at noise frequencies. These studies have been carried on by the Joint Subcommittee on Development and Research of the Edison Electric Institute and Bell Telephone System. The results of their preliminary study of the problem were included in an article published in the August 1938 issue of the Edison Electric Institute *Bulletin*. Subsequently, the joint subcommittee has co-operated with the operating power and telephone companies in a number of field studies on the effect of capacitors, and a report summarizing this experience is now in preparation. It has been found that the use of capacitors may be either detrimental or beneficial from the inductive-co-ordination standpoint, depending on the particular conditions in each case. Advance planning by the two industries along the lines suggested in the article referred to has reduced the number of troublesome situations to a small percentage of the number of capacitor installations. Where the use of capacitors has resulted in increased noise, it has been practicable generally to improve conditions by relatively simple measures applied to either the power or telephone systems or both. A summary of the available measures is included in the article.

Portable Capacitors

Several utilities have found, with a sudden increase in load at particular points on the system, a situation which is likely to occur under present conditions; portable three-phase capacitor units dispatched to the point are effective in relieving the load on the overloaded facilities until more permanent changes in the system can be made. Also, two single-phase mobile capacitor units are in use; their purpose is to relieve the overload on open-delta banks of transformers occasioned by the failure of one transformer of a three-phase delta-connected bank.

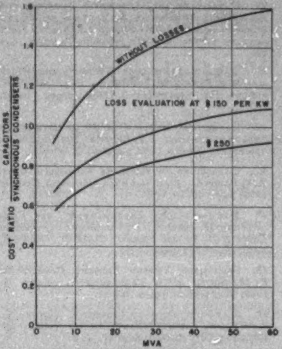


Figure 10. Relative costs of capacitors and synchronous condensers for conditions enumerated in Figure 9

Self-Excitation of Induction Motors

In some capacitor applications the system is so arranged that, in the event of opening of a feeder circuit breaker, an induction motor paralleled with a capacitor may be isolated from the remainder of the system. The stored energy in the rotor and load will then drive the motor, and, if the kilovolt-amperes of the capacitor is sufficiently large, the combination will become self-excited, the motor acting as an induction generator receiving its excitation from the capacitors. The overvoltage is rarely of sufficient magnitude to endanger the insulation. If lights are connected to the same circuit, their life may be shortened somewhat because of the magnitude and duration of the overvoltage. These situations, however, rarely occur.

Conclusions

1. Capacitors are usually the most economical form of supplying basic kilovars when installed near the load.

2. Capacitors release transformer, feeder, substation, or generator capacity, or stated otherwise, permit greater loading in terms of active or real power. This is particularly important during the war emergency.

3. Unless switching is used, the amount of capacitors that should be installed may be limited by

(*a*). Overvoltage conditions at light load.
(*b*). Synchronous machine instability.
(*c*). Over-all system energy losses.

4. The addition of capacitors reduces the system losses by supplying the load kilovar component. The greatest economy is obtained by supplying the kilovars close to the load. Reduction in total system losses reduces the fuel required to supply the load and also makes available additional generating capacity.

5. Capacitors require less critical material than synchronous condensers and do not tie up manufacturing space now required for equipment more essential to the war effort.

6. Permanently connected capacitors reduce voltage drop but have only a minor effect upon the regulation, since they raise the voltage at light load and at full load by substantially the same amount.

7. Capacitors, when located close to the load, release more kilovars in the synchronous machines than is indicated by the capacitor name-plate rating.

8. A capacitor does not have the favorable characteristic of a synchronous condenser in reducing light flicker.

9. The initial cost of capacitors and synchronous condensers balance around 7,000 kva, but, when the greater losses of the synchronous condenser are taken into consideration, the balance point will be very much greater. In this comparison it is necessary in some cases to recall that a standard synchronous condenser, if required, can also absorb kilovars from the system to an extent equal to 50 per cent of its rating.

10. Capacitors reduce billing charges, a factor of great importance to industrial customers having a power-factor clause in their power contracts.

11. Capacitors are readily made portable to provide emergency capacity where needed.

12. The proper application of capacitors requires a consideration of all components of the system, including generation, transmission, and distribution.

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The Cathode-Ray Oscillograph Applied to Long-Time Switching Transients

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Synopsis: The cathode-ray oscillograph because of its inherent characteristics is ideally suited and extensively used for the measurement of transient voltages associated with switching operations. The type of oscillograph ordinarily used covers a wide range of measurement conditions and is necessarily large and expensive. For this reason very few such units are available, and new problems, arising from the necessity of providing reliable service with loads increasing because of the war effort, have required every available measurement facility.

In two recent applications where extremely wide-range performance was not required, a two-tube low-voltage cathode-ray oscillograph was used successfully. This oscillograph and the two applications are described, and it is shown how such measurements of long-time transients may be made with portable, inexpensive, and relatively simple equipment embodying the advantages of cathode-ray oscillography.

SINCE it first began to be appreciated that the transient voltages associated with the opening or closing of an electric-power circuit had an important bearing on the switching device, as well as the connected circuit elements, much thought and effort have gone into the development of instruments which will measure these transients faithfully.[1–8] This development has led to the modern cathode-ray oscillograph which tells the complete story of the transient both as to time and amplitude.

This instrument is suited ideally to this task because of certain inherent characteristics:

1. Its indicating element, a beam of electrons, has inertialess response to the rapid changes of the transient voltage.

2. It may be constructed so that its beam intensity is adequate to give a photographic record of those changes.

3. It may be either a voltage-sensitive or a current-sensitive device, but as the former it is capable of giving a measurement with extremely low energy drain from the circuit being measured. This means that it may be used with high-impedance-capacitance voltage dividers for the measurement of extremely high voltages at transient frequencies where potential transformers, even though practical from a voltage standpoint, would not respond accurately to the rates of change involved.

4. Its beam may be turned on or off with such speed and precision that recording may be readily synchronized with the occurrence of the transient, even to the extent of having the transient itself initiate the recording sequence.

Cathode-ray oscillographs have been designed and built and are in daily use in switchgear laboratories that are capable of recording, for all three phases, many cycles of power-frequency voltage, while at the same time adequately resolving transient oscillations of the highest frequencies encountered in this type of phenomena. Such units designed to cover the full range of measurement possibility are necessarily complicated and expensive, and, while they can be made mobile, they scarcely would be considered as portable. However, this full range of measurement capability is not always necessary. In fact, it is frequently desirable to sacrifice high-speed time resolution in order to get a better picture of the relation between various parts of long-time transient phenomena.

While this, of course, is readily accomplished with the big high-speed oscillograph, only a few such outfits are available, and these just about cover normal measurement needs. With the increased loading of electrical systems because of the war effort, many new engineering problems are constantly arising. The solution of these problems frequently requires both laboratory and field investigations to supplement past experience, and additional measurement facilities must be provided.

It is apparent that applications involving long-time transients may be handled by a cathode-ray oscillograph which, without the high-speed features of the all-purpose type mentioned above, could be made far simpler and less expensive. In the following pages such a simple, inexpensive outfit is described along with two applications to switchgear field testing.

The Oscillograph

This special cathode-ray oscillograph shown in Figure 1 was designed to record long-time transients with durations up to one or two seconds. For this reason it can use a low-voltage cathode-ray tube, and the unit incorporates two such tubes. Recording is by moving film photography.

(a). Tubes

The two cathode-ray tubes which are operated at 1,300 volts have three-inch fluorescent screens of the high-peak-intensity short-persistance type suitable for moving film photography. A view from above the oscillograph looking down into the tube compartment as in Figure 2 shows one tube in full but only the white screen of the second tube which is mounted directly underneath the first one. The voltages to be measured are applied to the vertical deflection plates,

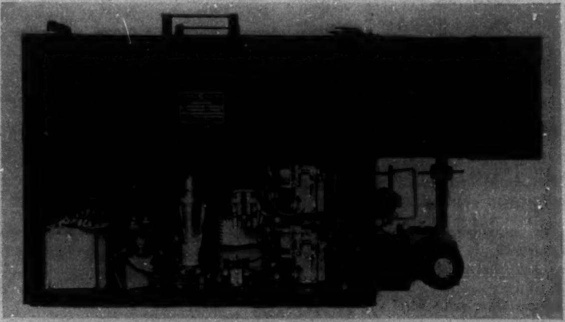

Figure 1. Special cathode-ray oscillograph

Side panel removed showing power-circuit and film-shifting relays. Film drum and drive in position

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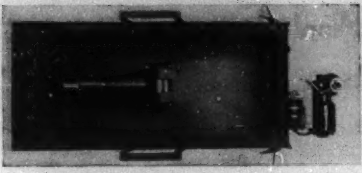

Figure 2. Top view of special cathode-ray oscillograph with cover and film drum removed, showing position of two tubes and lens

so that the traces appear on the tube screens as unidimensional Lissajous figures in line with each other, and time resolution is provided by the motion of the recording film.

(b). FILM DRUM

An *f*2.0 lens, which may be seen inside the tube compartment at the end next to the driving motor in Figure 2, focuses the cathode-ray traces on a moving film. The film, 3 $^{7}/_{8}$ by 30 $^{1}/_{2}$ inches is held by spring belts on the rotating drum shown in its holder in Figure 3. The drum holder is equipped with a manually operated shutter and a removable cover for loading. It is readily attached to the oscillograph case by spring clips as shown in Figure 1, and, when in place, the axis of the drum with its key fits inside a hollow slotted driving shaft which in turn is geared to a small electric motor. The drum is free to move inside the holder a distance of several inches in a vertical direction, so that different portions of the film may be exposed to give several records on a single film. Its vertical position is determined by the position of a supporting threaded collar which may be moved up and down on the outside threaded driving shaft. In operation the drum is automatically shifted a predetermined space after each exposure. This is accomplished by causing a solenoid operated lever to engage the supporting collar. The collar then moves downward as the shaft continues to rotate. When the lever is not engaged the collar rotates with the shaft and has no vertical motion. The distance the film is shifted is dependent on the time the lever and collar are engaged and this time is determined by the setting of a time-delay relay which controls the solenoid. A limit switch removes all power when the drum attempts to shift past the final position. All of these elements are visible in Figure 1.

(c). CIRCUIT

The oscillograph circuit is shown schematically in Figure 4. Capacitance C_1 is kept charged to 1,300 volts to supply all d-c potentials. The cathode-ray tube excitation and control voltages are obtained from the voltage divider consisting of R_1, R_2, R_3, and R_4. The beam intensity is controlled by adjustment of R_1, and, when this is set just at cut-off, the circuit involving thyratrons T_1 and T_2 may be used to initiate the beam automatically for a predetermined length of time.

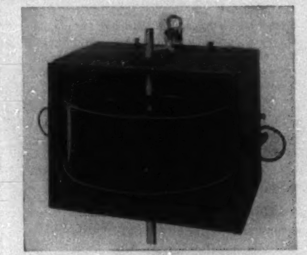

Figure 3. Film drum and holder with cover removed

Shutter visible at rear of holder

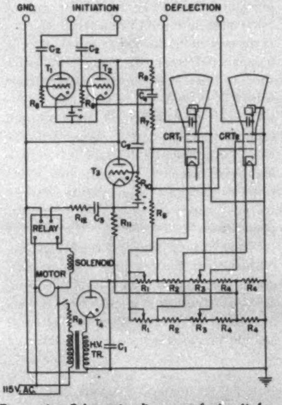

Figure 4. Schematic diagram of circuit for special cathode-ray oscillograph

For such automatic initiation a positive pulse is applied to either initiation terminal. The double input permits tripping from a surge of either polarity by supplying the trip indication through a transformer or coil with a grounded midpoint. When the trip impulse is received at the grid or grids, either T_1 or T_2 fires and discharges C_4 through R_9. This causes current to flow through R_6 and R_7, thereby reducing the cathode-ray tube-control grid bias enough to establish the beam. When C_4 is discharged, T_1 and T_2 regain control since the current through R_7 is too small to maintain ionization. However, the cathode beam is maintained until C_4 is recharged and this time may be adjusted over wide limits by selection of proper values for R_6, R_7, and C_4.

When C_4 begins to discharge, a positive pulse is applied through C_5 to the grid of thyratron T_3. This fires T_3 and discharges C_3 through the time-delay relay circuit. After this indication the time-delay relay circuit begins its sequence and after a delay of several seconds energizes the film-shifting solenoid for the period of time set by the relay adjustment.

(d). OPERATION

The normal sequence of events in obtaining a record with the oscillograph is as follows:

1. With the loaded film drum in place and set for its highest position, the tubes are energized and the motor started.
2. The shutter on the film holder is opened manually, and the tripping impulse is applied to the initiation circuit. This produces two traces on the film showing whatever voltages were on the deflection plates during the time the beam was on.

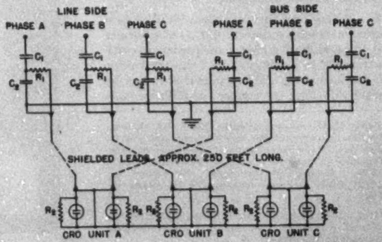

Figure 5. Voltage-divider and cathode-ray-oscillograph connections for measurements on 230-kv system at Boulder power plant

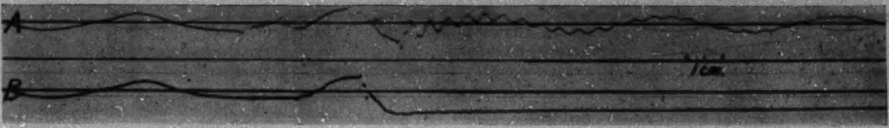

Figure 6. Sample cathode-ray oscillogram of line-dropping test on 230-kv system

A. Bus side of oil circuit breaker, voltage to ground, phase *A*

B. Line side of oil circuit breaker, voltage to ground, phase *A*

Time progresses from left to right

Scale: 1 centimeter = 4,400 microseconds

3. The film is automatically shifted to expose a fresh portion.

4. Steps 2 and 3 are repeated if desired.

5. The oscillograph power is removed and the film is ready for development.

Measurements on 230-Kv System

While the oscillograph was not originally designed for switching transients in particular, it was available and worked out very well when such an application presented itself.

When plans were made to conduct line-switching tests with a 230-kv oil circuit breaker on the system of the Southern California Edison Company Ltd. at Boulder power plant, the feasibility of taking cathode-ray oscillograms of the voltage transients was considered. It was known that the transient frequencies involved would not be very high because of the large capacitance of the connected transmission lines. In fact, the expected frequencies were not above the capability of a magnetic oscillograph. However, no potential transformers were available for the measurement of voltages which might reach crest values of several hundred kilovolts, and the capacitance potential devices normally used to measure steady-state voltages could not be expected to give true transient response with a magnetic oscillograph connected. A cathode-ray oscillograph, on the other hand, could readily be used with capacitance voltage dividers and, in addition, could be relied on to follow accurately any abrupt voltage changes such as would be associated with restriking.

On this basis it appeared that the cathode-ray oscillograph described above would be suitable, and plans were laid accordingly.

Since several of these two-tube units were available, it was decided to use three and measure voltages to ground for both line side and bus side of the breaker on each phase. Capacitance potential devices were also available in each of these positions. Such measurements involved the subtraction of two voltages to determine the voltage across a pole of the breaker but provided the desirable measurement of the actual voltages to ground to which the system insulation was subjected.

(a). Installation

The oscillographs were set up in a room under the switchyard where the breaker and capacitance potential devices were located, and the cathode-ray tubes were connected to their respective voltage dividers by shielded cables approximately 250 feet long. The connections were as shown in Figure 5, and it may be seen that the arrangement was such as to give the voltages on both sides of each breaker pole on a single film.

The voltage dividers were contrived by using the coupling capacitor units installed on the two outgoing transmission circuits, and these constitute the C_1 units in Figure 5. The C_2 units were made up of small radio-type capacitors. Two different values were used depending on the expected voltage. Ratios for the two conditions were 8,300 to 1 and 4,600 to 1 which provided for recording up to crest voltages of about 800 and 450 kv respectively.

During a test the regular potential network and carrier-current equipment were disconnected from the coupling capacitors, and connection was made to the C_2 units. Connection to the cables from the voltage dividers was through resistances, R_1, equal to the cable surge impedances. At the oscillograph end each tube was provided with a leakage resistance, R_2, to prevent accumulation of charge on the deflection plates from stray electrons of the cathode beam. All of the oscillograph grounds were connected to the cable shields, and actual grounding was done at only one place. Care was taken to minimize loops between voltage leads and ground returns.

(b). Operation

The oscillographs were initiated simultaneously by the application of a positive battery voltage. This was accomplished by a relay which was energized at the same time as the trip coil of the circuit breaker.

The film-shifting feature proved very convenient in that it was possible to record all voltages for each series of five tests without changing films. For this reason no delays in the testing schedule were chargeable to cathode-ray-oscillograph operation.

Voltage calibrations were obtained by recording steady-state voltages with the cathode-ray tubes connected to one set of voltage dividers while the line voltages were read in the usual way with the other set of potential devices in normal operation. This procedure was repeated with the roles reversed for the two sets of potential devices to complete the calibrations for all six tubes and dividers.

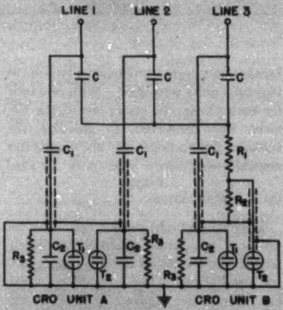

Figure 7. Voltage-divider and cathode-ray-oscillograph connections for measurements on the 15-kv system of a large industrial plant

A complete set of oscillograms was obtained for each of the 41 tests made at Boulder power plant. The results are analyzed elsewhere,[4] but a sample record is shown in Figure 6. For these tests a film speed of approximately 90 inches per second was used with a beam duration of about a half-second. This gave some overlapping of the record but was not objectionable. Zero lines were provided by tripping the beams on for an exposure with the deflection plates grounded.

It is interesting to note in passing that the entire equipment for these measurements, including shielded cable and darkroom supplies, had a shipping weight of less than 900 pounds.

Measurements on a 15-Kv System

Since the low-voltage two-tube oscillograph worked out so well on the Boulder power-plant application, it was the logical choice when cathode-ray oscillograms were required on some recent capacitor switching tests. These tests were on a 15-kv circuit at a large industrial plant. In this case line-to-ground voltages on the capacitor side of the breaker only were required, and two of the two-tube units were used. With one tube for each phase, the spare measuring element was connected to measure the voltage fluctuations of the capacitor neutral.

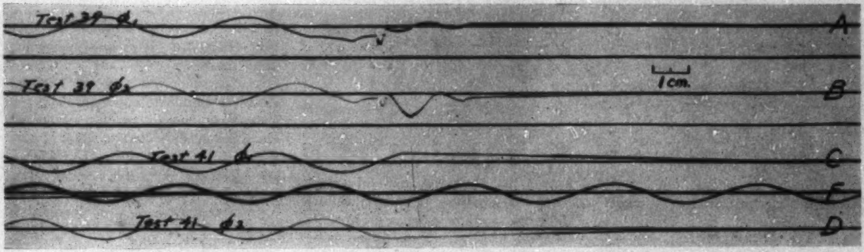

Figure 8. Sample cathode-ray oscillograms of capacitor switching tests on 15-kv system showing several tests on single film

A. Capacitor voltage to ground, phase 1, test 39

B. Capacitor voltage to ground, phase 2, test 39

C. Capacitor voltage to ground, phase 1, test 41

D. Capacitor voltage to ground, phase 2, test 41

E. Steady-state 60-cycle voltage on phase 1. Prior to test 42

Time progresses from left to right

Scale: 1 centimeter = 4,400 microseconds

(a). Installation

The oscillographs in this case were set up in a temporary shack immediately adjacent to the circuit breaker and capacitor installations. The connections which are shown in Figure 7 were essentially the same as those used at Boulder power plant with one exception. In this case the distance from the high-voltage capacitors to the oscillographs was not over ten feet and so the C_2 capacitors were mounted at the oscillograph end of the shielded leads.

The high-voltage capacitors (C_1) in this case were not regularly installed equipment but were provided with the oscillograph equipment. They consisted of porcelain wall tiles with inside and outside surfaces coated with metal foil over a sufficient area to give a capacitance of 200 micromicrofarads. The C_2 capacitors were again of the radio type and the ratio used was 300 to 1, which permitted recording up to approximately 30 kv.

For the measurement of the neutral voltage a resistance divider was used. This consisted of a string of ten two-watt metalized resistors totaling 50,000 ohms (R_1) in series with a 500-ohm unit (R_2) across which the cathode-ray tube was connected to give a ratio of 100 to 1 and recording up to ten kilovolts.

Again a single ground connection was made, in this case at the oscillographs.

(b). Operation

Initiation of the oscillographs was accomplished in the same manner as at Boulder power plant except that the relay was energized simultaneously with the starting of the magnetic oscillographs. From one to five records were taken on each film depending on the needs of the testing schedule.

Since magnetic oscillograms of the voltages on the bus side of the breaker were taken, it was possible to obtain voltage calibrations for the capacitance dividers by comparing the magnetic oscillograms with the cathode-ray oscillograms at times when the breaker was closed and steady-state voltages were present. Calibration for the resistance divider was obtained from direct measurement of the resistances R_1 and R_2.

Oscillograms were obtained for 52 tests, including both opening and closing operations of the breaker. A film speed of 90 inches per second and beam duration of a half-second were used again and zero lines were provided as before. A sample record is shown in Figure 8.

Conclusion

The preceding paragraphs have given a description of a special cathode-ray oscillograph incorporating a number of new features. This oscillograph uses the inexpensive low-voltage tube found in thousands of small oscilloscopes used in practically every laboratory, radio service shop, and engineering classroom of the country. It, however, utilizes the features of moving film recording and automatic-beam control ordinarily found only in high-voltage equipments. In addition it uses two tubes to provide for the simultaneous recording of two phenomena.

It also has been shown how this oscillograph by actual performance in the field has proved itself well-suited to the measurement of long-time transients. It compares favorably with the high-voltage cathode-ray oscillograph if high recording speeds are not required, especially if cost and size are factors to be considered. It, of course, is not to be considered as a general substitute for the magnetic oscillograph since it does not have so many recording elements. Rather it is an additional measuring device which realizes with low cost and simplicity the peculiar advantages of the cathode-ray oscillograph, low energy drain and inertialess response, for the problem of long-time transient measurement.

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Factors Involved in the Selection of Railroad Motive Power

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Synopsis: The paper presents the position of the three types of motive power in railroad service and offers a brief historical background to show how each one arrived at its present status. A table lists advantages and disadvantages inherent in each type and includes supplementary information to explain the listings in the table. The subject is presented from the viewpoint of the user, and a method of determining more closely the place for each type of locomotive is suggested. An appendix lists all of the considerations which must be included in a complete cost study.

FOR 60 years the steam locomotive was without competition, as a form of motive power, on American railroads. Its first serious competitor, the electric locomotive, appeared in 1895 and during the following 40 years displaced steam in several specific applications. The reasons for this were that the electric locomotive possessed certain inherent advantages that made it both more desirable and economical for these applications. Electrification did not displace steam operation completely, because experience demonstrated its limitations. The Diesel-electric locomotive made its initial appearance in this country—in switching service—in 1925, but, as in the case of the electric locomotive, almost ten years went by before the industry accepted it as a competitor. From 1935 to date the Diesel-electric locomotive forged ahead more rapidly than either of its competitors—electric or steam—in any similar period. There were many reasons for this. Outstanding among these are that the Diesel-electric has most of the advantages of both electric and steam motive power without too many of its disadvantages, plus an appeal to the public that increased the revenues of the railroads when an increase was desperately needed. What, now, are the limitations of the Diesel-electric locomotive, and to what extent can either the electric or Diesel-electric be expected further to encroach upon the dominions of steam.

One hundred years of railroading have demonstrated that, in the final analysis, any controversy that exists as to which is the best type of motive power must be decided upon the basis of sound economic principles. Experience has also demonstrated that there is no universal formula by which the user can determine whether one or the other type should or should not be used, but there are certain fundamentals upon which agreement can be reached, through the application of which a final choice may be made. No application of these fundamentals can be made until the problem is clearly stated.

Historical Background

The steam locomotive came into being more than 100 years ago as a competitor to the horse and the canalboat. From that point it grew in size and power, and the railroad grew with it, including the enginehouse, the shop, water and fuel services, and all facilities required to maintain and operate steam power. Most railroad operation is designed around the characteristics and requirements of the steam locomotive.

The electric locomotive was first applied to trunk-line railroad service in 1895, when it was used to eliminate a smoke hazard by hauling trains with their steam locomotives through the Baltimore and Ohio tunnel at Baltimore Md. From this beginning its use was extended to the operation of congested city terminals where smoke elimination was important, to operation of trains on heavy grades (including tunnels), to suburban passenger service requiring short headways and rapid acceleration where smoke elimination was also a factor, and to heavy-traffic general service lines where the added cost of the power distribution system could be justified by savings in operating cost and improved service.

The Diesel-electric locomotive was developed from the rail car, the latter having been used for several years to take care of diminishing traffic on branch lines. The first locomotive of this type to be used on American railroads in 1924 was in fact a rail car equipped with a heavy-duty Diesel engine and electric transmission. From this beginning the application of Diesel-electric locomotives to switching service has been extended until a railroad purchase of a new steam switcher is exceptional.

Road application of Diesel engines began with lightweight passenger trains in which the first car contained baggage and mail compartments, in addition to the space occupied by the power plant. These trains were increased in size, and the motive power grew from one to two, and finally to a three-unit Diesel-electric locomotive. Four-unit locomotives are used in freight service, and some Diesel-electric locomotives are used interchangeably in freight and passenger service.

A common error made by transportation engineers, for reasons best known to themselves, in dealing with the selection of motive power has been to approach the problem primarily on the basis of substituting one type of motive power for another, whereas actually the ultimate solution may be an economically sound use of combinations of different types—each in the place where and to the extent to which it is best suited.

Table 1. Analysis of Motive-Power Characteristics

	Electric	Steam	Diesel-Electric
1. Operating Cost			
(*a*). Fuel	*D*	*D*	*A*
(*b*). Water	*A*	*D*	*A*
(*c*). Lubricants	*A*	*A*	*D*
(*d*). Enginehouse expense	*A*	*D*	*A*
(*e*). Supplies (of no great importance)			
2. Fixed Charges			
(*a*). First cost	*D*	*A*	*D*
(*b*). Depreciation	*A*	*A*	*D*
(*c*). Interest, taxes and insurance	*D*	*A*	*D*
3. Operating Characteristics			
(*a*). High acceleration	*A*	*D*	*A*
(*b*). Overload capacity	*A*	*D*	*D*
(*c*). Availability	*A*	*D*	*A*
(*d*). Flexibility	*A*	*D*	*A*
(*e*). Use of factor	*D*	*D*	*A*
(*f*). Freedom from complete failure	*D*	*D*	*A*
4. Special Features			
(*a*). Smoke elimination	*A*	*D*	*A*
(*b*). Damage to roadbed	*A*	*D*	*A*
(*c*). Merchandising appeal	*A*	*D*	*A*
(*d*). Dynamic braking	*A*	*D*	*A*
5. Possibility of Eliminating Facilities			
(*a*). Operating	*A*	*D*	*A*
(*b*). Maintenance	*A*	*D*	*A*

A—Advantage. *D*—Disadvantage.

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The first requisite is to state the problem; in simple terms it is how to decide whether or not to use steam, Diesel-electric, or electric motive power, or combinations thereof, for performing the required functions of a railroad. A study of the problem may, for example, indicate that, to do a specific job, a railroad should use Diesel-electric switchers, Diesel-electric road passenger locomotives and steam freight locomotives, or possibly Diesel-electric switchers and electric road passenger and freight power. It is quite possible that, to the man with an open mind, the very effort to prove the case for one type of motive power as against another may demonstrate the logic of utilizing combinations.

The second requisite is the necessity of collecting a variety of data relating to the specific application under consideration. The nature of those data may be seen by reference to Appendix A.

Once the basic data have been collected and studied, an equal familiarity with the characteristics of the three types of motive power should be acquired. With that end in view Table I has been prepared. For those interested in the reasons why any given characteristic is considered an advantage or a disadvantage, it may be of value.

Fuel

When the cost of coal delivered to the tender is four dollars a ton, the cost of coal is about the same as the cost of power required for an electrified line. This relation will vary considerably, depending upon the power contract, the amount of transmission and distribution losses, and so forth. But experience indicates that if the cost of coal is less than this, no savings in fuel cost can be made by electrifying a steam-operated railroad. For class I railroads the average cost of coal delivered to the railroad is $2.68. To this must be added the cost of distributing it to the coaling plants and loading it on the tender. The cost of handling coal from the car to the locomotive (including fixed charges) is about 15 cents per ton. Obviously, the railroads which are remote from sources of supply pay a greater hauling charge than those with short hauls from the mines.

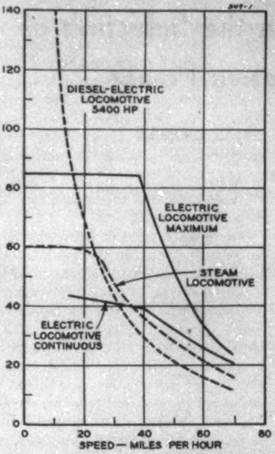

Figure 1. Comparative drawbar-pull–speed curves of electric, steam, and Diesel-electric locomotives on one per cent grade

Type Locomotive	Weight on Drivers	Total Weight
Electric	360,000	460,000
Steam	281,440	1,060,010 (incl. tender)
Diesel-electric	856,000	856,000

Fuel costs for steam and Diesel-electric locomotives can be compared best from performance records. One railroad using both types for freight service reports a fuel cost of 33 cents per mile for coal-burning steam locomotives and 28 cents per mile for Diesel-electric locomotives in the same service. These figures favor the steam locomotive somewhat, since in this instance the Diesel-electric locomotives are operated in high-speed service, whereas the steam locomotives are used at lower rates of speed.

In passenger service one road reports fuel costs of 17 cents and 10 cents for steam and Diesel-electric locomotives, respectively, a saving of 41.1 per cent. In two other cases the savings reported are, respectively, 16.3 and 39.8 per cent.

It is in switching service that the Diesel-electric locomotive makes the greatest saving of fuel. There are three reasons for this. One is the inherently high thermal efficiency of the Diesel engine. Another is the relatively high standby losses of the steam locomotive, and the third is the electric transmission which permits the Diesel engine to operate near its full-load rating at all locomotive speeds. Hourly fuel costs taken from a recent report are shown in Table II.

Water

The average water cost for a steam locomotive is approximately ten per cent of the fuel cost. On Diesel-electric and straight-electric locomotives, water is a relatively small item, even in passenger service which requires oil-fired boilers on the locomotive for train heating and for the steam-ejector type of air conditioning. The amount of water used for this purpose is only about one tenth of the total water used for all purposes by a steam locomotive in the same service. In freight and switching service, the electric locomotive uses no water, and the amount needed by the Diesel-electric locomotive is practically negligible.

Lubricants

An empirical method of approximating lubricating costs for Diesel-electric locomotives is to assume that the consumption of lubricating oil is from $1^1/_2$ to 3 per cent of the fuel oil, whereas the cost per gallon is ten times as much. Thus the cost of Diesel-electric lubricating oil is from 15 to 30 per cent as much as the fuel cost. Steam locomotives doing the same work will consume lubricants costing about half as much as those used by the Diesel-electric locomotive, and accumulated experience shows that the cost of lubricating electric locomotives is much less than that for steam.

Enginehouse Expense

Enginehouse expense is a disadvantage of the steam locomotive. In road service it may be from 1.6 to 4 times as much for the steam as for the Diesel-electric locomotive, while in switching it may be from 5 to 12 times as much. Thus on roads using both types of power we find such costs, as are given in Table III.

Electric-locomotive enginehouse expense is less than that of the Diesel-electric, since it is essentially a Diesel-electric without the Diesel engine. The high steam costs arise from the fact that the steam locomotive requires periodic attention for the cleaning of flues, boilers, grates, and so forth, with the resulting need for resteaming and attention to fires.

Locomotive supplies are an item included in all locomotive cost accounting, but they are a small one, and there is so little difference for each type that this is not a controlling factor.

Smoke Elimination

Electric locomotives are completely effective in eliminating smoke. They have been responsible for the transforming of smoking yards into first-class residential districts and for the safe operation of trains through long tunnels. Diesel-electric locomotives produce little smoke, but their exhaust gas and the fuel they carry have precluded their use in some terminals.

Damage to Roadbed

Electric and Diesel-electric locomotives which have no unbalanced weights in their wheels do not produce what is known as dynamic augment. This adds to the problem of steam-locomotive design for high speeds and in the case of slipping wheels may result in track stresses sufficient to kink the rails. The smaller wheel diameters and lower center of gravity used with Diesel-electric locomotives increase shearing stresses at the head of the rail, but this is largely offset by lower axle loads. It is generally accepted that the electric and Diesel-electric cause less damage to roadbed than steam locomotives, but much work is being done to improve the characteristics of the latter at high speeds.

Merchandising Appeal

Diesel-electric-powered streamlined trains were introduced in 1933, and the great increase in passenger traffic between 1933 and 1941 is good evidence that people like them and will desert the highway for them. A part of the credit for this shift must be given to fare adjustments and a part to air conditioning, but undoubtedly the Diesel-electric locomotive deserves its share.

At a meeting of the American Association of Passenger Traffic officers in November 1941, an attempt was made to evaluate the influence of passenger service on freight traffic. A poll of railroad traffic officers showed that none think it of no importance, and 84 per cent of those questioned feel that the influence of passenger service is either a major or an important factor. A somewhat different attitude is shown by traffic managers who buy transportation. Almost half of a number of these men who were questioned said that the great bulk of freight traffic is routed without consideration for passenger service. Even though the latter group is most nearly correct, it is obvious that passenger service is an important influence.

Dynamic and Regenerative Braking

Dynamic braking has been developed for the Diesel-electric locomotive. This consists of using the traction motors as generators and dissipating the energy generated in resistors. The brakes are designed to hold, at a definite speed on a descending grade, the same train weight that the locomotive can haul up the same grade at the same speed. In actual operation, on descending grades, train weight may be heavier, and speeds maintained are higher than on the same grade ascending, so that for controlling such trains some assistance for the dynamic brake is required from the train air brakes. In trials on various grades, the amount of train air braking required was reduced to approximately one fourth of that required by conventional braking with train air brakes. This represents an important reduction in brake shoe and wheel wear and reduces damage to wheels by overheating. It is also a safety device.

The electric locomotive is capable of employing regenerative braking—of returning the power generated in the motors to the power system. It is definitely superior to dynamic braking for improving train operation, since it allows closer speed regulation on the down grades, and in actual practice on heavy grade lines it makes an appreciable saving of the electric energy required by the railroad.

Fixed Charges

In the matter of fixed charges against operation, we are concerned with two major factors: first cost and service life. Originally, the Diesel-electric locomotive was at a distinct disadvantage with respect to steam because of high initial cost. However, progress in design and production methods, as well as an increased volume of production, has reduced the initial cost of Diesel-electric locomotives to approximately $87.50 per engine horsepower as compared with approximately $35.00 per cylinder horsepower for steam.

The cost of an electric road locomotive is about $65.00 per continuous horsepower at the rail. To this must be added the cost of the power-distribution and contact system, but no close estimate of its cost can be made since, roughly speaking, the same system that serves ten locomotives can also serve a hundred. If a substitution of Diesel-electric or electric locomotives for steam is being considered, some credit should be given to the replacing units for fuel-, water-, and ash-handling systems and such enginehouse and shop facilities as are not required by electric or Diesel-electric power. In any plan for improvement some real thought should be given to the elimination of terminals made possible by improved locomotives and better operation.

To become involved here in any discussion of the depreciation question would serve no good purpose. Experience with steam and electric locomotives has demonstrated that a depreciation rate somewhere in the neighborhood of 4.5 per cent well serves the purposes of the railroad industry's present methods of accounting. In the matter of Diesel-electric locomotives, the question of depreciation has been approached in a somewhat different manner than has been the case with steam. By this is meant that recognition has been taken of the fact that a Diesel-electric locomotive is, for example, 40 per cent Diesel engine, 40 per cent electric equipment, and 20 per cent mechanical equipment. Because of this fact, three depreciation rates might be desirable: one to the mechanical equipment, one to the electric equipment, and one to the Diesel engine.

Years of experience with both mechanical and electric equipment, such as is now used on Diesel-electric locomotives, have simplified the problem of establishing depreciation rates. The unknown factor has been that of the service life of the Diesel engine. It is quite probable that time will demonstrate that the economic service life of railway-type Diesel engines may be longer than was anticipated in the early days of their service on the railroads. To summarize this matter of depreciation without offering any conclusion, it may be said that individual roads have established depreciation rates for Diesel-electric locomotives all the way from four to ten per cent annually.

Operating Characteristics

High acceleration is an inherent characteristic of both Diesel-electric and electric locomotives and a characteristic which has been lacking in steam locomotives, particularly of older designs.

Overload Capacity

The electric is the only one of the three types of motive power which has real overload capacity. Steam locomotive horsepower depends upon mean effective piston pressure and speed, and horsepower outputs near the maximum are obtainable over only a short portion of the speed range.

The maximum horsepower output of a Diesel-electric locomotive is dependent upon the capacity of the Diesel engines.

Table II. Hourly Fuel Costs

	Steam	Diesel-Electric	Saving
Railway *A*—600-hp Diesels	$0.801	$0.224	$0.577
Railway *B*—600- and 1,000-hp Diesels	1.120	0.370	0.750
Railway *C*—1,000-hp Diesels	1.0636	0.3267	0.7369

It cannot be increased above this amount, but the electric transmission permits the use of this horsepower over a relatively large part of the speed range.

The relation of continuous tractive force to maximum tractive force of an electric locomotive is shown in Figure 1. Because the electric locomotive can draw from the power-supply lines up to the heating limits of the motors, it can for short periods deliver horsepower outputs which are almost twice the continuous rating. Thus a locomotive having a continuous rating of 4,860 horsepower at 65 miles per hour may be capable of outputs at the driving wheels up to 9,000 horsepower when the service conditions demand. In so far as performance is concerned, it may be looked upon as the ideal locomotive. In actual practice, an electric locomotive having 72 per cent of its weight on drivers can exert enough tractive force to slip its drivers at any speed.

To obtain this overload capacity, operation must be so arranged that the motor temperatures are well below maximum permissible values before the extra horsepower is called for. One overload must not be superimposed on another. Within the limits of its horsepower capacity the Diesel-electric locomotive is similarly limited.

Availability

When a steam locomotive arrives at its terminal, it requires certain servicing and repair operations. A study recently made by one railroad indicates that steam freight locomotives are in service approximately 12 hours out of a 24-hour period, that they spend eight hours at terminals undergoing servicing and repairs and the other four hours waiting for something to do. This corresponds to an availability factor of $66^2/_3$ per cent. As compared with this figure, the Diesel-electric locomotive will have an availability factor of possibly 90 per cent and the electric locomotive perhaps 95 per cent. Availability of any type of motive power can be improved by the intelligent use of modern terminal facilities.

Table III. Enginehouse Expense

	Steam	Diesel-Electric
Freight service (per mile)	$0.04	$0.01
Passenger service (per mile)	0.02	0.012
Switching 1 (per hour)	0.393	0.038
Switching 2 (per hour)	0.2279	0.0427
Switching 3 (per hour)	0.47	0.04

Flexibility

The flexibility of Diesel-electric locomotives is an "advantage characteristic" brought about by the fact that it is built in the form of "packaged power": for example, in 1,000- or 2,000-horsepower units, from one to four of which may be used to provide the power required for a variety of operating conditions. When, for example, a 5,000-horsepower steam locomotive is used on a "1,000-horsepower job," the loss to the operating company entailed by the resultant low load factor is obvious.

What has been offered in this paper may be considered as a pattern or design for procedure. It is suggested that it now be implemented by setting up a set of specific conditions on a railroad or a section of a railroad. This specific case could then be placed in the hands of three groups of recognized transportation engineers experienced in the application of steam, electric, and Diesel-electric motive power. Each group could then proceed to determine the extent that each type should be used for this specific case. When put together, the three reports would indicate to what extent definite values can be given to the many factors involved.

Appendix A

Data Required for the Selection of Proper Motive Power for a Specific Service

1. No rule-of-thumb method for selection of type.
2. Data required for selection of type:
   - Density of traffic.
   - Permissible hours of operation per year.
   - First cost of each type plus attendant facilities.
   - Maximum starting tractive force.
   - Maximum running tractive force and speed.
   - Limiting track and bridge conditions.
   - Clearance diagram.
   - Data on all track curves.
   - Maximum operating speed.
   - Maximum tonnage of train and number of cars (empties and loads).
   - Complete profile and track alignment.
   - All speed restrictions.
   - Operating schedule with normal train.
   - Operating schedule with maximum train.
   - All intermediate stops and stop time.
   - Helper zones and conditions.
   - Altitude and climatic conditions.
3. Method of determining physical characteristics and capacity:
   - Determine weight required on drivers.
   - Pick wheel arrangement to meet track curve and speed requirements.
   - Estimate power requirements, and justify by the necessary speed-time calculations.
   - See that this power may be built into the clearance limitations and with above wheel arrangement.
4. Data required to compute operating costs:
   - Wage agreements.
   - Number of men in crew.
   - Coal or steam oil-fuel cost (including all transportation and handling).
   - Steam fuel consumption.
   - Diesel fuel cost (including all transportation and handling).
   - Diesel fuel consumption.
   - Electric-power cost (rates and demand charges).
   - Electric-power consumption (including losses from source to locomotive).
   - Combined power-consumption chart (based on train sheet).
   - Lubrication cost for each type of locomotive.
   - Water cost and consumption for each type.
   - Cost of other supplies.
   - Enginehouse expense for each type (actual, not assumed).
   - Repair costs averaged for 20 years for each type.
   - Operating cost of distribution equipment (electrification only).
   - Maintenance cost of distribution system (electrification only).
   - Operating miles or hours per year for each type of power in each class of service.
5. Data required to compute fixed charges:
   - First cost of each unit of motive power.
   - Plotted train sheets to determine number of motive power units including spares.
   - First cost of all necessary attendant facilities.
   - Interest rate on borrowed money.
   - Depreciation rate for each locomotive and facility.
   - Tax rate.
6. Total cost to own and operate each type:
   - Compute cost of operation per mile or hour.
   - Multiply by total hours of operation per year (including relief).
   - Add all fixed charges.

There is no rule-of-thumb method by which the proper type of motive power (steam, electric locomotive or multiple-unit car, Diesel-electric locomotive or Diesel-electric railcar) may be selected for a given service. Whereas it is true that experience has proved that one or another type of power is the most economical for a specific operating condition (a Diesel switcher is economical where it can be operated 24 hours per day), general classes of service cannot be so segregated. The density of the traffic, the permissible hours of operation per year, the first cost of the motive power and facilities, the relative operating costs of each, and other pertinent conditions must all be studied. The answer will be found in the summation of costs for owning and operating each type.

The study which must be made in order to arrive at an intelligent selection of the most suitable and economical locomotive or car involves three general steps:

1. Determination of the physical characteristics and horsepower capacity of the vehicle.
2. Compilation of the comparative yearly operating costs of each type under consideration.
3. Addition of the yearly fixed charges of the motive power and its appurtenances (which include the power distribution system in the case of electrification).

Selection of Locomotive Characteristics

The selection of the proper locomotive weight and wheel arrangement and a determination of the power which should be built into the locomotive must be the result of careful study of all of the operating requirements to be met. Among these are:

Maximum starting requirements.
Maximum running tractive force at a known speed.

Limiting track or bridge conditions.
Clearance diagram.
Sharpest curves which must be negotiated.
Maximum speeds of operation.
Maximum tonnage to be hauled.
Complete profile and track alignment.
All speed restrictions.
Operating schedule with normal train.
Operating schedule with maximum train.
All intermediate stop time.
Grades where helpers may be used.
Altitude and climatic conditions.

The weight per driving axle is limited by track and bridge strengths, the minimum weight on all drivers is fixed by the maximum tractive force required (starting or running), the permissible rigid wheel base is determined by the sharpest curves to be negotiated, the use of guiding axles or trucks is a function of the maximum speeds, and the power which must be applied is determined by the train weights, profile, speed restrictions, and necessary schedules to be met. The steam or electric locomotive selected may be one which has been built previously or a modification thereof, while the Diesel-electric is usually the nearest commercial size (in production) which fulfills the conditions.

Operating Costs

With the operating conditions known, and a locomotive of each type selected (or possibly a railcar or multiple-unit cars), the next step is to determine the operating yearly costs for each system. There are seven general cost items which form the basis for compiling these figures. In addition, electrification carries two other charges—operation of substations and maintenance of distribution system. These charges, then, are:

Wages—enginemen and train crew.
Fuel (steam or Diesel-electric) or power cost (electric) at the locomotive.
Lubrication.
Water (only for train heating boilers in Diesel-electric or electric locomotives).
Other supplies.
Enginehouse expense.
Repairs—running and class.
Operation of distribution equipment (electrification only).
Maintenance of distribution system (electrification only).

Since wage rates, fuel consumptions, and other cost items may vary with different train consists, all such figures should be compiled on the basis of yearly aggregates, taking into consideration the hours or miles (and costs) operated by relief power of another type.

Wage rates are subject to agreement between the railroad brotherhoods and the railroad, and wage-rate books are normally available. Enginemen are usually paid on the basis of weight on drivers, so that excess weight should be avoided.

The cost of coal for the steam locomotive should be the base rate (cost at the mine plus the freight charges to the lines of the railroad under study), plus a charge for hauling cost on its own line. To this must be added the handling cost per ton, which includes the operating costs on the coaling station. The fuel consumption rate and therefore the cost per mile or per hour must be determined from previous steam records for each train consist.

The cost of fuel for a Diesel-electric locomotive should be the cost in tank-car lots plus the two freight charges and the handling already enumerated. Approximately 260 gallons of Diesel fuel weigh a ton, so that the cost of transporting on company lines is small. The fuel consumption and therefore the cost per mile or per hour for each train consist may be determined by a consideration of the profile, Diesel-engine fuel rates, and engine loading for the complete service cycle, including engine idling.

The cost of electric energy must be obtained from a known power cost (rate plus demand charges), starting with the locomotive power consumption and adding transformation losses (transformers, rectifiers, rotary converters, motor generators, power-factor correction, and so forth, as used in the system considered), and distribution losses. The aggregate power consumption and the peak demands are determined by a close study of the power required by the individual trains over the profile and the train sheet from which a combined load curve is made.

Lubrication costs for each type of motive power are fairly well known. Lubrication costs of the Diesel-electric, because they include that of the engine, are usually much higher than for either of the other types of locomotives.

Water costs for steam locomotives are relatively high, as the other two types use water for train heat only.

Other supplies are a minor item but are usually higher for steam than for either of the other two motive-power units, although for multiple-cab Diesel-electric locomotives this cost may exceed that of steam.

Enginehouse expense is somewhat difficult to obtain. A steam locomotive requires considerable handling and care, including ash handling, boiler washing, and so forth. The Diesel-electric locomotive requires less attention than the steam locomotive, but more than is necessary for the electric locomotive. This item of cost is usually recorded as cost per engine handled (per engine turned).

For Diesel-electric power there is a tendency to assume that the enginehouse expense is low and arbitrarily to include a low figure in the cost comparison. This is sometimes an erroneous method, however, for unless the use of the Diesel-electric locomotive actually results in a reduction of enginehouse personnel, there is no real saving to the railroad. This is often the case where one or two Diesel-electric locomotives operate out of an enginehouse handling many steam locomotives. The best way to figure the Diesel-electric enginehouse expense is to compute the actual enginehouse-expense saving (not to be confused with savings in personnel as a result of lower repair labor required), and subtract this from the normal steam expense to give the Diesel-electric expense figure.

In an electrification it is frequently possible to eliminate all steam power from the enginehouses, and this will effect very substantial savings in enginehouse costs.

Repair costs for steam power must be prepared from averages of similar operations over a long period of years corrected to the present-day value of the dollar. The reason for this is that certain costly repairs (classes I, II, and III) occur more or less infrequently and yet must be considered in the averages for the life of the locomotive. Similarly, Diesel-electric and electric motive-power-repair expense figures must have a contingent amount included over and above normal current repair costs to cover reconditioning of long-life items. It may be pointed out that while the repair costs of the running gear and electric equipment are nearly comparable for Diesel-electric and electric locomotives, the repair cost of the Diesel-electric will always be much higher than for the electric unit, because of the engine-repair costs, and also because it may be necessary to use a multiplicity of Diesel-electric cabs to do the work which may be performed by a single electric cab.

Locomotive Utilization

With all of the costs tabulated, the operating cost per year for each method of propulsion may be determined by combining these with the cost of the necessary relief-locomotive costs where the relief locomotive differs in type. In the case of steam or electric operation, it may be assumed that sufficient reserve motive power is available to fulfill the scheduled service with that type of power, but where relatively few Diesel-electric motive power units are available, the total miles or hours of Diesel-electric operation are lower than the yearly requisite by the amount of relief necessary. The total hours and cost must then be filled out by steam-locomotive relief time and cost.

In the case of switching locomotive power, it is often found that the Diesel-electric will cut the number of hours of service required to do a given amount of work. This is particularly true in congested yard service, where a Diesel-electric locomotive will switch from 20 to 33 per cent more cars per hour. In isolated service, however, a one-to-one ratio usually holds, except that the Diesel-electric locomotive will frequently reduce or eliminate the overtime hours which are necessary with steam. To determine the actual ratio of hours (straight and overtime), it is necessary to analyze all switching-locomotive assignments. In preparing a switching comparison of operating costs, it is usually desirable to segregate the cost tabulation into those services where the operation is continuous, where locomotives are assigned 16 hours per day, and where assignments are for one trick per day only. In the last case, an economic justification of Diesel-electric locomotives depends upon a very low Diesel-electric first cost as compared to steam.

Fixed Charges

To figure the total cost of owning and operating any type of motive power, it is necessary to include those annual charges which are applicable. These fixed charges, independent of the intensity of utilization, are:

Interest on the investment.
Depreciation charge.
Taxes.
Insurance.

Since it is necessary for railroads to borrow money to purchase new motive power, the annual interest charge should be figured on the purchase price plus the first years' interest charge, which is usually paid in advance.

The depreciation rate is usually higher for Diesel-electric locomotives than for either of the other types of power. When added experience with this type of motive power has been obtained, it may be found proper to establish a rate of depreciation comparable to that used for other types of power.

Taxes and insurance vary for different localities and must be determined on the same investment as is used for calculating the depreciation charge.

Total Yearly Cost of Each System

By totaling the fixed charges for any type of power and adding to its yearly operating cost, the annual cost of owning and operating each type is obtained. Since the selection of a system of motive power is usually based on financial considerations, the results of the tabulations should have considerable weight in determining the type of locomotive which should be purchased.

Comparative Figures Must Be Fair

In order to be of value in analyzing the relative merits of the different types of power, all figures and calculations must be fair. If the approach to such an analysis is biased by a predilection for any one of the types, there are many ways in which a preference may be shown. The results of such a tabulation, however, are valueless.

Field for Electrification

The traffic conditions which must exist in order to justify electrification are fairly well known. Unless the traffic density is high, or special conditions exist to warrant the cost of erecting a power distribution system, consideration of electrification is seldom warranted. It must be understood, however, that, where concentration of locomotive power is essential, no other type of propulsion as yet devised can compete with the electric locomotive.

Field for Diesel-Electric Motive Power

Diesel-electric motive power is extremely flexible in its range of operation and has fairly low operating costs. Where it may be used intensively, so that the fixed charges per mile or hour are low, the total cost of owning and operating units of this type is in its favor. For the large bulk of the locomotive operations in the United States, however, freight locomotives average but 102 miles per day and passenger locomotives but 166. Until the first cost of Diesel-electric power is not more than 50 per cent greater than that for satisfactory steam power, the savings which may be made by Diesel-electric road power will not justify the *replacement* of steam power in the average service. However, as the existing steam power must be retired because of age, the Diesel-electric savings need only justify the *additional* price necessary to acquire the Diesel-electric instead of new steam power.

Diesel-electric locomotives may be used more intensively than steam power. This results, in some instances, in replacing several steam locomotives with one Diesel-electric locomotive of the same train-hauling capacity (two or three to one).

Fundamental Basis for Motive Power Study

It should be emphasized that all comparative studies should start with a consideration of the characteristics of the railroad (profile, alignment, speed restrictions, and so forth) and of the maximum size of train to be handled. Where existing locomotives are handling a service, it is frequently possible to apply larger Diesel-electric or electric locomotives and show excellent economies. On the other hand, new steam power of the larger hauling capacity would also show improved economies. The exception to this, of course, is where the electric locomotive will afford a power concentration not available in either of the other types and thus effect savings or improvement in service not obtainable with other than electric locomotives.

The Power-Recovery System of Testing Aircraft Engines

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FOR its part in the development of the power-recovery system of testing aircraft engines the electrical industry may properly take pride. It has aided the war effort by helping to give to the aircraft-engine industry a testing technique that contains advantages not previously available. In this system of testing worth-while energy can be saved—more than half that required to operate an aircraft-engine manufacturing plant—and power demand can be reduced to the extent that power-demand costs and the capacities of power-supplying apparatus are materially reduced, engines can be tested with the ease and the quietness typical of electric machinery, testing can be carried on in buildings of simplified design, accurate electric load readings can be obtained to check and substantiate readings taken by conventional engine-testing methods, and, as reported by an aircraft engine builder, testing may be accomplished more quickly than with other systems. All of this can be done too on a basis that is economically sound.

What is the power-recovery system of testing aircraft engines? It is the purpose of this paper in its entirety to answer that question. How was this system of testing started, and what were the reasons for developing it? The answer to this latter question can be given in a few paragraphs.

The development of the power-recovery system of testing aircraft engines started in the fall of 1937 as a result of an inquiry from Pratt and Whitney Aircraft, division of United Aircraft Corporation, East Hartford, Conn.

In substance, Pratt and Whitney Aircraft asked: Can a system of testing be devised which will be economically efficient and which will make the loading of large engines easy and simple, and which will, at the same time, conform to the requirements of aircraft-engine testing?

The following, again in substance, is an explanation of why such an inquiry was made:

Extensive engine testing under loaded engine conditions has always been a necessary part of aircraft-engine *design and development* and a necessary part of regular everyday aircraft-engine *manufacture or production.* Such testing is necessary during development, because new designs, either of complete engines or of engine parts, must be verified by test. Such testing is necessary during manufacture, because it is by test only that each engine coming through production can be proved satisfactory and acceptable. The former class of testing, generally referred to as *laboratory or development* testing, is only of incidental interest in the present purpose. This development testing is generally accomplished by means of some form of cradled dynamometer or by means of such a dynamometer and a test stand on which the engine can be mounted and loaded by means of a propeller. The main issue and the real point of discussion is the latter form of testing, testing during manufacture, or what is commonly referred to as *production testing.*

Associated with production testing of aircraft engines to such an intimate degree that it becomes an inherent part of this testing is the breaking in of the engine. When an aircraft engine is placed in an aircraft, it must be ready to deliver its rated output. It must be ready to take off and fly.

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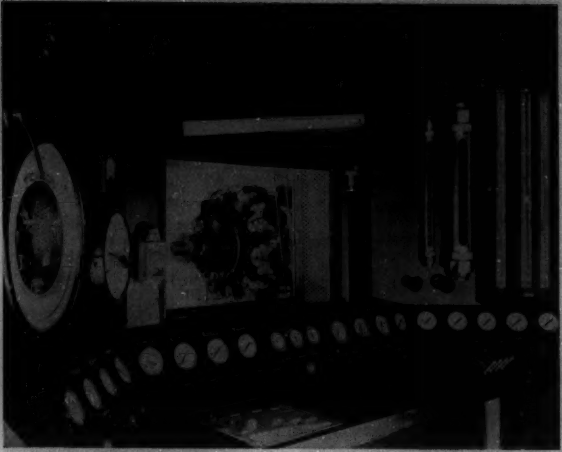


Figure 1. Aircraft engine in engine test room ready for moving in place for connection to power-recovery test stand

View is through the test operator's observation window. Test operator's control bench is in the foreground

Thus, for production testing of aircraft engines the aircraft-engine industry has always used testing techniques that involve several relatively lengthy loaded engine operating periods during which an engine is simultaneously proved in and broken in.

The situation in 1937 was that the only schemes used for production loading of aircraft engines were those involving propellers, water brakes, electric brakes, and similar devices, all of which involve disposal of engine power by complete wastage. The fuel consumed accomplished no useful purpose other than to permit testing the engine. Furthermore, engines were growing in size to the extent that testing schemes involving the disposition of power by wastage were beginning to present difficult problems.

The Pratt and Whitney Aircraft inquiry, therefore, led to the finding of a better and easier way to test aircraft engines and one in which the greater part of the power generated by the engine was recovered.

First Power-Recovery Testing Installation

The first solution was a power-recovery system employing a synchronous machine with a slip coupling.

Figure 2 shows schematically the arrangement of this power-recovery stand. This consists of a synchronous machine, which acts as either a motor or generator, and a hydraulic slip coupling interposed between the engine and the synchronous machine. Two such equipments were placed in operation at Pratt and Whitney Aircraft, East Hartford, Conn., in the spring of 1940. Figure 3 shows the electric part of a similar equipment subsequently supplied to another engine builder. The hydraulic coupling, to be added to the electric unit, will be mounted on the main base extension shown.

Operation of Slip-Coupling Synchronous-Machine Test Stand

It is an obvious truth that the only generally universal way to dispose usefully of the aircraft-engine output is to convert that output into electric energy and dispose of the resulting electric energy by making it available in the engine-plant electric power mains. In other words, any universally applicable power-recovery system of testing must be an electric system, and it must have as its foundation of operation an electric generator of one form or another.

In Figure 2, the generator is a synchronous machine. It is equipped with an amortisseur winding and is started and connected to the line by being started at full voltage in the conventional synchronous-motor manner. During starting the coupling is set to transmit no torque, so that the synchronous machine starts unloaded, and the engine and engine half of the coupling remain at rest. After being connected to the line, the synchronous machine functions as a motor or generator, depending on whether mechanical energy is taken from it or delivered to it.

The synchronous machine operates at only one speed, synchronous speed, whereas the engine must operate over a range in speed. Furthermore, the synchronous machine will not control the loading of the engine. Within the limits of its capacity, it will deliver or absorb as much power as may be taken from it or delivered to it. Therefore, by itself, it will not meet two basic engine-testing requirements, namely, the control of the speed and the control of the torque at which an engine is loaded. Accordingly, the slip coupling is inserted, and it is the function of the slip coupling to provide these requirements.

The operation of this combination of synchronous machine and slip coupling is this: After the synchronous machine has been brought to speed and synchronized in the manner previously mentioned, the coupling is controlled to make the synchronous machine "turn over" the engine or run the engine mechanically. Then the engine is "fired," and after it is warmed up, it is brought, by its own power, to a speed above that of the synchronous machine. The synchronous machine, then acting as a generator, loads the engine on the power system by slipping in the coupling. The amount and the speed of this loading are controlled by the engine throttle and the degree to which the coupling is "energized."

It is of parenthetical interest that in this application the hydraulic slip coupling is used for the first time in a new role. Prior to its use here, it had been used either at low-slip values or on drives with rapidly decreasing power with increase in slip, such as fan drives. In the engine-loading application, the power to be transmitted through the coupling increases and increases rapidly with increase in coupling slip.

Engine and Test-Stand Loading

A power-recovery test stand designed for engine loading or generating requirements generally will have ample capacity for any corresponding motoring requirements. As a simple illustration of generator and coupling loading and power distribution between them, assume that an engine with the characteristic shown in Figure 4 is to be tested at speeds of 1,000 rpm, and 1,200 rpm, and that the synchronous machine has a speed of 720 rpm. Then, neglecting test-stand effi-

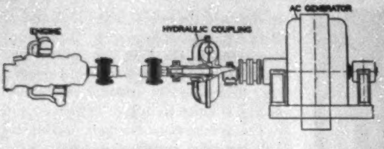


Figure 2 (left). Schematic view of the first power-recovery test stand

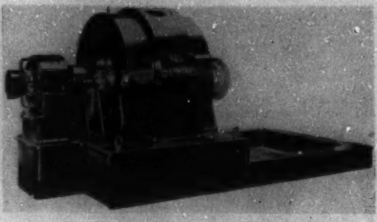


Figure 3 (right). Power-recovery test stand of the synchronous - generator type designed for the arrangement shown in Figure 2

The hydraulic coupling will be mounted on the base extension

ciency which will be mentioned presently, the power distribution is as follows:

Engine-Output Shaft (Propeller Shaft)		Coupling Loss*		Generator Loading**	
Speed (Rpm)	Horsepower	Horsepower	Kilowatts	Horsepower	Kilowatts
1,000	920	258	192	662	490
1,200	1,550	620	463	930	695

* Proportional to slip: (1,000 − 720 = 280)/1,000 × 920 = 258

** Inversely proportional to engine speed: 720/1,000 × 920 = 662

Thus at 1,550 horsepower output of the engine, all of the 1,550 horsepower is not lost as is the case with systems of the load-dissipating type. The bulk of the power, 930 horsepower, is made available for useful purposes by the generator.

This distribution of engine output between power lost and power recovered is typical of all power-recovery systems discussed in this paper. That is, the power lost is proportional to the degree to which the engine operates above the synchronous speed of the generator. The difference between the engine output and the power lost is, again neglecting efficiency, the power recovered.

Figure 5, which is actual test data, illustrates more exactly the character of power distribution. Curve *A* is the engine output for a typical load run. *C* is the power returned to the power system through the generator. The difference between *A* and *C* is the total power lost and is made up of the true slip loss *A-B*, the same as previously mentioned, and the transmission loss *B-C*. This latter loss is made up of two parts, the losses in the slip coupling or equivalent device and the losses in the loading generator. Tests indicate that the transmission losses remain approximately constant for all loading conditions, and in value are approximately five per cent of rated engine output.

The ultimate interest from the standpoint of power recovery is, of course, generator horsepower hours or kilowatt-hours output. This involves the time factors in the engine-testing schedules and will be referred to presently. Before doing so, however, it will be helpful to know about the power-recovery developments that have been made since the original Pratt and Whitney Aircraft installation.

Power-Recovery Developments Since the First Power-Recovery Installation

In the fall of 1940, approximately six months after the original Pratt and Whitney Aircraft installation was placed in operation, the building of the first of three aircraft-engine plants to be erected by the automobile industry, using the power-recovery system of testing, was initiated. For the purpose of this paper, these engine plants can be designated as plant 1, plant 2, and plant 3.

For plant 1 the synchronous-machine hydraulic-coupling scheme of testing was adopted.

Work on plant 2 was started in the spring of 1941, and for this plant the same scheme of testing was adopted, except that magnetic slip couplings of the eddy-current type instead of hydraulic couplings were used. A modification in the testing scheme also was incorporated with these magnetic couplings in that they are arranged for braking below synchronous speed, as explained presently.

For plant 3, the most recent of the plants, another system of testing involving wound-rotor induction generators was developed to meet the requirements of this installation. These induction test stands will be described presently also.

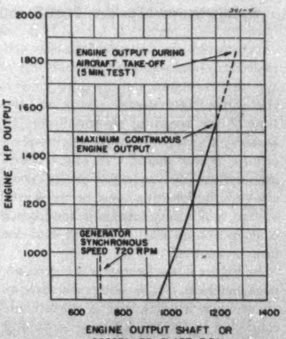


Figure 4. Curve to illustrate the fundamentals of engine horsepowers and speeds as discussed in text

At this writing there are now installed or on order for these various locations a large number of power-recovery testing equipments through which aircraft-engine production is or will be pushed. This total is a growing percentage of the total of all types of stands installed since the inception of the power-recovery system of testing. The equipments are made up of Westinghouse, Allis-Chalmers, or General Electric generators and, where used, hydraulic coupling division of American Blower Corporation hydraulic couplings or Dynamatic Corporation magnetic couplings.

Braked Magnetic-Coupling Synchronous-Machine Test Stand

The synchronous machine of Figure 2 has a relatively high synchronous speed, a speed that is in the upper reaches of the engine speeds. This high speed is selected in order to minimize the loss in the coupling which, as already stated, increases with increase in the difference between the engine and generator speeds. Also, as already stated, the engine cannot be loaded on the generator below synchronous speed, since at any engine speed below synchronous speed the synchronous machine will act as a motor. That is, the slip coupling is a speed stepping-down and not a speed stepping-up device. Therefore, the selection of a high generator speed presupposes that an engine is ready to operate at this high speed before it is required to be loaded; that is, it has been partially broken in by mechanical operation, possibly on the test stand itself, or possibly on a separate electric-motor running-in stand.

At the plant 2 installation an engine can be loaded below generator speed by loading on the magnetic coupling. To permit this, the generator and generator half of the coupling are held stationary by means of a brake when the engine is operating be-

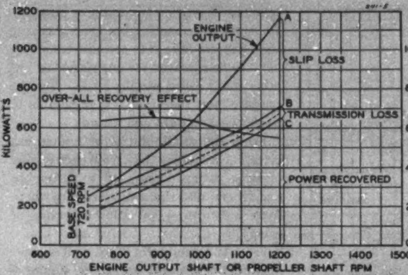
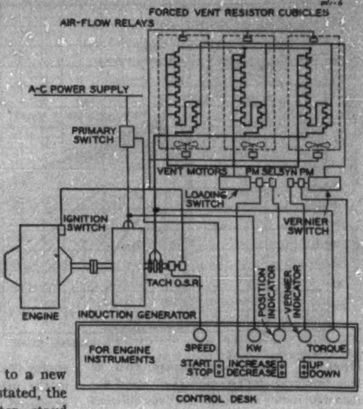


Figure 5. Test-cell performance showing typical distribution of power loss and power recovery for a power-recovery test stand

Figure 6 (right). Schematic arrangement of induction generator power-recovery stand



low synchronous speed. In other words, below synchronous speed the magnetic coupling functions in the same manner as any eddy-current loading machine, and above synchronous speed, with brake released and generator operating, it functions as a slip coupling. The magnetic coupling is water cooled and, designed for the slipping operation, has inherently sufficient capacity for the braked operation. The fact is that the loss of power under the braked condition of operation is low. This is so, because the engine outputs at low speeds are low, and the duration of the loading runs at low speeds are comparatively short.

Induction-Generator Power-Recovery System

Thus at the plant 2 installation a modification of the original power-recovery unit was made which permitted loading an engine below synchronous speed by the addition of a brake. For the plant 3 installation, still broader test-stand requirements were set up. For this installation it was desired not only to load the engine over its entire speed range but also to load the same engine in this manner when equipped with gear heads having three different ratios of speed reduction and consequently three different engine-output shaft speeds. This requirement made necessary a test stand of very considerably greater range of speed than previously used with the synchronous-machine slip-coupling stands, approximately 4:1 as compared with 1.5:1 or 2:1. Also, in this installation it was desired to eliminate the necessity of dissipating losses by means of cooling water, a requirement which assumes increased importance in the light of the increased speed range of operation and consequently increased slip loss.

These requirements pointed to a new type of stand, and, as already stated, the wound-rotor induction-generator stand was developed accordingly. Figure 6 shows schematically the fundamentals of the induction-generator stand.

As is well known, the induction generator is a variable-speed machine with inherent characteristics similar to those of the conventional wound-rotor induction motor. In its application to the test stand, the electrical design of the induction machine is such that its synchronous speed corresponds approximately to the minimum loading speed of the engine. When the generator is connected to the power system and driven by the engine at any speed above its normal synchronous speed, the generator will pump part of the engine output into the power system as recovered power and remainder into the loading resistor as lost power. The proportions of these powers are the same as for synchronous-machine slip-coupling stand.

With this wound-rotor induction-type stand the sequence of operation will be somewhat as follows: The first operation consists of a cold start and run-in test for testing mechanical operation and lubrication of the engine, the same as with the synchronous stand. This is done by starting up as an induction motor by secondary resistance control. After starting, speed is gradually increased up to approximately synchronous speed of the motor. When ready to "fire," the ignition switch is turned on and the engine throttle adjusted for approximate idling speed. When ready to load, the throttle is opened, and, as the generator speed passes through synchronism, load is applied automatically to the engine. By the proper manipulation of the loading resistor and the engine throttle, any desired values of load and speed are then established and maintained.

Figure 7 illustrates the nicety of speed and torque control available with this form of drive. In this figure the synchronous speed of the induction machine is 33 per cent of normal rated engine speed. The upper left-hand quadrant is the motoring range and the lower right-hand quadrant the generating range. The multipoint vernier control indicated is superposed on any one of the main loading points with the result that a very close speed and torque control is obtained over the entire range of operation.

Figure 8 shows the horsepower-loss and horsepower-recovery division of loading corresponding to the three gear ratios of Figure 7. With an engine passing through the usual test time and loading cycles the division of horsepower hours or kilowatt-hours recovered and lost would be somewhat as follows:

Engine Gear Ratio	Per Cent High (A)	 Medium (B)	 Low (C)
Kilowatt-hours recovered	58	47	37
Kilowatt-hours lost	42	53	63

Disposition of Power Loss With Induction-Generator Stand

One of the features of the induction-generator type of stand is the ease with which a high-loss loading element, the loading resistor, can be supplied, and the ease with which high-loss power can be dissipated. It will be noted from Figures 7 and 8 that, because of the wide range of operation, the amount of power to be dissipated is relatively considerable. For

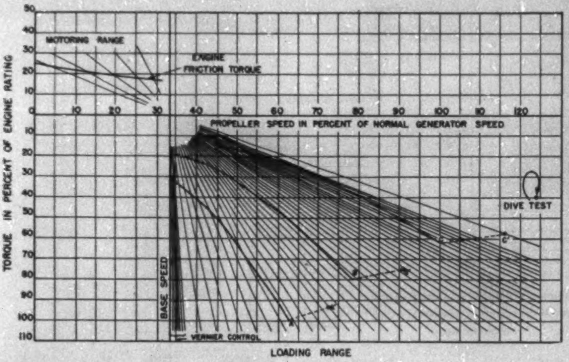


Figure 7. Torque-speed characteristics of induction-generator power-recovery stand designed for testing an engine with three different gear heads

The solid lines *A*, *B*, and *C* are normal loading ranges for high-, medium-, and low-gear ratios respectively. The points *A'*, *B'*, and *C'* are take-off loadings. The dive test point represents the highest speed to which the stand is required to operate

the more common present large engines equipped with gear ratios as previously discussed, the loading resistor would have a maximum continuous rating of approximately 1,200 kw. With the use of a resistor of the finned Calrod construction it is a relatively simple matter to blow this energy into the air. With such a construction no consideration need be given to the cost and availability of a suitable water-cooling or water-energy dissipating system.

Aircraft-Engine Testing With Propellers

The aircraft-engine industry has made testing developments of its own, so that a variety of testing schemes from which to choose are available to the designer of an engine-testing procedure and layout. But in the manufacture of the large engines produced today, the selection of a testing scheme probably would resolve into a choice between testing with propellers similar to aircraft propellers and testing with power recovery.

Figures 10 and 11 illustrate typical physical arrangements for these two schemes of testing. It will be noted that the structures shown differ fundamentally in that with propeller testing the structure must be designed to accommodate the air stream set up by the engine loading propeller, whereas with the power-recovery system this is not necessary, because the air stream is not present.

For the rest of this paper, the statements made may be scrutinized with the view that, if power-recovery testing were not used, propeller testing would be.

Recovery of Power

If one knows the engine sizes and rates of production and the horsepower-speed-torque characteristics of the testing cycles it is a simple matter to calculate the expected recovery of energy. These calculations will predict minimum or pessimistic expected results, however, because they will not take into account tests that have to be repeated for one reason or another, tests called penalty runs. For the purpose here, therefore, it is preferable to use actual test data and records.

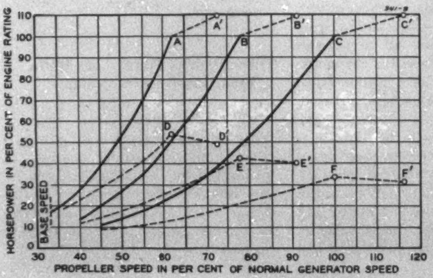


Figure 8. Curves showing the division between horsepower lost and horsepower recovered for the conditions of operation shown in Figure 7

For example, the area under curve *A* is engine output, and that under corresponding curve *D* is power recovered. The difference between the two is the power lost

From the standpoint of individual engines, records show that, from each engine of large rating coming off an assembly line, there may be recovered anywhere from 3,000 to 6,000 kilowatt-hours of energy.

From the standpoint of over-all engine-manufacturing-plant operation, Figure 9 is indicative of what may be expected. This figure is the plotting of the over-all performance of one of the new engine plants for a period of 72 consecutive hours. The plotting is actual normal plant performance made from instrument readings taken at the plant for the period indicated. The top curve shows the plant power consumption. The bottom curve shows the power purchased from the connected public utility. The difference between the two is the power recovered from the power-recovery test stands. It will be noted that this recovered power is more than half that required to operate the plant.

Table I. Record of Percentage of Total Engine-Plant Power Consumption Supplied by Power-Recovery Testing for a Large Engine Plant During a Period of Six Months

Month	Per Cent Recovery
1	54.6
2	57.5
3	53.0
4	53.7
5	59.6
6	57.3

Table I is similar over-all plant performance for a period of six months and shows again power recovery exceeding half of the total power required to operate the plant during that period.

Reverting to Figure 9, it will be seen that power-recovery testing also reduces power demand and consequently purchased power-demand charges. Without power-recovery testing, demand charges would be based on the top instead of the bottom curve. Obviously, also, the capacity of interconnecting apparatus such as transmission lines and transformers would have to be based on the top instead of the bottom curve.

The power-recovery system saves energy, reduces demand charges and interconnecting apparatus capacity, and does all this to an important degree. A propeller test stand will not do any of these

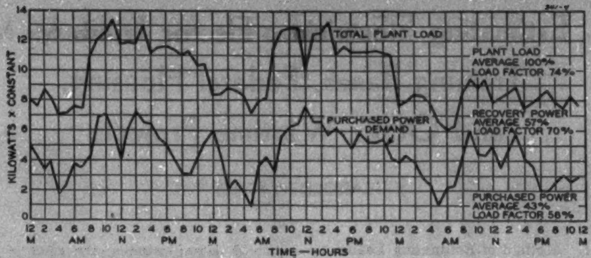


Figure 9. This 72-hour total plant power chart shows plant load, power recovered from the test stands, and purchased power

The area between the two curves represents the power supplied to the plant by the power-recovery test stands. In this record, 57 per cent of the total plant load is secured by power recovery and 43 per cent is purchased from the connected power company

things. These are important accomplishments that are of direct interest to the engine builder.

Measurement of Engine Loading

Besides the advantages enumerated at the outset of this paper, the power-recovery system of testing carries the important advantage that it is still open to expansion and improvement. As the paper shows, this system of testing is very new. Its full possibilities have not yet had time to develop.

One such possibility is the accurate measurement of engine loading by electric instrument measuring techniques. When the mechanical energy output of the engine has been converted into electric energy, the basis for such measurement is present. This is an important asset of the power-recovery system of testing that is open to the engine builder. It is an involved subject, however, and entails departure from aircraft-engine testing procedure of long standing.

Cost of Power-Recovery Testing Equipment

When all pertinent considerations are taken into account, the cost picture surrounding power-recovery testing is particularly favorable. The question from a cost standpoint is: how much more does a power-recovery system cost than a system in which power is not recovered, for example, the propeller system? In other words, to justify any greater cost for power-recovery testing, it is necessary only to justify the difference between the cost of power-recovery testing and such another system as may be contemplated.

The cost of the test-stand equipment required for a large engine power-recovery stand will likely be between $20,000 and $30,000. This equipment cost is largely offset, however, by the simpler building structure permissible with the power-recovery system. Those that are in a position to know building cost differences and such other cost differences that enter into over-all cost computations have submitted cost differences ranging from nothing to $15,000 per test stand for a power-recovery testing system of the capacity being discussed in this paper.

Assume that the difference standing against power-recovery testing is $15,000. Assume likewise that 1.5 engines per day per stand per 300-day year are tested at a saving in kilowatt-hours per engine of 4,000. Then with power at $0.004 per kilowatt-hour the annual saving is $7,200. The corresponding amortization rate is slightly over two years. In other words, the cost difference is eliminated in slightly over two years, and thereafter the savings continue indefinitely. No nonrecovery testing system can present such an outlook from an investment standpoint.

Furthermore, the preceding cost picture is not the best that can be presented. It does not take into account reduction in power-demand charges. Likewise, it does not take into account the possibility of testing engines more quickly by power-recovery and consequently the possibility of installing fewer stands for a given plant engine output.

Use of Critical Materials

In these times, the use of the critical materials necessary with power-recovery testing is, of course, a consideration of first line importance. New installations of power-recovery testing could not be advocated without a favorable critical-materials situation.

Figure 11. Schematic section of a power-recovery test stand of the synchronous-machine slip-coupling type

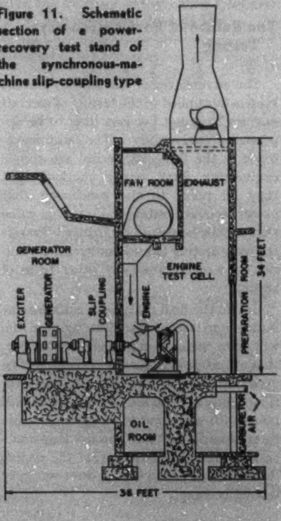
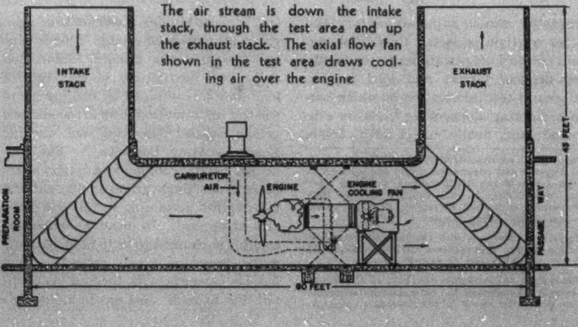


Figure 10. Schematic section of a modern propeller test stand

The air stream is down the intake stack, through the test area and up the exhaust stack. The axial flow fan shown in the test area draws cooling air over the engine



The approach to the critical-materials problem is in the fact that the use of critical materials is necessary to generate and supply power to any aviation engine plant, regardless of what means of securing power may be used. If the power-recovery system of testing can supply power with less use of such materials, its adoption should be justifiable, in fact, advocated.

Table II shows a comparison between the critical materials involved in obtaining a kilowatt of load capacity from power-recovery equipment and from a medium-size steam-generating plant:

Table II

Power Recovery	
Critical materials pounds per kilowatt	
Copper	10
Iron and steel	90
Steam Plant	
Critical materials pounds per kilovolt-ampere	
Copper	50 to 70
Iron and steel	250 to 400

The data show that power recovery is very much favored. Unless the circumstances surrounding a specific engine plant are very unusual, the conclusion is evident that it is in the best interest of the war effort to advocate the installation of generating capacity right at the engine plant by power recovery rather than to advocate the installation of an equivalent capacity in a distant steam station.

The Future of Power-Recovery Testing

The advantages of the power-recovery system as applied to the testing of aircraft engines have not yet had time to be appreciated universally. The statements made in the foregoing recital are simple engineering facts, easily checked, and they all point to the conclusion that power-recovery testing has intrinsic value that cannot be passed by lightly. Power-recovery testing has advantages that cannot be secured from any other form of testing.

But, in the final analysis, it is the engine builder and not the electrical engineer who will point the way and decide to what further extent power-recovery testing will be used. It may be quite possible that with the passing of time the engine builder will point with more and more favor, because he is already pointing toward larger aircraft engines—and the larger the engine the more advantageous power-recovery testing becomes.

Estimating Electric Loads for Military-Training Bases, Federal Housing, and Other Wartime Projects

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I. Method of Estimating Loads

At present a great amount of engineering talent is being utilized in the electrical planning and designing of various types of military-training bases, federal-housing projects, and other wartime projects. The Bonneville Power Administration has been requested to supply electric power to many types of war projects and of course was confronted with the problem of estimating the power requirements of each.

This paper is intended to show the methods used in estimating load requirements for various types of military and other wartime projects; to present certain conclusions pertaining to the resulting demand factors and load factors; to present test information on which barracks lighting and cooking loads are estimated; and, finally, to set forth certain derived data illustrating the effect of electric cooking on the total load.

Table I. Summary of Derived Information From Load Estimates of Military Training Bases

	⊖ Army Training Base*	Section II Navy Training Base**	Section III Air Training Base†	Section IV Lighter-Than-Air Station‡
Number of men	35,000	30,000	3,400	1,000
Total connected kilowatts	26,829	15,929	2,598	4,939
Average monthly maximum kilowatts	6,394	4,890	1,180	2,065
Annual maximum kilowatts	7,726	5,800	1,340	2,425
Demand factors (per cent)⊗				
Barracks and mess-hall lighting	41.5	40	50.4	56
Total load	23.8	31	45.4	42
Load factors (per cent)⊗				
Barracks and mess-hall lighting	54.4	56	54.6	56
Total load	74.8	73	69.4	67
Diversity between major load groups (per cent)⊗	1.48	1.27	1.14	1.08

* Complete electric cooking on one-sitting basis. 12,885-kw connected cooking load.

** Electric cooking on three-sitting basis. 4,481-kw connected electric-cooking load.

† Electric cooking on three-sitting basis. Large loads in shop motors and hangar lights.

‡ Approximately one half of total connected load in shop power and helium-repurification-system motors. Large load in hangar lighting.

⊗ Based on a typical average day.

⊖ See reference 2.

Detailed load estimates of various types of military bases and other wartime projects in the following sections of the paper, are presented as examples from which certain conclusions are drawn.

The general method used is to portray for each hour or half-hour period of the day the expected fluctuation for the various classes of load. The detailed connected load is first segregated into groups which have similar characteristics as to the time and nature of their uses. The barracks lighting, mess-hall lighting, and single-phase kitchen power are first segregated into one load group. This is done in order that the demand factors and daily load fluctuations of this type of load may be estimated by making use of the test information shown in the author's article entitled "Lighting and Cooking Loads in Army Barracks."[1] The connected electric cooking load is next segregated into a single group so that the test information pertaining to this load may also be applied. In instances where the cooking is planned so as to prepare meals to be served on a two- or three-sitting basis or cafeteria style, the load peaks applicable to each meal are broadened to

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The author acknowledges the help of Claude R. Lester, acting chief of the rates and statistics section, Bonneville Power Administration.

Table II. Electric-Cooking-Load Characteristics Based on Detailed Load Estimates of Military-Training Bases⊕

	● Army Training Base	Section II Navy Training Base	Section III Air Training Base	Section IV Lighter-Than-Air Station
1. Connected cooking load	12,885	4,481	487	223
Per cent of total	48	28	19	4.5
2. Peak responsibility*				
Kilowatts⊖	2,576	1,150	126	50
Per cent	40.3	23.5	10.7	2.4
Watts per man	73.5**	38.3†	37.1‡	50.0⊗
3. Incremental cooking load				
Kilowatts⊖	1,152	1,084	88	50
Watts per man	32.9**	36.1†	25.8‡	50.0⊗
4. Ratio of kilowatt peak responsibility / connected cooking kilowatts (per cent)	20	26	26	22

* Demand of electric cooking load at time of the total peak.

** Based on 35,000 men.

† Based on 30,000 men.

‡ Based on 3,400 men.

⊗ Based on 1,000 men. These ratios high because of bakery and recreation building loads.

⊖ Includes estimated distribution and substation losses.

⊕ Based on a typical average day.

● See reference 2.

an extent consistent with their probable variation from the test curves.

The office, shop, and other daytime loads are next segregated into groups having similar characteristics, and the hourly fluctuations during the day are estimated for each group.

Night lighting of a steady nature such as street, protective, and flood lighting is considered as a group in which the full connected load will be used during all hours of darkness.

The motive-power load is likewise segregated into groups of similar nature, and the load fluctuation of each group is estimated independently. Shop power, major refrigeration power, special power such as helium-repurification-compressor power (section IV) are samples of groups frequently found. Water pumping is estimated on the basis of the design plans. This type of load ordinarily can be operated on an off-peak basis.

Tables I and II present the general results of detailed load estimates for the four types of military bases presented in sections II, III, and IV and for the large army cantonment, details of which appear in a second article by the author, entitled "Estimated Load for 35,000-Man Army Cantonment."[2]

Table I presents a comparison of important derived information on demands, demand factors, and load factors.

Table III

Total connected load	1,897 kw
Average monthly maximum demand	665 kw
Annual maximum demand	860 kw
Average monthly load factor	63 per cent
Annual load factor	49 per cent
Demand factor of total load	35 per cent
Diversity between major load groups	1.19

The first is the large army training base in which full electric-equipped kitchens are planned with meals to be served on a one-sitting basis.

The second is a large naval training station in which the cooking is to be done electrically, with meals to be served on a three-sitting basis. Considerable steam-heated cooking equipment is used in this station.

Third is an air training base in which the hangars and miscellaneous power form the major part of the load.

The fourth is a naval lighter-than-air station in which the hangar lighting, shop power, and motive power for the helium repurification compressors form the principal loads. Motive power in this base is approximately one half of the total connected load.

Table IV. Summary of Cumulative Demand Factors for Typical Average Day

Groups	Types of Load	Connected Load (Kilowatts)	Estimated Maximum Kilowatts at Substation*	Cumulative Demand Factors (Per Cent)
1	Lighting and small power in living quarters and mess halls	4,064	1,612	40
1–2	Includes addition of general lighting and incidental heat and power	9,779	3,421	35
1–3	Includes addition of street lighting	9,979	3,504	35
1–4	Includes addition of pumping (off peak)	10,481	3,504	33
1–5	Includes addition of motive power	11,448	3,806	33
1–6	Includes addition of electric cooking and baking	15,929	4,890	31

* Includes estimated ten per cent distribution and substation losses.

The summary of the demand factors shows that the barracks lighting is about 40 per cent for the large bases. The smaller bases have higher demand factors of 50.4 per cent to 56 per cent. This is due to greater diversity being realized in the larger bases. The demand factors for the total load are reduced to 23.8 per cent for the large army base having electric cooking for meals on a one-sitting basis and 31 per cent for the large navy base with electric cooking for meals on a three-sitting basis. Demand factors for the total load of the air training base and the lighter-than-air station are 45.4 per cent and 42 per cent, respectively. These are types of bases in which the hangar and power loads predominate.

The load factors for the barracks and mess-hall lighting are fairly constant at 54.4 per cent to 56 per cent. The load factors for the total loads range from 67 per cent to 74.8 per cent, the higher load factors being expected for the large bases. The lighter-than-air station has the lowest estimated load factor, because the helium repurification process is assumed to be in use for only six days per month.

The estimated diversity between load groups is 1.48 for the large army base having electric cooking for meals served

Table V. Summary of Cumulative Load Factors for Typical Average Day

Groups	Types of Load	Energy Use (Kilowatt-Hours)*	Maximum Demand (Kilowatts)	Daily Load Factor (Per Cent) ⊕
1	Lighting and small power in living quarters and mess halls	21,790	1,612	56
1–2	Includes addition of general lighting and incidental heat and power	50,510	3,421	62
1–3	Includes addition of street lighting	53,150	3,504	63
1–4	Includes addition of pumping (off peak)	59,090	3,504	70
1–5	Includes addition of motive power	64,680	3,806	71
1–6	Includes addition of electric cooking and baking	85,720	4,890	73

* Estimated requirements at the substation, including ten per cent distribution and substation losses.

Table VI. Peak Responsibility of Various Groups of Loads for Typical Average Day

Group	Type of Load	Kilowatts*	Per Cent
1	Lighting and small power in living quarters and mess halls	1,540	31.5
2	General lighting and incidental heat and power	1,881	38.5
3	Street lighting	0	0
4	Pumping (off peak)	0	0
5	Motive power	319	6.5
6	Electric cooking and baking	1,150	23.5
	Total of peaks	4,890	100.0
	Group Diversity = 6,188/4,890** = 1.27		

* Demands at time of estimated maximum demand for total load at the substation. Includes estimated ten per cent distribution and substation losses.

** Sum of peaks of individual groups (see Table VII).

on a one-sitting basis; 1.27 for the large naval training base with electrically cooked meals served on a three-sitting basis; 1.14 for the air training base in which the shops and hangars are operated continuously day and night; and 1.08 estimated diversity between load groups in the lighter-than-air base in which the hangars, shops, and helium repurification system are all operated on a 24-hour per day basis.

In Table II certain derived information on the electric cooking loads in each of the four bases is presented.

It is to be noted particularly that the connected cooking load for the large army base is nearly three times that of the navy base of approximately the same number of men in size. This is chiefly due to the effect of meal planning on one- and three-sitting bases, respectively.

In each of the load estimates shown, the ratio of the cooking demand at the time of the total peak for the training base (peak responsibility) is always less than the ratio of the connected cooking load to the total connected load. As an example, the connected cooking load of the 35,000-man army base is 12,885 kw or 48 per cent of the total connected load, whereas the cooking demand at the time of the total peak is 2,576 kw or only 40.3 per cent of the peak load.

The peak responsibility of the cooking load in watts per man is 73.5 for the army base, while it is only 38.3 watts per man for the similar-sized navy base. The principal reason for this difference is the planning of the meals on a one-sitting basis in the army base and a three-sitting basis in the navy base.

The incremental cooking load per man varies from 25.8 watts to 36.1 watts. The figure of 50 watts per man for the lighter-than-air station is due to a majority of the total cooking load being com-

Table VII. 30,000-Man Naval Training Station—Estimated Loads for Typical Average Day

Hour	Annual Maximum Day	Lighting and Small Power in Living Quarters and Mess Halls	Annual Maximum	General Lighting and Incidental Heat and Power	Annual Maximum	Street Lighting	Total Lighting and Miscellaneous	Pumping (Off Peak)	Annual Maximum	Motive Power	Total Lighting and Power	Annual Maximum	Electric Cooking and Baking	Annual Maximum	Total Load: At Point of Use	Total Load: Annual Maximum	Total Load: At Substation†
Connected Load (Kilowatts)		4,064		5,715‡		200	9,979	502		967‡	11,448		4,481‡		15,929		
12–1 a. m.		245		320		200	765	300		85	1,150		305		1,455		1,600
1–2		245		320		200	765	300		85	1,150		305		1,455		1,600
2–3		245		320		200	765	300		85	1,150		305		1,455		1,600
3–4		245		320		200	765	300		85	1,150		602		1,770		1,945
A.M.–5		325		320		200	845	300		85	1,230		1,385		2,615		2,875
5–6		1,000		320		200	1,520	300		275	2,095		1,325		3,420		3,760
6–7 a.m.		1,260		410		100	1,770	300		275	2,345		1,295		3,640		4,005
7–8		1,180		750			1,930	300		275	2,505		820		3,325		3,660
8–9		1,020		1,415			2,435	300		275	3,010		950		3,960		4,355
9–10		895		1,865			2,760	300		265	3,325		1,120		4,445		4,890
10–11		815		1,865			2,680	300		245	3,225		1,100		4,325		4,760
11–12 noon		815		1,865			2,680	300		245	3,225		1,115		4,340		4,775
12–1 p.m.		855		950			1,805	300		305	2,410		610		3,020		3,320
1–2		815		1,865			2,680	300		285	3,265		710		3,975		4,375
2–3		815		1,915			2,730	300		245	3,275		890		4,165		4,580
3–4	860	1,000	2,505	1,965			2,965		315	245	3,210	1,520	1,070	5,200	4,280	5,720	4,710
4–5	1,350	1,400	2,115	1,710			3,110		325	290	3,400	1,480	1,045	5,270	4,445	5,800	4,890
5–6	1,595	1,465 *	1,715	975	100		2,440		345	310	2,750	1,510	1,065	5,265	3,815	5,790	4,195
6–7 p.m.	1,595**	1,465	2,040	1,300	200	100	2,865		325	295	3,160	1,110	760	5,270	3,920	5,800	4,310
7–8	1,390	1,220	2,395	1,765	200	200	3,185		305	275	3,460	960	740	5,250	4,200	5,775	4,620
8–9	1,270	1,140	2,380	1,665	200	200	3,005		240	210	3,215	605	485	4,695	3,700	5,105	4,070
9–10		610		1,120		200	1,930	300		160	2,390		420		2,810		3,090
10–11		490		470		200	1,160	300		95	1,555		345		1,900		2,090
11–12 m.		245		320		200	765	300		85	1,150		345		1,495		1,645
Total		19,810		26,110		2,400	48,320	5,400		5,080	58,800		19,130		77,930		85,720
Daily or monthly load factor (per cent)		56		55		50	63	75		68	71		58		73		73
Annual load factor (per cent)		52		43		50	51	75		61	57		41		62		62

* 4,064×51.5 per cent estimated demand factor×70 per cent estimated diversity = 1,465 kw maximum-demand, typical average day.

** 4,064×56 per cent estimated demand factor×70 per cent estimated diversity = 1,595 kw maximum-demand, annual maximum day.

† Includes estimated ten per cent distribution and substation losses.

Estimated by separate details of various load elements in each group.

posed of night baking and ships service building loads, which probably will be used extensively at the time of the maximum demand for the base.

The most important conclusion relative to the electric cooking loads is the ratio of the peak responsibility to the connected cooking load. This is shown to vary from 20 per cent to 26 per cent in the four load estimates presented. The ratio of 20 per cent for the army base reflects cooking for meals on a one-sitting basis. This ratio is increased to 26 per cent for the training bases having meals served in three sittings. This difference is reflected by the longer duration of the peak periods and the reduced diversity of cooking on the three-sitting basis. Thus the general conclusion is that only one fifth to one fourth of the total connected cooking load in these types of training bases may be reflected as actual demands at the time of the maximum demand for the entire training base.

Estimated loads are shown in section V of the various types of living units contained in a large war housing project providing housing for 8,700 families and 4,000 single men. These types consist of apartments, dormitories, temporary demountable houses, and permanent houses. The over-all connected load is 87,764 kw, including allowances for pumping and commercial uses. The estimated maximum demand is 10,725 kw, resulting in an annual demand factor of 12.2 per cent for the entire project.

Table VIII. Summary of Cumulative Demand Factors for Typical Average Day

Service	Connected Load (Kilowatts)	Estimated Maximum Demand at Substation*	Demand Factor (Per Cent)
1. Lighting in barracks, officers' quarters, galleys, and so forth	568	286	50.4
2. Includes addition of lighting and power in hospital	782	390	49.9
3. Includes addition of lighting in administration building, offices, shops, and so forth	1,196	583	48.7
4. Includes addition of lighting in recreation building	1,448	676	46.7
5. Includes addition of lighting in hangars	1,638	885	54.0
6. Includes addition of lighting in streets, runways, flood lights, and so forth	1,688	940	55.7
7. Includes addition of motive power in shops, refrigerators, water pumps, and so forth	2,111	1,083	51.3
8. Includes addition of electric cooking in galleys	2,485	1,138	45.8
9. Includes addition of electric cooking in hospital and recreation building	2,598	1,180	45.4

Diversity = 1,350/1,180 = 1.14

* Includes estimated ten per cent distribution losses.

Table IX. Summary of Cumulative Load Factors Based on Typical Average Day

	Energy* (Kilowatt-Hours)	Demand* (Kilowatts)	Daily Load Factor (Per Cent)
1. Lighting in barracks, officers' quarters, galleys, and so forth	3,750	286	54.6
2. Includes addition of lighting and power in hospital	5,030	390	53.7
3. Includes addition of lighting in administration building, offices, shops, and so forth	8,140	583	58.2
4. Includes addition of lighting in recreation building	8,820	676	54.4
5. Includes addition of lighting in hangars	12,790	885	60.2
6. Includes addition of lighting in streets, runways, flood lights, and so forth	13,560	940	60.1
7. Includes addition of motive power in shops, refrigerators, water pumps, and so forth	17,000	1,083	65.4
8. Includes addition of electric cooking in galleys	18,740	1,138	68.6
9. Includes addition of electric cooking in hospital and recreation building	19,660	1,180	69.4

* Includes estimated ten per cent distribution losses.

Table X. Peak Responsibility of Various Groups of Services for Typical Average Day

	Kilowatts*	Per Cent
1. Lighting in barracks, officers' quarters, galleys, and so forth	275	23.3
2. Lighting and power in hospital	115	9.7
3. Lighting in administration building, offices, shops, and so forth	192	16.3
4. Lighting in recreation building	66	5.6
5. Lighting in hangars	208	17.6
6. Lighting of streets, runways, flood lights, and so forth	55	4.7
7. Motive power for shops, refrigeration, pumps, and so forth	143	12.1
8. Electric cooking in galleys	82	7.0
9. Electric cooking in hospital and recreation building	44	3.7
Total	1,180	100.0

* Based on estimated maximum demands at substation for maximum demand period of 6 to 7 p.m

A short discussion follows on the estimated loads, demand factors, and load factors for a typical army hospital and an army reconsignment depot. Similarity may be noted as to those factors as compared with the other types of bases discussed.

Army Hospital

Plans for a typical army hospital designed for 1,000 beds on a general 1,500-bed plan were analyzed. Living quarters were planned for an operating personnel of 668 men. Complete electric cooking was provided for in the nurses', officers', patients', and medical-detachment messes and in wards, post exchange, and recreation buildings.

The results of this detailed load estimate, which was performed in the same general manner used in other studies presented in this paper, are shown in Table III.

In this hospital the connected electric cooking equipment was 1,048 kw, which accounts for 55 per cent of the total connected load of the hospital. The peak responsibility was estimated to be 319 kw, or 48 per cent of the total maximum demand for a typical day. The ratio of the kilowatts of peak responsibility to the connected cooking load is estimated to be 30 per cent.

Reconsignment Depot

This is an army depot which is built for the purpose of receiving shipments of all types of supplies by rail and then regrouping and reshipping the supplies to various destinations. The major part of the labor will be done by civilians, most of whom will live outside the depot.

The total connected load was found to be 1,293 kw, the major portion being lighting and space heating in warehouses. A limited amount of housing for civilians is provided.

Electric cooking equipment is provided in a cafeteria, an officers' mess, and a civilian mess hall.

The estimated average monthly maximum demand was 565 kw, which results in a demand factor of 44 per cent. The annual maximum demand is estimated to be 815 kw.

Thus the method used in estimating the types of loads described in this paper

Table XI. Naval Air Training Base—Estimated Load for

Group Number		1	2		3		4	5		6		7
Type of Load	Annual Maximum	Lighting in Living Quarters	Lighting and Power in Hospital	Annual Maximum	Lighting in Offices, Shops, and so forth	Annual Maximum	Lighting in Recreation Building	Lighting in Hangars	Annual Maximum	Night Lighting (Streets, Flood, and so forth)	Total Lighting	Motive Power
Hour / Connected Load (Kilowatts)		568	214		414⊖		252†	190‡		50	1,688	423⊗
12–1 a.m.		45	20**		80			190		50	385	130
1–2		45	20		80			190		50	385	130
2–3		45	20		80			190		50	385	130
3–4		45	20		80			190		50	385	130
4–5		60	20		80			190		50	400	130
5–6 a.m.		150	20		80			190		50	490	130
6–7		230	20		85		20	150		50	555	130
7–8		210	50**		155		20	100		25	560	130
8–9		175	50		135		20	100			480	130
9–10		160	50		120		20	100			450	130
10–11		145	50		120		20	100			435	130
11–12 noon		145	50		120		20	100			435	130
12–1 p.m.		150	50		120		20	100			440	130
1–2		145	50		120		20	100			435	130
2–3		145	50		120		20	100			435	130
3–4		150	50		135		20	100			455	130
4–5		200	80		155		20	130			585	130
5–6 p.m.	285	260*	90	200	180	125	30	150	50	25	735	130
6–7	270	250	105**	200	175	125	60	190	50	50	830	130
7–8	240	220	105	200	165	125	125	190	50	50	855	130
8–9	220	200	105	200	150	125	125	190	50	50	820	130
9–10		110	50		130		60	190		50	590	130
10–11		80	20		80			190		50	420	130
11–12 midnight		45	20		80			190		50	385	130
Total		3,410	1,165		2,825		620	3,610		700	12,330	3,120
Daily load factor (per cent)		54.6	46.2		65.4		20.7	79.2		58.3	60.1	100

* 568 kw connected × 51.5 per cent demand factor from test information × 90 per cent estimated diversity for 36 buildings = 260 kw estimated maximum demand.

** Estimated 50 per cent of lighting on at maximum; 10 per cent at minimum; 25 per cent in day.

† 252 kw connected × 50 per cent estimate night = 125 kw; 10 per cent estimate day, or 20 kw.

‡ 190 kw connected × 100 per cent night, 50 per cent day.

⊗ 423 kw connected × 30 per cent all day.

⊖ Cooking on three-sitting basis, 374 kw con-

begins with the grouping of various types of connected loads. These groups are compiled on the basis of their application to test data available and their similarity and nature of probable time of daily use. Then, by estimating the fluctuations of each group over an average day, the maximum demands of the total of the groups may be approximated.

Sections II to V present details of the load estimates discussed.

II. Load Estimate for 30,000-Man Naval Training Station

The details of the estimated load for a 30,000-man naval training station are presented in Tables IV through VII.

This station has a planned connected load of 15,929 kw. The estimated maximum demand for a typical average day is 4,890 kw, and for an annual maximum day, 5,800 kw. The plans for this base called for electric cooking in the mess halls, subsistence buildings, ships service buildings, dispensary, and bakery. Some steam tables and cookers are planned in addition to the electric cooking equipment.

The demand factors range from 40 per cent lighting and small power in the living quarters and mess halls to 31 per cent for the total load (Table IV). The average daily load factors range from 56 per cent for lighting and small power in the living quarters and mess halls, to 73 per cent for the total load (Table V).

Table VII presents the basic details of the estimated loads of the various types found in this training station.

The maximum demand for the lighting and small power in living quarters and mess halls was based on test data previously mentioned, and is shown by footnotes on Table VII. Because a large number of barracks was being planned (160), the diversity would reduce the group demand to an estimated 70 per cent of the test demand factors for both the average and annual maximum days. The load fluctuation during the day was prorated on the basis of the aforementioned test curves.

The general lighting and incidental heating and power totals 5,715 kw of connected load. This load was segregated into smaller groups of similar characteristics, and the daily load fluctuations of each group were estimated independently. Included in this general group are various segregations such as lighting in offices. This load was estimated on the basis of a 60 per cent demand factor during the day, with small amounts being used at night. Lighting, heating, and small power normally used in the daytime, were estimated on the basis of 50 per cent of the connected load being used during the day, and approximately 10 per cent at night. The demand for the ships service and recreation buildings was estimated at 75 per cent of the connected load in the evening with small amounts used during the day. The evening and night lighting was estimated to be in use to the extent of 40 per cent of the connected load during the evening and late afternoon. This lighting is principally in the drill halls, auditorium, and bakery.

The street lighting was assumed to be used fully during the night hours.

The pumping power having a connected load of 502 kw was estimated to have a continuous demand of 300 kw and will be operated on an off-peak basis.

The motive power totalling 967 kw connected was likewise segregated into smaller groups of similar characteristics, and the fluctuations during the day of each group were estimated separately. Included in this group are loads for the incinerator and sewage plant, of which the sewage power is the major load. These were estimated at the full capacity at morning rising hours, 80 per cent after meal hours, and about 50 per cent at other hours. A group containing the day motive power was estimated at 25 per

Typical Average Day

Total Lighting and Power	Annual Maximum	8 Electric Cooking (Galleys)	9 Electric Cooking (Hospital and Recreation Building)	Annual Maximum	Total Load At Buildings	 At Substation	Annual Maximum
2,111		374⊖	113⊕		2,598	●	
515		15	10		540	590	
515		15	10		540	590	
515		15	30		560	620	
515		50	50		615	680	
530		130	60		720	790	
620		125	60		805	890	
685		120	40		845	930	
690		75	30		795	870	
610		85	45		740	810	
580		105	50		735	810	
565		100	45		710	780	
565		95	25		685	750	
570		50	20		640	700	
565		65	25		655	720	
565		85	45		695	760	
585		100	40		725	800	
715		95	35		845	930	
865	130	90	45	1,205	1,000	1,100	1,330
960	110	75	40	1,220	1,075	1,180	1,340
985	60	40	40	1,140	1,065	1,170	1,250
950	20	15	40	1,080	1,005	1,110	1,190
720		15	40		775	850	
550		15	20		585	640	
515		15	10		540	590	
15,450		1,590	855		17,895	19,660	
65.4		51.0	59.4		69.4	69.4	

nected×35 per cent demand factor = 130 kw maximum demand.

⊕ 50 per cent×113 kw connected = 60 kw maximum same fluctuation as one mess barracks; evening loads held high because of cooking in recreation building of 57 kw connected.

● Includes estimated 10 per cent distribution losses.

◐ Based on separate detail of smaller load groups.

cent demand factor during work hours, with small amounts being used at night. This load is principally laundry and shop power. The evening motive power, consisting chiefly of ventilating and heater fan motors in recreation buildings, mess halls, and ships service buildings, was estimated to be used at a 50 per cent demand factor during evening hours and during the breakfast period. The refrigeration motors for the cold-storage plant having a connected load of 153 kw were assumed to have a demand of 50 per cent of the connected load during the day hours, decreasing to 10 per cent during the cooler night hours.

The connected load for the electric cooking and baking was also segregated into smaller groups, and the daily load fluctuation of each group was analyzed separately. The cooking in mess halls and subsistence buildings is planned so as to prepare meals to be served on a three-sitting basis. The demand factors found by the test data of 35 per cent for the typical day and 50 per cent for the maximum day are used to determine the maximum demand. No diversity factors are used in this instance, as a total of only nine mess halls and subsistence buildings are planned. The fluctuation in cooking load during the day is based on the test curves in general, except that, because of the meals being served in three sittings, the cooking periods are assumed to be of longer duration. Included in this group is the bakery which is planned to be operated at night. The maximum demand is assumed to be 95 per cent of the connected load of 231 kw during the "warming-up" period at the start of the bakers' shift. During the rest of the night baking period, the demand is estimated to be 75 per cent of the connected load.

III. Load Estimate for Naval Air-Training Base

This base has a connected load of 2,598 kw. The estimated maximum demand for a typical day is 1,180 kw, and for an annual maximum-demand day, 1,340 kw.

The base is for the purpose of training navy pilots and is planned for an ultimate total of 3,400 officers, instructors, and trainees. There is planned a 952-man electric-equipped mess hall for the enlisted men and a separate mess hall for the officers. Three hangars, an assembly and repair shop, and a recreation building are other major loads.

The demand factors range from 50.4 per cent for the lighting in the living quarters and mess halls to 45.4 per cent for the total load. The average daily load factors range from 54.6 per cent for lighting in the living quarters and mess halls, up to 69.4 per cent for the total load.

Table XI contains the basic details for the estimated fluctuations in each of the various loads during a typical day. The same general procedure was used as in the preceding section II.

The electric cooking is planned for meal service on a three-sitting basis, as the 952-man mess hall must feed about 3,000 men. The demand factors used are based on the test information previously mentioned. The load fluctuation during the day is based on the test curves in general, except that because of longer meal periods the peaks are estimated to be of longer duration than shown by the test data.

The maximum demands and daily fluctuation of the other types of loads are explained by means of footnotes on Table XI.

Tables VIII through XI present details of the load analysis.

IV. Load Estimate for a Naval Lighter-Than-Air Station

This base has a designed connected load of 4,939 kw. The estimated maximum demand for a typical day is 2,065 kw, and for an annual maximum day, 2,425 kw.

The base is to be used to house, repair,

Table XII. Summary of Cumulative Demand Factors for Typical Average Day

	Connected Load (Kilowatts)	Estimated Maximum Kilowatts Demand at Substation*	Cumulative Demand Factors (Per Cent)
1. Lighting in living quarters	508	286	56
2. Includes addition of miscellaneous lighting in offices, hospital, recreation building, storehouses, and so forth	989	424	43
3. Includes addition of hangar office and shop lighting and heating	2,724	1,342	49
4. Includes addition of street and flood lighting	2,761	1,380	50
5. Includes addition of shop and miscellaneous power	4,091	1,766	43
6. Includes addition of electric cooking and baking	4,314	1,815	42
7. Includes addition of helium compressors, exhausters, and air-compressor motive power	4,939	2,065	42
Diversity = 2,228/2,065 = 1.08			

* Includes estimated ten per cent distribution and substance losses.

Table XIII. Summary of Cumulative Load Factors for Typical Average Day

	Kilowatt-Hours*	Demand*	Daily Load Factor (Per Cent)
1. Lighting in living quarters	3,828	286	56
2. Includes addition of miscellaneous lighting in offices, hospital, recreation building, storehouses, and so forth	7,056	424	69
3. Includes addition of hangar office and shop lighting and heating	22,693	1,342	70
4. Includes addition of street and flood lighting	23,160	1,380	70
5. Includes addition of shop and miscellaneous power	31,433	1,766	74
6. Includes addition of electric cooking and baking	32,593	1,815	75
7. Includes addition of helium compressors, exhausters, and air-compressor motive power	38,530	2,065	78

* Estimated requirements at substation including ten per cent distribution losses.

and maintain powered balloons for coast patrol service. The personnel needed for the operation of the base is only about 1,000 men. The principal type of load at this base is hangar lighting, shop power, and power for a helium repurification system. There are two hangars planned, each about 1,050 feet in length and capable of housing six patrol balloons in each hangar. The balloons are approximately 70 feet in diameter and 250 feet long. Shops and offices are located along the sides of the hangars.

Cooking will be done electrically in a 696-man enlisted men's mess and in a 252-man officers' mess. The baking will be done electrically, and electric cooking is planned in the recreation building and hospital.

The estimated demand factors (Table XII) for this base range from 56 per cent for the barracks lighting, to 42 per cent for the total load. The estimated daily load factors range from 56 per cent for barracks lighting up to 78 per cent for the total load on days when the helium repurification system is being operated. On an average monthly basis the load factor is estimated at 67 per cent.

Details of the load estimate are shown on Table XV. The manner of making the estimates for the various types of loads is substantially the same as in the other load estimates given in this paper. There are, however, two or three loads of a somewhat special nature.

Table XIV. Peak Responsibility of Various Groups of Services for Typical Average Day

Service	Kilowatts*	Per Cent
1. Lighting in living quarters	220	10.7
2. Miscellaneous lighting in offices, hospital, recreation building, storehouses, and so forth	165	8.0
3. Hangar office and shop lighting and heating	957	46.3
4. Street and flood lighting	39	1.9
5. Shop and miscellaneous motive power	385	18.6
6. Electric cooking and baking	50	2.4
7. Helium compressors, exhausters, and air-compressor motive power	249	12.1
Total	2,065	100.0

* Based on maximum demand period of 8–9 p.m. Demands estimated at substation.

One of these is 330 kw of connected lighting in the main part of each hangar that will be used continuously. It is estimated that the full connected load will be used at night, and 30 per cent of the connected load will be used during the day.

Another special load is shop power located in the hangars and having a total connected load of 950 kw (included in the power group) planned to be used on a three-shift basis. An estimated demand factor of 25 per cent was used to estimate the loads throughout the day, except that at meal times and shift changes the load is expected to be lower.

The connected loads for the helium repurification system and air-compressor motors total 625 kw. The helium re-

Table XV. Naval Lighter-Than-Air Station—Estimated Daily Fluctuation in Various Types of

Time of Day	Annual Maximum	Lighting in Living Quarters	Annual Maximum	Miscellaneous Lighting	Annual Maximum	Hangar, Office and Shop Lighting and Heating	Annual Maximum	Street and Flood Lighting	Total Lighting	Annual Maximum	Shop and Miscellaneous Power	Annual Maximum
Connected Load (Kilowatts)		508*		481**		1,735**		37†	2,761		1,330**	
12–1 a.m.		60		65		855		35	1,015		220	
1–2		60		65		855		35	1,015		345	
2–3		60		65		855		35	1,015		345	
3–4		60		65		855		35	1,015		355	
4–5		60		65		855		35	1,015		360	
5–6		150		80		860		35	1,125		360	
6–7 a.m.		225		80		580		20	905		235	
7–8		210		165		395			770		235	
8–9		180		165		385			730		355	
9–10		160		165		335			660		365	
10–11		145		165		335			645		360	
11–12 noon		145		165		335			645		235	
12–1 p.m.		150		100		260			510		225	
1–2		145		155		340			640		360	
2–3		145		155		335			635		360	
3–4		150		155		335			640		365	
4–5	240	200	170	165	505	335	0		700	265	240	95
5–6	250	260*	195	190	585	320	20		770	255	230	70
6–7	285*	245	170	140	840	525	35	20	930	400	350	50
7–8	250	215	180	150	840	825	35	35	1,225	400	350	45
8–9	225	200	180	150	885	870	35	35	1,255	400	350	45
9–10	110	110	125	110	875	860	35	35	1,115	395	345	40
10–11		85		80		855		35	1,055		345	
11–12 m.		60		75		855		35	1,025		230	
Total		3,480		2,935		14,215		425	21,055		7,520	
Load factor (per cent)												
Daily or monthly		56		64		68		51	70		90	
Annual		51		63		67		51	66		78	

† On during all dark hours.

* 508×51.5 per cent demand factor = 260 kw maximum (5–6 p.m.). Annual maximum 508×56 per cent = 285 kw maximum.

** Estimated on the basis of individual detail for each of the separate load groups.

purification system consists of three 75-horsepower motors for the high-pressure compressors and one 150-horsepower motor in each hangar for the low-pressure exhauster motors. There is also one 50-horsepower air-compressor motor in each hangar. The estimated load for the average day was based on the assumed use of one 150-horsepower exhauster motor and one 75-horsepower high-pressure compressor motor being used simultaneously. The load for the estimated maximum day is based on one 150-horsepower exhauster motor and all three 75-horsepower motors being used at one time.

The repurification process is performed in two stages. The impure helium is taken from the balloons at slightly more than atmospheric pressure and compressed by means of the low-pressure exhauster motor to a pressure of 60 pounds per square inch. Thence it is delivered to a large storage sphere which is about 60 feet in diameter. The helium gas is then put through purification processes which remove the carbon dioxide, nitrogen, and other impurities. The second stage is performed by the high-pressure compressors. These are three stage, tandem, reciprocating compressors which compress the helium to a pressure of 2,500 pounds per square inch and deliver it to the tank car. By means of by-passing one of the stages of the high-pressure compressor, the helium can be delivered at a pressure of 750 pounds per square inch directly to the storage tanks. The repurification process through the high-stage compressors takes about three days of constant running.

Table XVI

Type of Units	Number	Connected Load (Kilowatts)		Estimated Annual Maximum Demands* at Time of System Peak	
		Per Unit	Total	Per Unit**	Total
Apartments	4,000	2.59	10,337	0.39	1,562
Dormitories†	4,000 men	0.34	1,367	0.18	725
Temporary houses	4,000	15.5	62,000	1.29	5,170
Permanent houses	700	16.0	11,200	2.26	1,584
Pumping	6	143.0	860		660
Commercial, street lighting, and miscellaneous	lot		2,000		1,034
Total			87,764		10,725

* Including estimated ten per cent substation and distribution losses.

** Average on three-shift basis of work.

† Includes mess halls, electric kitchen, administration building, street lighting, and post office.

Loads, Typical Average Day

Electric Cooking and Baking	Total Lighting, Power, Heating, and Cooking	Annual Maximum	Helium Repurification and Air-Compressor Motors	Annual Maximum	Total		
					At Point of Use	Annual Maximum	At Substation
223**	4,314		625**		4,039		‡
30	1,265		225		1,490		1,640
30	1,390		225		1,615		1,775
30	1,390		225		1,615		1,775
50	1,420		225		1,645		1,810
80	1,455		225		1,680		1,850
70	1,555		225		1,780		1,960
50	1,190		225		1,415		1,555
40	1,045		225		1,270		1,395
50	1,135		225		1,360		1,495
60	1,085		225		1,310		1,440
50	1,055		225		1,280		1,410
40	920		225		1,145		1,260
35	770		225		995		1,095
40	1,040		225		1,265		1,390
55	1,050		225		1,275		1,400
50	1,055		225		1,280		1,410
35	975	425	225	1,700	1,200	1,870	1,320
25	1,025	425	225	1,800	1,250	1,980	1,375
45	1,325	425	225	2,205	1,550	2,425	1,705
50	1,625	425	225	2,175	1,850	2,390	2,035
45	1,650	425	225	2,195	1,875	2,415	2,065
35	1,495	425	225	2,005	1,720	2,205	1,890
30	1,430		225		1,655		1,820
30	1,285		225		1,510		1,660
1,055	29,630		5,400		35,030		38,530
55	75		19	(monthly—6 days per month)			
46	69		10	(annual)			

‡ Includes estimated ten per cent substation and distribution losses.

Tables XII through XV present details of the load analysis.

V. Load Estimate for a War Housing Project

In order to determine the loads to be expected from the various types of housing units included in this project, the plans were first examined in detail to ascertain the equipment to be used and the lighting outlets provided. This information was checked wherever possible with that actually installed.

Table XVI shows the connected loads and estimated annual maximum demands of each of the various groups of housing units. The annual maximum demand of the total project is estimated to be 10,725 kw at the main substation.

APARTMENTS

The detail of the connected load on the 4,000 units of apartments is indicated in Table XVII.

The typical *A* unit (one-room) has two ceiling pull-chain outlets (200 watts), two duplex plug outlets (100 watts), one light switch and plug combined (200 watts), and a "Rangette" (1,650 watts), totaling 2.15 kw per apartment unit.

The typical *B* unit (two-room) has two pull-chain outlets (200 watts), four duplex plug outlets (200 watts), two light switches and plug outlets combined (400 watts), and a "Rangette" (1,650 watts), totaling 2.45 kw per apartment unit.

The service units are for laundry, bathrooms, heating plant, and storage rooms. The total lighting aggregates 3.6 kw connected. The power consists of ventilating motors aggregating 290 horsepower for the total of 150 service units.

The average connected load per apartment, including service units and street lighting, is 2.59 kw, and the coincidental

Table XVII

Units	Type	Connected Load	
		Per Unit	Total
1,024	*A* (1-room)	2.15	2,202
2,976	*B* (2-room)	2.45	7,291
(150)	Service units		
	lighting	3.6	540
	power	1.93 (Avg.)	290
(70)	Street-lighting units	0.2	14
4,000	apartments	2.59	10,337

Table XVIII

Lighting	0.8 kw	500 kilowatt-hours
Small appliances	1.0 kw	
Refrigerator	0.2 kw	250 kilowatt-hours
Range	11.0 kw	1,100 kilowatt-hours
Water heater	2.5 kw	3,000 kilowatt-hours
Total	15.5 kw	4,850 kilowatt-hours

demand at the time of the system peak averaged 0.39 kw per unit. The resulting demand factor is 15 per cent.

The war apartment units were first separated into three groups of units, each group representing the apartments to be used by each working shift. The relative number of apartments assigned to each shift was determined on the basis of the number of workers currently employed on each shift. On this basis 1,600 apartments were considered to be on the day shift, 1,600 on the night shift, and the remaining 800 on the "graveyard" shift. The load fluctuations during the day were estimated for each group separately. On this basis the coincidental demands at the time of the system peak were estimated to be 832 kw for the first shift, 521 kw for the second shift, and 209 kw for the third shift, making the total of 1,562 kw for the 4,000 apartments.

The basic estimate of 0.4 kw average maximum demand per unit was used. After analysis of the load fluctuation on the three-shift basis, the average was lowered to 0.3 kw per unit. Allowing for seasonal fluctuation and distribution losses, the resulting maximum demand per unit was calculated to be 0.39 kw per unit.

DORMITORIES

The dormitory units consist of ten dormitories, each housing approximately 400 men. A large complete electric kitchen furnishes meals at all hours for the men housed in them.

Each of the ten dormitory units has 83 kw connected in lighting and 17 kw connected in ventilating fans and boiler-room power. This makes a total connected load of 100 kw per dormitory. Each dormitory consists of 148 double rooms, 96 single rooms, and 4 lounges or club rooms.

An administration building has a connected load of 50 kw. The connected street lighting load is 50 kw.

The kitchen and mess halls have a total connected load of 267 kw. This consists of 180 kw in electric cooking and baking equipment; 59.8 kw in kitchen lighting, mess-hall lighting, and small kitchen motive power, and 27.2 kw in refrigeration, ventilation, and boiler-room motive power.

The load fluctuations during the day were estimated separately for each of the various load groups within each of the dormitory groups assigned to the three shifts. Similarly, the load for the kitchens and mess halls was estimated by analyzing the daily fluctuations for each load group separately.

The total connected load for the ten dormitories, administration building, street lighting, and the mess halls and kitchen was 1,367 kw. The annual maximum demand was estimated to be 725 kw.

TEMPORARY HOUSES

The temporary houses consist of 4,000 demountable units, each unit having the connected equipment and using the estimated energy per year shown in Table XVIII. The average monthly maximum demand per unit is estimated at 1.5 kw, based on test information which was secured from various sources.

The houses were segregated into groups applicable to each shift on the same basis as were the apartments. When the daily fluctuations of the combined groups on the three shifts were analyzed, the average monthly maximum demand at the point of use was estimated to be one kilowatt per unit. After accounting for seasonal variation and distribution losses, the annual maximum demand resulted in an average of 1.29 kw per unit.

PERMANENT HOUSES

The permanent houses consist of 700 units, each unit having the connected equipment and using the estimated amounts of energy per year shown in Table XIX. The average monthly maximum demand per unit is estimated to be 1.75 kw, based on test data.

The load on this class of houses was estimated on the assumption that most of them would be occupied by families of the permanent employees and not subject to shift variations. The annual maximum demand at the substation, including distribution losses, is estimated to be 2.29 kw per unit.

The demand requirements for both the permanent and temporary houses appear, perhaps, to be higher than ordinarily would be expected on an ordinary system. This is due to a realization of 100 per cent saturation of ranges, refrigerators, and water heaters. This equipment is all standard household size, the water heater having a 1,000-watt base element with a 1,500-watt booster, both thermostatically controlled.

Table XIX

Lighting	1.0 kw	600 kilowatt-hours
Small appliances	1.3 kw	
Refrigerator	0.2 kw	420 kilowatt-hours
Range	11.0 kw	1,380 kilowatt-hours
Water heater	2.5 kw	3,600 kilowatt-hours
Total	16.0 kw	6,000 kilowatt-hours

The use of this equipment where no individual house meter is provided places this service in the unmetered category. People living in these housing units will probably not be educated to unmetered use of electricity and will use much more than the general average. It is to be expected that on cool mornings many people will take advantage of the unmetered service and partially heat the house by means of using either the surface or oven elements.

Because of the three-shift working and living conditions, the load peaks may be of longer than usual duration. The morning cooking peak for those going to work on the day shift will probably be around 6:30 to 7:30 a.m. This peak will be followed by another one probably from 7:30 to 8:30 a.m. for those coming off shift at 8:00 a.m. Thus the peak period of the combined project will probably extend over a three- or four-hour period, during which there will not be a great variation between hourly peaks.

PUMPING AND COMMERCIAL

Water supply to the project is furnished by means of five 150-horsepower and one 30-horsepower pumps. At the time of the peak on the system it was considered that the demand would be 660 kw.

It was estimated that the commercial, industrial, and street-lighting requirements would approximate 2,000 kw of connected load. The maximum demand was estimated at 1,034 kw.

References

1. LIGHTING AND COOKING LOADS IN ARMY BARRACKS, **Harold M. Potts.** *Electric Light and Power,* December 1942, page 60.

2. ESTIMATED LOAD FOR 35,000-MAN ARMY CANTONMENT, **Harold M. Potts.** *Electric Light and Power,* January 1943, page 78.

The Rating of Power Transformers as Determined by the War Emergency

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Synopsis: Accurate rating of transformers is one of the most important elements in the problem of providing continuous service to the customers of the utility industry. Rather than attempt to follow a general rule for the loading of transformers it is believed that the urgency of wartime needs amply justifies the effort required to place individual ratings on each transformer bank of the utility under consideration. Experience has shown that such ratings vary widely. The transformers cover a wide range of sizes, designs, manufacturers, and periods of manufacture; with weights per kilovolt-ampere ranging from 7 to 20 pounds, excitation voltages ranging from 95 to 110 per cent, their daily load factors from 25 to 100 per cent, and their seasonal loading from 0 to 100 per cent. The consequent saving of "critical" material is self-evident when each case is considered individually.

During the war it is desirable to base station ratings on the thermal capacity of all the transformers normally available for service, rather than attempt to set up station ratings based on abnormally high emergency ratings of the remaining units after the assumed failure of one unit. It is essential that these higher station ratings be properly applied so that the danger of failure is minimized.

In order to evaluate this program of individual transformer ratings, only dry units are considered. Since moisture accelerates the rate of insulation deterioration[1] and lowers its dielectric strength, increased ratings are not assigned to a questionable transformer until its moisture content has been reduced to a satisfactory value. Once this factor is eliminated, consideration of the other factors can be taken with greater accuracy and safety. Insulation deterioration and oil sludging are affected both by the magnitude and the duration of their temperatures. However, because of the simplicity of the rating method, it is necessary to consider only the magnitude at first. But as the load increases, the duration as well must then be taken into account if a maximum utilization of transformer capacity is to be realized. The capacity of bushings, tap changers, and other current-carrying parts must be checked since they may prove to be the limiting factor.

Such a program of calculating transformer capacities based on load cycles, ambient temperatures, manufacturer's test data, and operating conditions is a large undertaking unless the work is systematized and carried only as far as load conditions warrant. Therefore, a system has been set up wherein the ratings are progressively increased as the load increases. The more detailed computations are needed only when the load approaches the ratings based on the more general and, consequently, more conservative method of rating. This procedure reduces the amount of calculating and load supervising to a minimum.

Method Used in Rating Transformers

This progressive method of rating transformers is divided into four steps:

1. The ambient temperature.
2. The manufacturer's test data and operating conditions.
3. The thermal capacity.
4. The length of time at each hot-spot temperature.

This last step which is called the equal-life method of rating takes the variations in daily peak loads, the variation in ambient temperatures, and the hot-spot temperature cycle into consideration. Each successive step which usually results in a higher rating utilizes the data obtained from the previous step.

Increased Ratings for Oil-Insulated Self-Cooling Units Due to Ambient Temperatures Below 30 Degrees Centigrade (First Step)

The first step in rating transformers is to take advantage of the ambient temperatures lower than 30 degrees centigrade. Ratings are assigned, based on the Pittsburgh Weather Bureau average monthly temperatures over a 64-year period (see Table I). In order to take care of the deviation from the average temperature, six degrees centigrade is added to the mean values to obtain acceptable values for rating purposes. The actual daily average temperatures exceed the accepted monthly values used for rating less than 15 per cent of the time. The American Standards Association rule[2] which states that the "capacity can be increased 1 per cent for each centigrade degree the average temperature is below 30 degrees centigrade" is then applied to obtain monthly ratings. It may be noted that in Table I, column 2, the accepted ambient temperature for July is 30 degrees centigrade and for December seven degrees centigrade, which results in a 100 per cent rating for the month of July and 123 per cent rating for December.

When the banks are located indoors, the assumed ambient temperatures are increased five degrees centigrade under good ventilating conditions and ten degrees centigrade for average conditions. If the differential between indoor and outdoor temperatures exceeds ten degrees centigrade by measurement, the ventilation is usually improved. If the difference is less than ten degrees centi-

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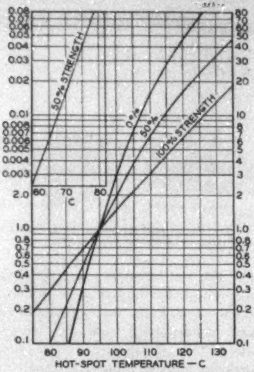


Figure 1. Life units versus hot-spot temperature for various degrees of insulation strength

One life unit equals one hour of operation at 95 degrees centigrade hot-spot temperature

grade, improvement is not usually justified.

Monthly ratings are of particular advantage in distribution substations where the lighting load is a considerable part of the total, since the shape of the seasonal load curve approximates that of the rating curve based on the outdoor ambient temperatures.

Increased Ratings of Oil-Insulated Self-Cooling Units Due to Design and Operating Conditions (Second Step)

When the load exceeds approximately 90 per cent of the rating as determined by the previous method, calculations based on the manufacturer's test data are made. In this, the losses in the transformer during the factory thermal test are calculated and compared with the actual losses in service of units built to the same specification. In determining the losses, corrections are made for the tap connections, operating temperatures, and applied voltages. In making these calculations the high- and low-voltage copper losses and the stray losses are considered separately, each being corrected to 95 degrees centigrade average copper temperature for the normal ratings. The core loss is corrected by some power of the ratio of the applied voltage to the rated voltage, the former voltage being determined from annual system voltage surveys. The exponent used is based on tests made at 100 per cent and 110 per cent excitation. If tests are not available at overexcitation, an exponent of 2.5 is suitable for most modern transformers. In the older designs this exponent ranges from 1.7 to 4.0. It must be remembered that this exponent is empirical and applies only to ratios near unity.

The temperature rises for the transformer under operating conditions at 100 per cent of rated name-plate current for the tap used are obtained by correcting the factory test values of both the hot spot over top oil and the top-oil rise by the ratio of the losses for the two conditions raised to the 0.8 power. The total losses are used for the top-oil temperature correction and the load losses for the hot-spot rise over top-oil correction. Curves of hot-spot rise over oil, hot-spot rise, and top-oil rise versus per cent load are then plotted, assuming that the rises vary as the 0.8 power of the losses as explained. (See appendix, Figure 5). This exponent is substantiated by information published in reference 3.

Before the ratings are assigned, it will be necessary to set up limiting temperatures for both the insulation and the oil. For normal ratings 105 degrees centigrade has been chosen as the limiting hot-spot temperature if the load factor is not too high. This agrees with the American Standards Association rating of 105 degrees centigrade for eight hours. The top-oil limits are set at 75 degrees centigrade for open or free-breathing transformers, 80 degrees centigrade for conservator-type units, and 85 degrees centigrade for inert gas units. These values are based mainly on experience, and the fact that sludging depends to some extent on the contact of the oil with oxygen as well as temperature and time. It is possible that these values may be revised upward if further experience justifies this action. The values chosen depend somewhat on the inspection period set up for oil reconditioning or filtering and the type of oil used.

Table I. Outdoor Ambient Temperatures

Used as a Basis for Rating Transformers Degrees Centigrade

Month	Average Daily Temperatures*	Average Daily Temperatures +6 C	Average Daily Maximum Temperature*	Average Daily Maximum Temperature +6 C
January	0	6	4	10
February	0	6	4	10
March	5	11	10	16
April	10	16	16	22
May	17	23	22	28
June	22	28	27	33
July	24	30	29	35
August	23	29	28	34
September	19	25	25	31
October	13	19	18	24
November	6	12	11	17
December	1	7	5	11

* Based on 64-year period from 1874 to 1938, Pittsburgh Weather Bureau.

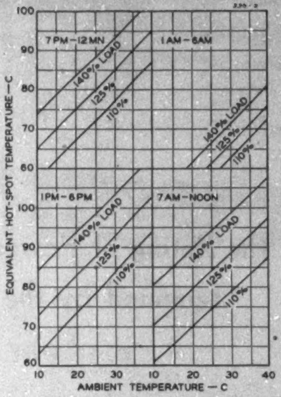


Figure 2. Illustrative equivalent hot-spot versus ambient-temperature curves for constant ambient temperatures and constant loads

Monthly ratings can now be assigned, based on the limiting hot-spot and top-oil temperatures, the ventilation, the outdoor temperatures, and the hot-spot and oil rise versus load curves. The ambient temperatures used for this method are the average daily maximum values plus six degrees centigrade as is indicated in column 4, Table I. It will be noted that the ratings are based on ultimate temperature rise and accepted maximum hourly temperature rather than the actual temperature rise and the daily average ambient temperature. This results in a somewhat conservative rating, which does not change with load cycle and requires little office supervision. Increased ratings of considerable magnitude are possible with this method of rating; yet periodic inspection of the load cycles and close scrutiny of the ambient temperatures are not necessary.

Increased Ratings Due to Thermal Capacity (Third Step)

When the load approaches the values assigned to the units by the preceding method, it is then necessary to take advantage of the thermal capacity of the units. Load cycles for a peak winter and a peak summer day are plotted and blocked. When transformer thermal transient calculations are being made, the shape of the load cycle curve at the time

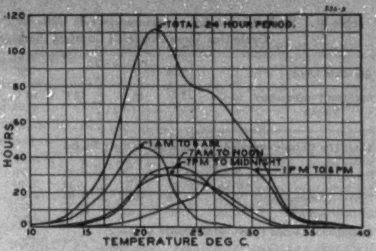


Figure 3. July temperature-distribution curve for Pittsburgh and vicinity

The curves give the distribution for the total 24-hour period as well as for 6-hour periods

of the peak load is most important. More conservative ratings are obtained by averaging the peak load over a period of hours. For base load stations a four-hour average and for residential stations a two-hour average are about the shortest periods that should be used.

Any of the published methods of making thermal transient calculations may be used, but since curves have been prepared for the previous method of rating, advantage should be taken of this information. Load-cycle calculations for two or three assumed peak loads should be made for the winter and summer cycles, using the step-by-step method. The assumed peak loads should be of the order of magnitude expected for the monthly ratings; for example 110 per cent, 120 per cent, and 130 per cent may be assumed for the summer cycle if the ratings for these months are expected to be between these values. The curves of Figure 5, the transient-heating equation, the time-constant curve, and the various tables as explained in the appendix are of considerable assistance in making these computations. Monthly ratings are obtained from curves of peak hot-spot rise and peak oil rise plotted against per cent load for both the winter and summer cycles. The curves for the spring and fall months are obtained by assuming values halfway between the summer and winter curves. Various simplifications in the method of calculating become obvious as experience is obtained in making these computations. Hereafter these ratings which are used as a basis for normal everyday recurrent loading will be referred to as "recurrent" ratings.

These three methods of rating progressively applied result in considerably higher than name-plate ratings as is indicated by Table II, which gives the results for these three steps as well as the general method of the interim report[4] for a typical suburban distribution station. In this example the general method is the more conservative, but this is not always true.

Table II. Comparison of General and Individual Bases of Rating

Aliquippa Substation, Three-Phase, 3,000 Kva, 22.5/4.33 Kv, 400 Amperes

Location—Outdoors

Time Constant *T*—3.9 hours

Method of Rating	Comparison of Rating (Per Cent) July	Comparison of Rating (Per Cent) December
I. General (step 1—ambient-temperature basis)	100	123
II. Individual (step 2—test-data basis)	107	143
III. General (interim report, Figure 1)*	108	132
IV. Individual (step 3—load-cycle basis)	122	154
V. Individual (step 4—equal-life basis)	137	154**

* 70 per cent load factor in July and 64 per cent load factor in December.

** Limited by current-carrying parts.

Industrial Versus Distribution Type of Substations

The same methods can be applied to rating both distribution and industrial substations. However, the monthly ratings have little meaning in industrial substations, as they usually have little or no seasonal trend, and in these cases the July rating would always be the limiting value. As the first step in rerating industrial substation transformers which have no seasonal trend, the load-factor method as given in the interim report which states "that the capacity may be increased 0.3 per cent for each one per cent the load factor is below 100 per cent" is quite satisfactory. However, it must be remembered that the magnitude of the hot-spot temperatures experienced at two stations having the same load factor but different load-cycle characteristics vary considerably. For example, a customer operating on one eight-hour shift may have the same load factor as one working three eight-hour shifts but would have considerably different hot-spot temperatures. More accurate methods for calculating the ratings are the individual methods discussed previously.

Increased Ratings Based on a Summation of the Life Consumed at Various Hot-Spot Temperatures (Fourth Step)

The fact that loads in most industrial plants show no seasonal trend suggests that one can afford to take more life out of the units during the summer months, as the insulation deterioration during the winter months may be almost negligible. This suggests a summation of the life consumed at each hot-spot temperature. This method of rating may be applied to either the distribution type or the industrial type of station, but the resulting rating increases are much greater in the latter stations. This method which has been called the equal-life method is now in the development stage and has not been proved by field experience.

In this method of rating, one hour of operation at 95 degrees centigrade is defined as one life unit. Assuming that the rate of transformer insulation deterioration varies in accordance with the data given in reference 1, Figures 6 and 7, a curve of hot-spot temperature versus life units can be plotted as shown in Figure 1.

The method of rating can be explained by giving a typical example. Three typical ambient temperatures such as 35, 25, and 15 degrees centigrade are added to the 24-hour hot-spot temperature-rise cycle for three different peak loads such as 110, 125, and 140 per cent and set down in table form. The three temperature-rise cycles are obtained from the previous method of rating (step 3). The hot-spot temperatures are then converted to life units by using Figure 1. The transformer assumed was considered to have deteriorated to 50 per cent of its original insulation strength. When the life units for the periods and constant ambient temperatures assumed are averaged, they can be reconverted into an equivalent continuous hot-spot temperature. The summary of this work for Aliquippa substation is shown in Table III, and the curves of equivalent hot-spot temperatures versus ambient temperatures for the three loads are given in Figure 2. The per cent load figure used in this method refers to the highest peak occurring during a 24-hour period. The day is divided into four periods, because in this type of station the heavy load occurs during the high-ambient-temperature period and the light load during the low-temperature period. A similar group of curves should be obtained for the Decem-

Table III. Reduction in Life Measured in Life Units for Aliquippa Substation Temperature Cycle (for Three Assumed Constant Ambient Temperatures)

Per Cent Load	110			125			140		
Ambient Temperature Period	15 C	25 C	35 C	15 C	25 C	35 C	15 C	25 C	35 C
Total Life Units Consumed During One 24-Hour Period									
1–6 a.m.	0	0	0.06	0	0.01	0.11	0	0.05	0.30
7 a.m.–noon	0.05	0.25	1.30	0.21	1.06	4.52	1.30	5.29	17.27
1–6 p.m.	0.08	0.48	2.37	0.42	2.10	8.72	2.47	9.95	31.50
7 p.m.–12 midnight	0.02	0.14	0.78	0.09	0.57	2.74	0.44	2.20	8.77
24-hour period	0.15	0.87	4.51	0.72	3.74	16.12	4.21	17.49	57.84
Average Life Units Per Hour for Above Cycle									
1–6 a.m.	0	0	0.010	0	0.002	0.018	0	0.008	0.050
7 a.m.–noon	0.008	0.042	0.216	0.035	0.177	0.753	0.217	0.882	2.880
1–6 p.m.	0.013	0.080	0.395	0.070	0.350	1.460	0.418	1.660	5.250
7 p.m.–12 midnight	0.003	0.023	0.130	0.015	0.095	0.456	0.074	0.367	1.465
24-hour period	0.006	0.036	0.188	0.030	0.156	0.675	0.175	0.730	2.410
Equivalent Hot-Spot Temperatures* for Life Units in Above Table (Degrees Centigrade)									
1–6 a.m.	$t<60$	$t<60$	68	$t<60$	$t<60$	71	$t<60$	66	76
7 a.m.–noon	66	75	85	75	84	93	85	94	103
1–6 p.m.	69	79	89	78	88	98	89	99	108.5
7 p.m.–12 midnight	62	72	82	70	80	90	79	88	98
24-hour period	64	74	84	74	83	92	83	93	102

* Plotted in Figure 2.

ber temperature cycle if it differs to any great extent from the July cycle.

The next step is to obtain a curve showing the distribution of the outdoor ambient temperature versus hours for the months of July and December based on a number of years. Such curves for July based on the same four periods of the day are shown in Figure 3. A summation of the life units consumed during the month for the actual ambient temperatures and the three assumed peak loads can then be obtained by applying the July temperature-distribution values to Figure 2. This summation is given in Table IV. The summation of the life units divided by the number of hours in a month gives the average rate of consumption of life units and an equivalent hot-spot temperature for each per cent load.

A curve of per cent load versus equivalent monthly hot-spot temperature can then be plotted (Figure 4). Since the loads for the various days of the week usually vary, it is necessary to express the peak loads for each day in terms of name-plate rated current and convert to life units, make the summation, and reconvert to an equivalent monthly hot-spot temperature. This calculation is given in Table V. The limit for this method of rating is assumed to be the consumption of an average of 0.5 life units per hour which is equivalent to continuous operation at 90 degrees centigrade hot-spot temperature. The rate of deterioration is fairly high at this temperature, and so the limit may have to be reduced as experience in this method is obtained.

When the summer and winter load cycles differ, calculated values of equivalent monthly hot-spot temperatures for two months, July and December, usually suffice, as similar values for other months can be obtained by weighing the two calculated values, taking the accepted monthly ambient temperatures into consideration. From the monthly values the total life units expended annually and an equivalent annual hot-spot temperature can be obtained by the method given in Table V.

A word of caution should probably be added here. In order to take advantage of these increased ratings, it is essential that the transformer be in good condition, that is, the moisture content should be low, the current-carrying parts such as tap changers and bushings should have sufficient capacity, and the oil-expansion space should be ample. It is also necessary to check the oil temperatures to see that the rate of sludging is not too high.

Other Methods of Increasing Capacity

Rating transformers on an individual basis has an additional advantage in that it indicates what corrective measures are necessary to increase their capacity. For example, if oil temperature is the limiting factor, improved ventilation, the installation of additional radiators, or the installation of inert gas equipment which permits operation at higher oil temperatures will usually result in relatively large increases in capacity. If overexcitation is limiting the rating, installation of boosters in the neutral of star-connected transformers is the logical solution.

One result of the war has been the shift in the policy of rating two bank stations from the emergency rating of one transformer bank to the recurrent ratings of two banks. This has resulted in considerably increased station ratings, but it may necessitate load curtailment during some emergencies.

The rapid increase in the war industry and the difficulty in obtaining materials has necessitated the shifting of units from lightly loaded stations to release capacity for new war plants. Here again individually calculating bank capacities pay dividends, as banks having high time constants can be applied to the stations having peaked load cycles, and low time-constant banks can be applied to high load-factor stations where it is not possible to take advantage of the thermal capacity of the units. If the amount of inert gas equipment is limited, the ratings indicate the transformers on which this equipment can be used most profitably.

Auxiliary Cooling

When the other methods of increasing capacity have been exhausted, it is necessary to install some auxiliary method of cooling such as air blast, water-spray rings, or heat exchangers. The use of fans is usually the most convenient method, and, when sufficient fans are permanently installed, a 20 per cent increase in name-plate capacity is added to the recurrent rating without field tests. If more capacity is required, it is desirable to run tests on the units in the field. When tests are made, and the top-oil rise and gradients are established for the new conditions, the same three methods of rating as discussed previously can be applied. However, it must be remembered that the exponents used in this case will be 1.0 instead of 0.8. Additional capacity up to approximately 50 per cent is possible with this method of cooling.

Emergency Ratings

In spite of all precautions failures will occur. So emergency ratings still have an essential purpose in system operation, because they point out the bottlenecks in case of such failures. Wherever it is possible that emergency ratings may be exceeded, plans must be made to take care of the various contingencies that may arise.

In calculating emergency ratings, the usual method employed is that discussed under step 3; with higher temperature limits being assumed, 115 degrees centigrade is used for the hot-spot limitations and 90 to 95 degrees centigrade for the top-oil limitation, the calculations being made for a typical 24-hour load cycle at the station considered. This calculated rating based on average conditions is used mainly for planning. Other short-time emergency ratings with higher temperature limits are used for special conditions where the load can be relieved by switching or by the installation of fans. When an actual emergency occurs, the load-carrying ability of the units is calculated on the spot, based on the actual ambients, loads, and probable duration of the emergency. It is desirable that all essential information on the transformer affected at the time of the emergency be available so that every possible kilovolt-ampere of capacity can be utilized without undue damage to the units. Curves or charts showing the variation of top-oil temperature and hot-spot gradients with load, together with the actual top-oil temperature and hot-spot readings, are very helpful in determining the allowable overload. In an actual emergency, every effort is made to relieve the overload by transferring load or by the use of fans, although the emergency ratings are not necessarily being exceeded, as it is desirable to prevent abnormal rates of insulation deterioration.

In the majority of cases, hot-spot meters are not installed, so in these cases

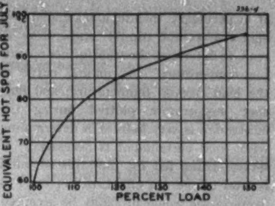


Figure 4. Illustrative curve for a distribution substation transformer showing the July equivalent hot-spot temperature versus per cent load

Table IV. Aliquippa Substation—Summation of Life Units for the Month of July

		110 Per Cent Load			125 Per Cent Load			140 Per Cent Load		
Ambient Temperature	Hours	Equivalent Hot-Spot Temperature (Degrees)	Life Units Per Hour	Total Life Units	Equivalent Hot-Spot Temperature (Degrees)	Life Units Per Hour	Total Life Units	Equivalent Hot-Spot Temperature (Degrees)	Life Units Per Hour	Total Life Units
1-6 A.M.										
10	1.18	$t<60$			$t<60$					
11.7	2.44	$t<60$			$t<60$					
13.3	4.88	$t<60$			$t<60$					
15.0	10.75	$t<60$			$t<60$					
16.7	20.00	$t<60$			$t<60$					
18.3	35.50	$t<60$			$t<60$					
20.0	45.75	$t<60$			$t<60$			60	0.0025	0.12
21.7	41.00	$t<60$			$t<60$			62	0.0036	0.15
23.3	17.00	$t<60$			61.2	0.003	0.051	64	0.005	0.09
25.0	5.12	$t<60$			62.5	0.004	0.020	66	0.0074	0.04
26.7	2.13	60.3	0.003	0.006	63.8	0.005	0.011	67.5	0.01	0.02
28.3	0.25	62.0	0.004	0.001	65.0	0.006	0.002	69.0	0.013	
	186.0			.007			0.084			0.42
7 A.M.-Noon										
11.7	0.38	63.0	0.004	0.002	71.0	0.019	0.007	81	0.115	0.04
13.3	1.80	64.5	0.006	0.011	72.5	0.024	0.043	83	0.165	0.30
15.0	3.31	66.0	0.007	0.023	74.0	0.032	0.106	85	0.225	0.75
16.7	6.25	67.7	0.010	0.063	75.6	0.046	0.288	86	0.260	1.63
18.3	13.50	69.3	0.014	0.189	77.3	0.074	1.000	88	0.360	4.86
20.0	24.00	71.0	0.019	0.457	79.0	0.090	2.160	90	0.480	11.52
21.7	29.54	72.5	0.025	0.738	80.7	0.110	3.250	92	0.66	19.50
23.3	29.00	74.0	0.032	0.928	82.3	0.145	3.820	93	0.76	22.00
25.0	26.54	75.5	0.044	1.170	84.0	0.200	5.308	95	1.00	26.54
26.7	22.35	77.1	0.058	1.300	85.2	0.230	5.140	96	1.15	25.70
28.3	15.48	78.7	0.078	1.210	86.8	0.300	4.644	97	1.30	20.10
30.0	8.60	80.4	0.110	0.946	88.3	0.380	3.270	98	1.50	12.92
31.7	2.87	81.9	0.135	0.388	90.0	0.490	1.407	100	1.95	5.60
33.3	0.88	83.4	0.170	0.150	91.5	0.600	0.527	101	2.25	1.98
35.0	0.75	85.0	0.225	0.169	93.0	0.760	0.570	103	2.90	2.18
36.7	0.75	86.5	0.280	0.210	94.7	0.980	0.735	104	3.30	2.48
	186.0			7.954			32.275			158.19
1-6 P.M.										
15	0.38	69.0	0.013	0.005	78.0	0.068	0.026	89	0.46	0.17
16.7	1.13	70.7	0.018	0.020	79.7	0.096	0.109	91	0.56	0.63
18.3	3.00	72.3	0.023	0.069	81.3	0.125	0.375	93	0.76	2.28
20.0	5.38	74.0	0.032	0.170	83.0	0.165	0.888	94	0.95	5.06
21.7	9.50	75.7	0.046	0.437	84.7	0.210	2.000	96	1.15	10.94
23.3	13.38	77.3	0.060	0.803	86.3	0.270	3.615	97	1.30	17.40
25.0	17.82	79.0	0.082	1.462	88.0	0.360	6.43	98	1.50	26.80
26.7	31.25	80.7	0.115	3.600	89.7	0.460	14.38	100	1.95	61.00
28.3	33.50	82.3	0.145	4.85	91.3	0.600	20.10	101	2.25	75.50
30.0	33.50	84.0	0.195	6.53	93.0	0.730	24.50	103	2.90	97.30
31.7	23.50	85.7	0.290	6.83	94.7	0.950	22.35	105	3.70	87.00
33.3	7.13	87.3	0.330	2.37	96.3	1.20	8.56	107	3.80	34.20
35.0	2.53	89.0	0.420	1.06	98.0	1.50	3.80	108.5	5.60	14.10
36.7	2.25	90.7	0.540	1.22	99.7	1.90	4.28	110	6.45	14.50
38.3	1.75	92.3	0.700	1.23	101.3	2.35	4.12	112	7.70	13.48
	186.00			30.66			178.67			460.35
7 P.M.-Midnight										
15.0	2.75	62.0	0.004	0.011	70.0	0.016	0.044	79	0.082	0.23
16.7	5.13	63.3	0.005	0.026	71.7	0.022	0.113	80	0.10	0.51
18.3	11.88	65.3	0.007	0.083	73.3	0.028	0.332	82	0.14	1.66
20.0	24.63	67.0	0.009	0.222	75.0	0.039	0.962	83	0.165	4.07
21.7	32.60	68.3	0.012	0.382	76.7	0.052	1.695	85	0.225	7.35
23.3	34.50	70.3	0.017	0.586	78.3	0.072	2.880	87	0.320	11.08
25.0	30.25	72.0	0.023	0.685	80.0	0.100	3.025	88	0.360	10.88
26.7	21.25	73.3	0.029	0.616	81.7	0.130	2.765	89	0.420	8.93
28.3	12.13	75.3	0.042	0.552	83.3	0.170	2.233	91	0.520	6.83
30.0	6.38	77.0	0.058	0.370	85.0	0.225	1.422	93	0.760	4.85
31.7	1.94	78.3	0.074	0.144	86.7	0.300	0.582	94	0.870	1.69
33.3	1.00	80.3	0.110	0.110	88.3	0.380	0.380	96	1.15	1.15
35.0	0.56	82.0	0.140	0.079	90.0	0.480	0.269	98	1.50	0.84
	186.00			3.87			16.700			60.07
Grand total	744.00	77.0	0.057	42.49	87.0	0.306	227.73	94.5	0.915	679.47

Table V. Aliquippa Substation Transformer Rating on an Equal-Life Basis for the Month of July

	Per Cent Load	Equivalent Hot-Spot Temperature	Life Units
5 weekday peaks*	137	91.5	0.61×120 hours = 73.20
Saturday peak	125	87.0	0.31×24 hours = 7.24
Sunday peak	100	60.0	0.0025×24 hours = 0.06
		90.0**	0.48×168 hours = 80.50

* All the weekday peak loads are the same magnitude and shape. The Sunday peak has a slightly different shape, but, because of its comparatively low magnitude, it has little effect on the results.

** The limit for this method of rating is assumed to be the utilization of an average of 0.5 life units per hour which is equivalent to continuous operation at 90 degrees centigrade hot-spot temperature.

it is necessary to add a calculated hot-spot rise to the top-oil temperature as read, to determine the transformer operating temperature. A running curve of the load, top-oil temperature, and hot-spot temperature plotted during the emergency is very helpful in determining when load curtailment is necessary. If service being fed by the stressed units is vital to the war effort, higher rates of depreciation would probably be accepted as long as a second failure is not imminent. Usually, however, when emergencies do occur, the ambient temperatures are lower, the loads are lighter, or the duration is shorter than the conditions assumed for calculating the ratings.

Conclusion

Individual transformer ratings for recurrent loads are amply justified by the additional capacity which is then known to be available for the war effort.

Some 20 customers have already been carried with loads up to 60 per cent above name-plate rating. AIEE limits of hot-spot operation have not been exceeded as yet.

So far there have been no failures of transformers due to loading nor any cases where it is believed that the rate of insulation deterioration is excessive.

Appendix

The following development is given to indicate a desirable form of the standard heating and cooling equations to use when a large number of calculations are to be made, using the step-by-step method of increasing ratings:

$$W = W_s + W_d$$

$$K\theta_u^{1.25} = Cp\theta + K\theta^{1.25}$$

$$p\theta_i = \frac{K(\theta_u^{1.25} - \theta_i^{1.25})}{C}$$

Initial slope of temperature heating curve

$$= p\theta_i = \frac{\theta_u - \theta_i}{T}$$

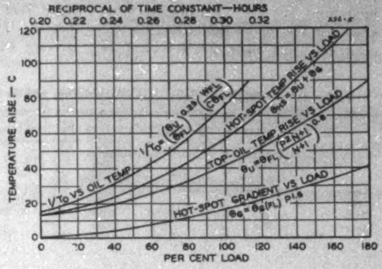
Therefore

$$\frac{1}{T} = \frac{K(\theta_u^{1.25} - \theta_i^{1.25})}{C(\theta_u - \theta_i)} = \frac{K\theta_u^{0.25}}{C} \; \frac{(1-R^{1.25})}{(1-R)}$$

$$\theta_u = \theta_{FL} \frac{(W_u)^{0.8}}{(W_{FL})^{0.8}} = \theta_{FL} \frac{(P^2N+1)^{0.8}}{(N+1)^{0.8}}$$

Figure 5. Illustrative curves for a typical transformer

These curves are used for rating transformers on an ultimate oil-temperature basis and for the thermal transient calculations



Therefore

$$\frac{1}{T} = \frac{K\theta_{FL}^{0.25}}{C} \; \frac{(\theta_{FL})}{(\theta_{FL})} \; \frac{(P^2N+1)^{0.2}}{(N+1)^{0.2}} \; \frac{(1-R^{1.25})}{(1-R)}$$

$$\frac{1}{T} = \underset{(1)}{\frac{(W_{FL})}{(C\theta_{FL})}} \; \underset{(2)}{\frac{(P^2N+1)^{0.2}}{(N+1)^{0.2}}} \; \underset{(3)}{\frac{(1-R^{1.25})}{(1-R)}}$$

$$\frac{1}{T} = \frac{1}{T_o} \underset{(4)}{\frac{(1-R^{1.25})}{(1-R)}}$$

1. The constant $(C\theta_{FL})/(W_{FL})$ is used as a time-constant reference figure for the transformers.

2. The term$(P^2N+1)/(N+1)$ for exponents of 0.2, 0.8, and 1.0 are set up in table form for values of N from 1 to 4 and P from 0.2 to 1.6 in 0.2 steps.

3. The term $(1-R^{1.25})/(1-R)$ can also be set up in curve or table form.

4. A curve of $1/T_o$ versus top-oil temperature is also plotted as shown in Figure 5.

5. A table of $(1-e^{-t/T})$ for various values of $-t/T$ is also of considerable assistance in solving the transient heating equation $(\theta_f - \theta_i) = (\theta_u - \theta_i)(1-e^{-t/T})$.

When one is solving the transient-heating equation for a 24-hour period, it is desirable to start the calculation at the end of the minimum load period. This is usually about six o'clock in the morning. The initial oil temperature (θ_i) which must be assumed for the first step, is usually about two or three degrees above the ultimate temperature rise for the minimum load. With a little experience, it is possible to estimate this temperature in advance within one degree centigrade. If the error in the original assumption is too great, the transient calculation should be continued until the maximum oil temperature is obtained. The values of (θ_u) and $(1/T_o)$ are obtained from Figure 5, the values of $(1/T)$ are obtained by multiplying $(1/T_o)$ by the function of R as explained above. The values of $(1/T_o)$ for the heating portion of the cycle corresponds to (θ_u) and for the cooling portion of the cycle to (θ_i).

List of Symbols

C = thermal capacity
N = ratio of copper to no-load loss
P = ratio of load amperes to name-plate rated amperes
$p = d/dt$
R = ratio of (θ_u/θ_i) for cooling cycle and (θ_i/θ_u) for heating cycle
t = time in hours
T = time constant
T_o = time constant for zero initial rise
W = watts loss in transformer
W_d = watts dissipation
W_s = watts stored
W_{FL} = watts loss at full load under operation conditions
W_u = watts loss at load which would produce θ_u
θ_{FL} = oil-temperature rise at full load under operating conditions
θ_i = initial oil-temperature rise
θ_u = ultimate oil-temperature rise
θ_f = final oil-temperature rise
θ_G = temperature gradient between hot-spot and top-oil temperature (ultimate values are used throughout)
θ_{HS} = hot-spot-temperature rise

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Protective Relaying for Long Transmission Lines

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UP to a certain length of transmission line, conventional distance relays will readily distinguish internal faults, for which the line should be opened, and external faults, load conditions, and power swings, for which the line should not be opened. This distinction is based on the magnitude of the impedance or reactance of the line as it appears to the relay. This impedance will be referred to hereafter as the input impedance and is determined by the current and voltage existing at the relay location.

The magnitude of the input impedance will distinguish clearly between internal and external faults for any length of lines, but, as longer lines are considered, the difference in impedance between load and fault conditions becomes less and may even be reversed. On the Kansas–Nebraska line, using conventional distance relays, there would be undesirable tripping on power swings from which the system can recover easily, such as those that may occur when the line is reclosed instantaneously after clearing a temporary fault. The power swing in this case is caused by the two terminals having drifted apart slightly during the reclosing period, because the sending end has lost load, causing it to speed up, and the receiving end has lost power, causing it to slow down; therefore, when reclosure occurs, a large amount of power flows and tends to draw the two ends together and thereby reduces the flow of power, and an oscillation is started.

Although there is little difference in the impedance magnitude during fault and load conditions on very long lines, there is, fortunately, a considerable difference in phase angle. During load conditions the heavy charging-current component tends to make the input impedance have a leading power-factor, whereas during an internal fault much of the line capacitance is short-circuited and the input impedance is therefore lagging considerably as shown in Figure 4 on curve Z_B.

As a result of the foregoing, impedance magnitude was once more used to distinguish between internal and external faults, but the distinction between faults and load conditions was based on phase angle, a quantity which was so selective that it was possible to differentiate further between load conditions, so that tripping was prevented on normal loads and power swings but allowed on out-of-step conditions.

No special relay construction was used. All the relay elements were of standard design with the reactance and resistance of their potential coil circuits arranged to obtain the desired characteristics. The equipment has certain advantages over the conventional distance-carrier-relay equipment and is not limited to extremely long lines; it can be used also for lines as short as about 50 miles. The equipment was developed for the 270-mile 154-kv interconnection between Nebraska Power Company and Kansas Gas and Electric Company systems to meet the general performance specifications laid down in a companion paper,[1] and it has been applied since to the "Ark–La" 154-kv line, approximately 200 miles long in Oklahoma and Arkansas.

Impedance Diagrams

If capacitance and shunt loads are neglected, the transmission of power over a simple single-line system can be represented vectorially as shown in Figure 2 wherein V_s and V_r represent the voltages behind the terminal generator impedances, and the voltages V_a, V_b, and so forth, are the voltages existing at various intermediate points. The system interchange current I_s and the angle δ will both increase in magnitude as the power transmitted is increased.

The relays located at the various intermediate substations will see impedance values determined by the relation between the substation voltage (such as V_a or V_b) and the current I_s. This value is the equivalent input impedance of the system and will be referred to as the input impedance. As the load changes, θ, V_a, V_b, I_s, and the input impedance will change. In a companion paper[1] the loci of the input impedance at a given location for various ratios of line-terminal voltages are shown to be circles on a resistance-reactance diagram. Furthermore, it is shown that, for the special ratio of line-terminal voltages $E_s/E_r = D$, where D is one of the "general line constants," the radius of such a circle is infinity, and the circle becomes a straight line.

For short lines where capacitance and shunt loads can be neglected the general line constant $D = 1$, and thus, if the terminal voltages V_r and V_s are always equal, the loci of input impedance as "seen" from terminals R and S (Figure 2) are straight lines coinciding with the 0 per cent and 100 per cent lines of the distance scale on Figure 3. When these straight-line loci obtain simultaneously at the two terminals of a circuit, the loci of the impedances "seen" at intermediate points on the circuit are likewise straight lines and form a family of parallel lines as in Figure 3, which was first presented to the Institute in a discussion by J. H. Neher.[2]

Under the conditions for which it is valid, that is, systems with negligible capacitance, no shunt load, and equal terminal voltages, Figure 3 shows that the input impedance, as seen from any particular point, is a minimum when the

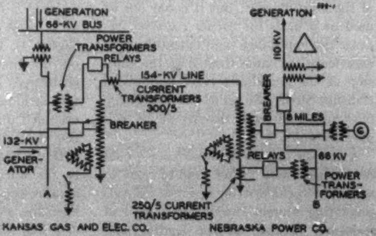

Figure 1. Connections of relays relative to transmission line

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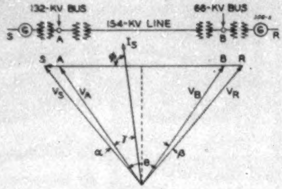


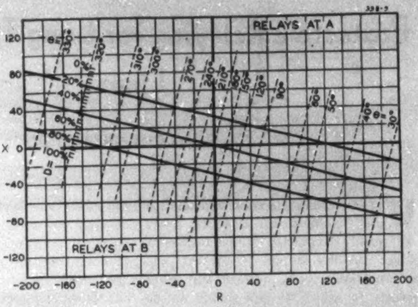


Figure 2 (left). Vector diagram of circuit in Figure 3

Figure 3 (right). Input-impedance diagram for relays at various locations along a line with $X/R = \tan 75°$ connecting two systems or machines, applicable to the special case in which $V_s = V_r$ assuming no shunt load and neglecting shunt capacitance

D = Distance of relay location from equivalent internal voltage of the leading end V_s, in per cent of total impedance from S to R

The co-ordinates are scaled in per cent of total impedance from S to R. Abscissas are resistance; ordinates are reactance

terminal voltages are displaced 180 degrees, and is infinite when the equal terminal voltages are in phase, since under this condition there is no current flow.

This type of chart is very convenient for determining whether or not a particular relay will operate for a particular amount of load transfer or for a particular angular displacement under swing conditions. To determine this, it is necessary only to plot the relay pickup characteristics on the diagram (being sure to plot both characteristics to the same scale) and observe at what value of the line-displacement angle θ the relay characteristic crosses the line characteristics.

In the more general case for which Figure 3 is not valid (that is, where terminal voltages are not equal, line capacitance is large, or there are important shunt loads), it is evident that the families of input-impedance circles applying to each relay location must be examined and compared with the relay pickup characteristics to determine if that relay is suitable for the proposed application.

In Figure 4, which represents a typical input-impedance circle for the 270-mile Kansas–Nebraska interconnection point A represents the system impedance for a fault at the far end of the line, and point D the impedance for a more remote fault up to which backup protection is desired. The third zone of the conventional impedance relay set to operate in backup time up to D would also operate on the maximum load of 50,000 kw. The conventional reactance relay with the same setting would not operate on this load, because it is outside the characteristic of the starting unit, but it could not be used as a basis for carrier relaying, because the impedance fault detector would experience the same trouble as the third step of the impedance relay and would transmit carrier and set up the out-of-step blocking circuit, and thereby delay tripping on faults.

A further margin of impedance would be required with either kind of relay to take care of the drifting apart of the two generating sources during the period of reclosure, because of the leading and losing load end speeding up and the lagging end losing generation and slowing down; otherwise, the relays would trip on the reduced impedance condition caused by the two stations swinging apart before reclosure.

The problem then was to design relays which would:

1. Trip instantaneously for faults up to A, including arc resistance.
2. Trip on out-of-step conditions and swings from which the system will not recover.
3. Not trip on loads up to the steady-state power limit.
4. Trip in delayed time up to D, for backup protection.
5. Not trip on swings from which the system would be likely to recover, that is, roughly up to $\delta = 110$ degrees.
6. Provide carrier blocking for external faults.

The Solution

Items 1, 2, and 3 require that the relays should trip instantaneously for conditions within the area A-A' 120 degrees–B-B' 240 degrees. A circle fits very well around these points (see Figure 4), the center of the circle being on the line-impedance line AB, about 60 degrees lagging the R axis, though it also includes some loads which should be excluded under item 3. Such a circle is the characteristic of a directional relay with a voltage restraint, which is similar to the starting unit of the standard reactance-type distance relay. This unit measures a quantity of the form $I \cdot f(\phi)/V = Y \cdot f(\phi)$ which is "admittance at an angle." Since this quantity is in mhos, the unit is called a "mho unit," corresponding to the term "ohm unit" applied to the reactance-measuring unit of the distance relay, which measures "impedance at an angle."

Items 4, 5, and 6 require a relay combination which will operate within the shaded area of Figure 5. The straight sides of the area are similar to the characteristics of a reactance unit except that they represent constant impedance at 20 and 30 degrees leading the unity-power-factor position instead of lagging it by 90 degrees. The ends of the area were cut off by the circular characteristic of an impedance unit as shown in Figure 5. In other words, by connecting *a* contacts of the impedance unit and the two ohm units in series with a timing unit, backup protection with delayed tripping occurs in the lightly shaded area KPQ. By connecting a set of *b* contacts of the three units in parallel in the carrier-control circuit, carrier blocking is provided over the same area KPQ, which disposes of items 4 and 6.

In reference to items 2 and 5, by connecting the contacts of the ohm units in series with those of the mho unit, the value of δ at which tripping occurs can be controlled accurately by the settings of the ohm units; otherwise, this limit is determined by the mho unit, whose setting is based on the length of the line and hence cannot be adjusted to a value of δ also. With the present line, Figure 5 shows that the mho unit would prevent tripping on swings up to about $\delta = 75$ degrees, but it was decided that about 100 degrees would be required to provide sufficient margin for power swings, especially after instantaneous reclosure of the breakers following a fault.

Relay Settings

At this point it will be apparent why 20- and 30-degree characteristics were

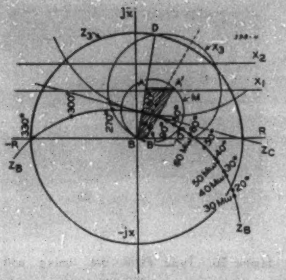


Figure 4. Impedance diagram of load and fault conditions on long line at terminal *B* of Figure 1

Angles marked on Z_B circle are angles between terminal voltages

AB = impedance of line to three-phase fault at end of line = 370 ohms

AA' = arc resistance with single-end feed, 60 miles per hour wind and minimum generation—fault at *A*—125 ohms

BB' = ditto with fault at *B*, near bus—32 ohms

AD = reach of backup (zone 3) protection

Mw = megawatts sent from *A* to *B*

chosen for the ohm units. As the characteristics were pulled in closer to the origin in order to prevent tripping on swings up to $\delta = 110$ degrees, they had to be made parallel to the boundaries of the tripping area in order to insure tripping for all fault conditions.

The 20-degree characteristic can be as close to the line-impedance vector as desired but, the closer it is, the lower the ohmic pickup, and hence the higher the potential burden. A pickup of 42 ohms was chosen first for this unit but was increased later to provide higher relay torques.

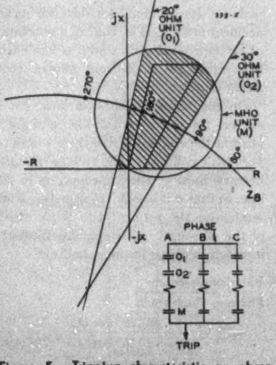


Figure 5. Tripping characteristic on phase faults and out-of-step conditions

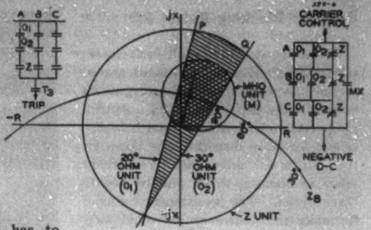


Figure 6. Blocking and backup protection on phase faults and power swings

Carrier blocking and time backup are provided in the shaded areas. Carrier is shut off by the mho unit in the double shaded area allowing instantaneous tripping for internal faults

The 30-degree characteristic has to allow for the maximum arc voltage, which will be during minimum generation and single-end feed.

Some approximate calculations of arc resistance for this circuit indicated that it might be between 50 and 70 ohms for a nearby fault and between 100 and 150 ohms for a fault at the far end. To allow for these arc values and to prevent tripping on swings required a setting of 72 ohms.

The diameter of the mho-unit circle represents its setting in ohms. To cover the fault area *AA' BB'* it was made initially about 375 ohms but was increased later to provide greater pickup torque and consequently faster action on far-end faults. The setting of the impedance unit was merely a rough margin between nearby and remote faults, so as to prevent the carrier and the timing unit from being operated unnecessarily; the value chosen was 500 ohms.

The effect of combining these circuits and their characteristics is shown in Figure 8, and explained in the next few paragraphs.

Over-All Operation of Protective Relays

Figure 8 shows the various zones of operation, and Figure 9 is a schematic diagram of the d-c connections.

A phase fault on the 154-kv line is within both the carrier and tripping zones (see Figure 8 zone T_1). The ohm units O_1 and O_2 and the *Z* unit in the same phase open their *b* contacts in the carrier-control circuit, turning on carrier. The mho units *M* pick up the *MX* auxiliary relay which by-passes the contacts of the *O* and *Z* units and restores negative direct current to the grid of the oscillator tube in the transmitter stopping carrier. Another contact of the *MX* relay de-energizes the receiver-relay holding coil, and, with carrier off, the receiver relay then closes its contacts and causes tripping in a total time of about $1^1/_3$ cycles.

A ground fault picks up the overcurrent fault detector *G* whose *b* contact removes negative direct current from the transmitter grid while the current polarized directional relay *GD* reapplies negative direct current and stops the carrier. Other contacts of *GD* de-energize the receiver-relay holding coil and complete the tripping circuit through the contacts of the receiver relay.

An external fault within the range of the mho unit will not trip instantaneously through the receiver relay, because, at the faulted end, the mho unit is reversed and hence does not shut off carrier, so that instantaneous tripping is prevented, but the relay will trip in intermediate time (T_2), if the fault is not cleared by the correct breaker.

An external phase fault within the range of the ohm and impedance units turns on carrier as before and starts the timing unit (see zone T_3C of Figure 8), but at the faulted end the mho unit is reversed and hence does not shut off carrier, so that instantaneous tripping is prevented, but backup time (T_3) tripping will occur if the correct breaker does not trip. If the carrier is off for maintenance, the receiver-relay contacts *R* are short-circuited by the switch *SC*, so that tripping occurs directly when the mho unit closes (zones T_1 and T_2). Instantaneous tripping with "carrier off" is limited to the line and a little beyond the windings of the transformers at the far end by the setting of the mho units. If the fault is beyond this zone, the mho unit does not operate, and the timing unit operates in backup time (T_3), if the fault has not already been cleared. A fault within the reach of the mho unit but beyond the location of the opposite mho units will be cleared in intermediate time (T_2) if not cleared already by carrier relays.

An external ground fault within the range of the overcurrent blocking unit (G_1) turns on carrier, but at one end the directional unit (*GD*) is reversed and does not turn off carrier, so that instantaneous tripping is prevented. In that case, the time overcurrent ground relay (type *IAC*) provides backup protection, unless the proper differential relay has already operated.

Receiver Blocking Relay, RB

Carrier is started for all fault conditions, though it may be stopped almost at once by the directional relay, if the fault is in the tripping direction. If tripping occurs, the carrier will not be maintained for more than the time taken for the directional relay and its auxiliary relay to operate, which will be one to three cycles. The persistence of carrier for more than three cycles would indicate an external fault and would operate the *RB* relay which is arranged to pick up in three to four cycles and drop out in six cycles.

The *RB* relay energizes the holding coil of the receiver relay, so that, during external faults, the *RB* relay maintains the blocking action for another six cycles after the carrier has been shut off by the resetting of the relays after the fault has been cleared. This arrangement prevents tripping through the carrier relays unless the condition is maintained, as would be the case if a second fault occurred just after the first one had been cleared.

Tripping on Out-of-Step Conditions

The conventional relay equipment for short lines employs blocking relays which prevent tripping on both power swing and out-of-step conditions. These conditions are recognized by means of an impedance unit which applies blocking after a time delay of about 0.1 second, unless it is prevented by an instantaneous impedance unit with a lower ohmic setting; in other words, a swing is distinguished from a fault by the rate of change of impedance. To effect tripping during actual out-of-step conditions requires a somewhat expensive relay which requires several power oscillations before it can be sure of the out-of-step condition.

With the present equipment not only are the foregoing features inherent in the relay characteristics, but they trip instantly when the angle crosses a threshold value. Not only is no time required for the decision, but the angle itself can be set accurately from system impedance curves. In the present case a setting of 72 ohms prevents tripping for power swings up to $\delta = 110$ degrees.

Figure 7a. Type *CGZ* special distance relay

The upper is the mho unit
The lower is the *Z* unit

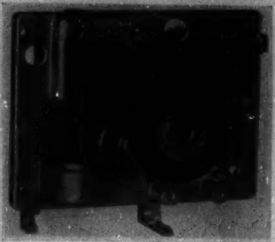


Figure 7b. Type *PDM* d-c timing unit (T_2 and T_3)

Figure 7c (right). Schematic connections of mho unit in phases 1–2

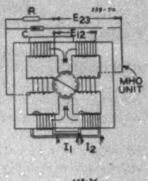
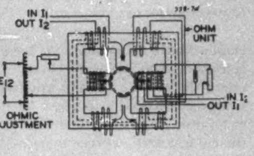


Figure 7d (below). Schematic connections of ohm unit in phases 1–2



The Mho Unit

The mho unit is a four-pole induction-cylinder unit, see Figure 7a; the two long poles have a potential winding whose flux induces eddy currents in the induction cylinder or rotor which react with the fluxes from the two short poles to produce torque. One of the short poles has two current windings excited by the currents in two of the phases, so as to make a flux proportional to their delta sum. The other short pole has a potential winding excited by the potential between the same two phases. The polarizing potential winding has voltage from the next lagging pair of phases. As an example, phase 1–2 relay has a torque proportional to: $V_{23}[(I_1-I_2)\cos(\sigma-\phi-30 \text{ degrees}) - V_{12}\sin\sigma]$, where σ is the angle between V_{12} and V_{23}, so that it operates when $(I_1-I_2)\cos(\beta-\phi-30 \text{ degrees}) > V_{12}\sin\sigma$, or when the admittance of the circuit

$$\frac{(I_1-I_2)\cos(\sigma-\phi-30 \text{ degrees})}{V_{12}\sin\sigma} > Y \text{ mhos.}$$

The value of Y is adjusted by an auxiliary potential transformer with nine ten per cent and ten one per cent taps, so that one per cent steps can be obtained.

Apart from its suitability for this application, the mho unit has the great advantage over the ohm unit of the conventional distance relay of not requiring a directional unit, since it is itself directional. On long lines, where arc resistance is not a large percentage of the total impedance, its accuracy for distance measurement is as good as that of a reactance or an impedance unit, and it is less likely to operate on loads and swings than either unit.

On the Kansas–Nebraska line $\phi = 72$ degrees for faults on the line, and for a fault at the end of the line there would be very little collapse of the voltage triangle, because most of the impedance is in the line, so that σ is about 115 degrees. Consequently, the ratio of the ohmic reach of the mho unit on three-phase faults to its reach on phase-to-phase faults is

$$\frac{\text{Three-phase-reach}}{\text{Phase-to-phase reach}} = \frac{\sin 120° \cos(115°-72°-30°)}{\sin 115° \cos(120°-72°-30°)} = \frac{0.866 \times 0.974}{0.906 \times 0.951} = 0.98$$

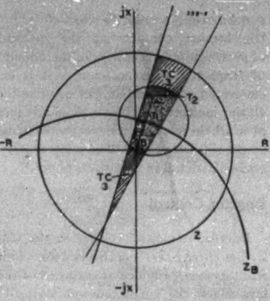


Figure 8. Over-all relay characteristics

In other words, the reach on three-phase faults is approximately the same as on phase-to-phase faults.

In order to provide fast operation on three-phase faults close to the station, the polarizing potential coil of the mho unit is made resonant, so that, when the line potential is suddenly reduced to substantially zero by the fault, the potential across the relay winding is maintained long enough to insure reliable operation. The connections of this circuit are shown in Figure 7c and are the same as are used in the standard *GCX* distance relay.

The Impedance Unit

The impedance unit is of the eight-pole induction-cylinder design (see Figure 7a), three of the poles producing current-operating torque and three producing potential restraining torque.

The windings on the three current poles are in series, but the middle pole has a copper sleeve, so that its flux is out of phase with that of the other two poles and hence produces torque. A similar method is used in the potential circuit where the middle pole is dephased by a series resistor. In the appendix, it is shown that this arrangement produces a steady torque, irrespective of the angle between the pole fluxes.

The induction-cylinder construction not only provides a high-torque low-inertia device, but it also has a pickup and dropout which are very close together; this is advantageous in a fault detector for very long lines where there is little distinction between the system impedance during load, swing, and fault conditions.

Its operating torque is proportional to the current squared, and its restraining torque is proportional to the adjacent voltage squared, so that the relay operates when the system impedance falls below its calibration. The pickup is independent of phase angle within four per cent, because of the steady torque of the induction cylinder.

Since the relay has only one current circuit, the reach on three-phase faults is 15 per cent more than on phase-to-phase faults, but this is of no consequence, because this unit controls the operation of carrier and time backup protection and hence need not be accurate.

The Ohm Unit

The ohm units are the same as that of the standard reactance-type distance relay, see Figure 7a, except that one is arranged to measure the 20-degree and the other the 30-degree component of impedance, instead of both measuring the reactive component. The change from the standard ohm unit was accomplished simply by changing the constants of the potential circuit to obtain the desired phase angle.

Each unit has four double-current windings arranged as in Figure 7d, so as to create a magnetic flux between the front and back poles of the unit. The left-hand pole has a potential winding whose phase angle is adjustable by a series resistor between 70 degrees and 55 degrees, so that the relay characteristic can be adjusted for maximum torque between 20 and 35 degrees. The right-hand pole has another double-current winding which is shifted 90 degrees in phase from the first by a floating secondary winding having a parallel capacitor and resistor.

The current flux from the front and back poles reacts with the flux from the right-hand pole to create an operating torque $K(I_1-I_2)^2$ where I_1 and I_2 are the currents in the double-current windings of the relay associated with phases 1 and 2. It also reacts with the flux from the potential pole to produce a restraining torque $E_{12}(I_1-I_2)\cos(\phi-\Delta)$ where Δ is the angle for maximum EI torque.

At the balance point the two torques will be equal so that $E_{12}\cos(\phi-\Delta)=K(I_1-I_2)$, but $E_{12}=E_1-E_2=(I_1-I_2)Z$, where Z is the impedance of a line conductor between the relay and the fault, therefore, $Z_1\cos(\phi-\Delta)=K$. K is adjustable by means of a transformer with one per cent taps.

The two ohm units in each phase have their contacts in series and are polarized in opposite directions (the 20-degree unit operates on incoming K_W and the 30-degree unit on outgoing K_W) so that the system impedance has to be between their characteristics in order to turn on carrier or to trip (see Figure 6).

Timing Unit

The timing unit is controlled by the same relays that control the carrier and is of the same design as used in the standard distance relay. It provides a short time delay for faults in the protected zone in case the carrier tripping is inoperative and a longer time delay for faults in the low-voltage windings of the transformer, and, during minimum generation at the other station, the third zone may reach a short distance into the system beyond the power transformer at the other station.

The speed of the timing unit is controlled by a centrifugal friction governor (see Figure 7b). A d-c solenoid winds up the spring which operates this unit. Two adjustable insulated contact arms close the intermediate and backup time contacts. The timing scale is graduated in tenths of a second up to three seconds. A time setting is made easily by loosening a knurled nut on the main shaft and setting the contact arms.

Ground Relays

The relays and connections for ground faults is the same as in the standard carrier-pilot relay scheme, except that the overcurrent tripping relay was made

Figure 9. Circuits of carrier relays for long transmission lines

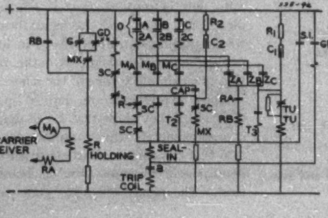


Figure 9a (left). D-c and tripping circuits

SC switch contacts shown in position for carrier on

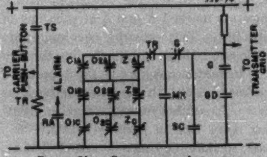


Figure 9b. Carrier control circuits

directional, thus eliminating an extra directional relay. The blocking unit is a plunger overcurrent unit which operates in a fraction of a cycle to remove negative bias from the grid of the oscillator tube in the carrier transmitter (see Figure 9b) and thus starts carrier. The tripping unit is an eight-pole induction-cylinder unit with alternate poles provided with a copper sleeve to lag their flux, so that a torque can be produced. An induction unit is used for the tripping function, because it is least liable to trip undesirably on transient conditions.

Transformer Differential Relays

These are the standard high-speed type *HDD* relays with harmonic restraint which were described in a previous paper.[3] Because of the absence of breakers on the line side of the transformers, it was necessary to provide a means of tripping the breaker at the other end for a transformer fault. This was done by arranging the differential relays to close a three-phase grounding switch on the 154-kv line which creates a fault and causes tripping at the other end.

Reclosing Relays

After the breakers at each end of the line have been tripped, they can be reclosed in four different ways:

1. Manually using the synchroscope.
2. Manually by automatic synchronizing.
3. Automatically with instantaneous or delayed reclosing.
4. Automatically with automatic synchronizing.

Figure 10 shows a detail of the reclosing circuit. Manual closing is done by closing the manual control switch (*CSW*) and the synchronizing switch on the right of Figure 10. Automatic synchronizing is initiated by turning switch 43 to position *S;* then the contacts 25 of the automatic synchronizing relay (type *GES*) will energize the reclosing circuit, provided that the voltage check relay 83 has operated, closing contacts 83*X* of its auxiliary relay.

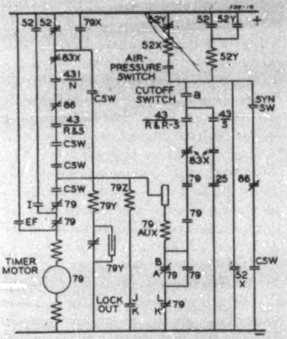


Figure 10. D-c connections of automatic reclosing circuit

In normal automatic operation it is customary to set the switch 43 at the position marked *R* and *S* at each end of the line. This provides one immediate reclosure at both ends of the line through the instantaneous step of the type *AC*-1 reclosing relay; this reclosure is made as fast as possible by connecting the initiating relay 79*X* in parallel with the trip coil, so that the timer is started at once, and the closing coil will be energized as soon as the breaker *b* switch is closed. If the fault persists and one end recloses first and energizes the line, the voltage-checking relay 83 at the other end operates; this opens contact 7–8 of 83*X* and transfers the control to the automatic synchronizing relay 25. Similarly, if only one end trips, the other end can only reclose through the synchronizing relay 25.

A number of combinations are possible: for instance, one end may have the selector-switch 43 set to *R* and the other to *S*, so that one end recloses at once and the other through the synchronizer, and, of course, the two may be interchanged. If both ends are at *R* and have similar *AC*-1 time settings, immediate reclosure will occur at both ends three times unless the breaker stays in after the first or second reclosure; with different timer settings only the early end will reclose immediately. Other combinations are *R* with *R* and *S*, *S* with *R* and *S*, and *S* with *S*. Three reclosures are provided by the type *AC*-1 reclosing relay at adjustable intervals, the maximum setting being three seconds; after the third reclosure the reclosing circuit is locked out.

The type *GES* automatic synchronizing relay 25 gives the closing indication to the circuit breaker slightly in advance of synchronism, in order to effect closure of the breaker contacts at the moment of synchronism, regardless of the amount of frequency difference, unless it is excessive, in which case it will not give the closing indication. The angle of advance at which the relay functions is proportional to the slip frequency. The relay was described in detail in a previous paper.[4]

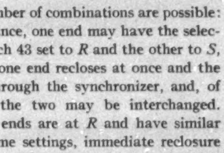


Figure 11. Connections of voltage and synchronism check relays

Reactor Control

In order to control voltage on the 154-kv line three 5,000-kva three-phase shunt reactors were provided at each end of the line which are connected automatically to the power transformer tertiary winding whenever overvoltage occurs or when the terminal breaker is opened. A short time delay is provided to avoid switching in the reactors during transient voltage disturbances resulting from line switching or from faults on the line.

Phase Faults During Heavy Load Conditions

Before the relay equipment was shipped, it was tested thoroughly on an artificial transmission line in the factory. These tests revealed that the directional properties of the mho units could not be relied upon above a certain value of load, that is, above a certain value of θ, the angle between the electromotive forces at the two ends of the line. This is caused by the fact that the voltage at the far end of the line controls the phase angle of the fault current, while the phase angle of the relay characteristic is controlled by the voltage at the near end. The fault current may therefore be far from the maximum torque angle of the relay and can be shifted still farther by the collapse of the voltage triangle on a nearby fault until it is near the zero torque position. Finally the superposition of the load or swing current actually may cause reverse torque, so that the mho unit may trip for a fault on the other side of the local transformer.

The likelihood of wrong tripping increases with the angle θ between the voltages at the ends of the system. With a bus fault the restraining torque of the mho unit is zero, so that it acts as a directional relay, and the conditions for wrong tripping can be expressed in terms of the components of current in the direction of maximum torque of the relay.

These expressions are derived in the appendix; they show that $\theta = 48$ degrees is approximately the maximum separation that can exist without causing wrong operation of the mho unit on incoming load. The phase 1–2 relay goes wrong

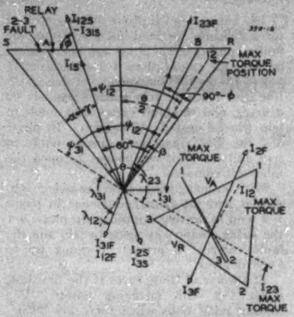


Figure 12. Vector diagram of bus fault with outgoing power

for a phase 2-3 fault; the other relays require more separation to make them go wrong.

Since power transfers in excess of this are expected, it was necessary to seek a solution. The solution to this difficulty was the addition of a polyphase directional-relay type *CAP*15*A* with its contacts arranged to supervise those of the mho unit. This relay has a torque proportional to the arithmetical sum of the 70-degree lagging components of the positive-sequence and negative-sequence volt-amperes and was described in a previous paper.[8]

The *CAP* relay operates correctly under these conditions of a bus fault for load conditions causing up to $\theta = 85$ degrees. The maximum loads expected would give $\theta = 53$ degrees, so that an adequate margin is provided. Under power-swing conditions it is, of course, possible to have more than 85 degrees displacement, but such a separation could only be reached during the reclosing period and would require a permanent fault; otherwise the tripping condition could not occur. While it was found possible to take care even of this condition, by means of a negative-sequence directional relay with a closing mechanical bias to permit operation on three-phase faults, yet it was regarded as a very improbable coincidence, and it was decided that even the *CAP* relay might never be called upon to prevent wrong operation.

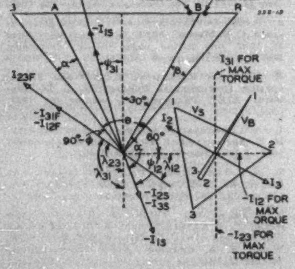


Figure 13. Vector diagram of bus fault with incoming power

System Tests

The calculated settings of the relays were checked very closely by their behavior during the system tests which are described in a companion paper. Internal and external faults were applied with zero power exchange and with the interconnection loaded approximately to its stability limit, all relay operations were correct.

Conclusions

The magnitude of the input impedance of a transmission line is always a reliable basis for locating faults, but, as the length of the line increases, impedance magnitude becomes less reliable for distinguishing between load and fault conditions. Fortunately, however, there is at the same time an increasing difference in phase angle.

Distinction by angle has the advantage that the degree of system separation for tripping can be controlled accurately and can be effected without delay.

Symbols

V_s = generator electromotive force at sending end
V_r = generator electromotive force at receiving end
V_a, V_b = terminal voltages at A and B
A, B = relay locations on transmission line
δ = angle between terminal voltages V_a and V_b
θ = angle between generator electromotive forces, V_s and V_r
α = angle between V_s and V_a
β = angle between V_r and V_b
σ = angle between polarizing and restraining potentials on the mho unit
ψ = angle between V_a or V_b and load component of current
λ = angle between V_a or V_b and fault component of current
ϕ = power-factor angle of input impedance looking into line $= \tan^{-1} \dfrac{X}{R}$

The following symbols are used on the diagrams:

M—Mho unit (phase directional unit)
O—Ohm unit

Figure 14. Relay panel

Z—Impedance unit
R—Receiver relay
TU—Timing unit
MX—Mho-unit auxiliary relay
RA—Receiver auxiliary relay
RB—Out-of-step blocking relay
G—Ground-fault detector
GD—Ground directional relay
GB—Ground backup relays
SI—Seal-in relay
TR—Carrier-test relay
TS—Carrier-test switch
SC—Carrier on-off switch
T—Target

A, *B*, *C* refer to the phases, for example. Z_A, Z_B, Z_C

1, 2, 3 refer to the zone of time or impedance. for example, O_1, O_2, O_3, T_1, T_2, T_3

25—Synchronizing relay
43—Transfer switch
43*I*—Inspection transfer switch
52—Circuit breaker
79—Reclosing relay
83—Voltage checking relay

86—Lockout relay
X, Y, Z—Auxiliary relays, such as 83X or 79Y

Appendix

The mho unit has zero voltage restraint during a bus fault involving more than one phase and therefore acts as a directional relay under that condition. The torque due to the swing current is $I_S \cos \psi$ where ψ is the angle measured from I_S to the maximum torque position. The torque due to the fault current is $I_F \cos \lambda$ where λ is the angle measured from I_F to the maximum torque position.

The following is an approximate analysis intended more as an explanation than as a rigid proof. The rigid analysis is extremely long and involved.

Referring to Figure 2, and assuming $V_S = V_R = V$ and that a 2–3 fault causes no drop in V_1

$$V_A \cos\left(\frac{\theta}{2}-\alpha\right) = V \cos\frac{\delta}{2} = V_B \cos\left(\frac{\theta}{2}-\beta\right).$$

Then, using values of Z_A taken from the system impedance circle for $V_S = V_R$, $I_{1S}=$

$$\frac{V\cos\frac{\theta}{2}}{Z_A \cos(\theta-\alpha)}$$

at the end with outgoing power or

$$\frac{V\cos\frac{\theta}{2}}{Z_B \cos\left(\frac{\theta}{2}-\beta\right)}$$

at the end with incoming power. Referring to Figure 12, with outgoing power

$$I_{12S} = -I_{31S} = \frac{3}{2}I_{1S} = \frac{3}{2}\,\frac{V\cos\frac{\theta}{2}}{Z\cos\left(\frac{\theta}{2}-\beta\right)}$$

The fault current is $\sqrt{3}V_S/2Z_{SB}$ from the sending end to a relay at B, and it is $\sqrt{3}V_R/2Z_{RA}$ from the receiving end to a relay at A. The total relay torque is $V_{pol}(I_S \cos\psi + I_F \cos\lambda)$ where V_{pol} is the polarizing potential of the unit, so that the relay trips when $I_S\cos\psi + I_F\cos\lambda$ is positive. The unit has maximum torque when the current is 30 degrees leading the polarizing potential, so that ψ or λ are 30 degrees less than the angle between I_S or I_F and the polarizing potential.

If one refers to Figure 13, treating counterclockwise angles as positive

$$\psi_{12} = \gamma - 60^\circ$$
$$\lambda_{12} = -90^\circ + \phi + \theta - \alpha - 60^\circ + 180^\circ$$
$$= \theta + \phi - \alpha + 30^\circ$$

Similarly

$$\psi_1 = 180^\circ + \gamma - 60^\circ + 90^\circ = \gamma - 150^\circ$$
$$\lambda_1 = 180^\circ + \phi + \theta - \alpha - 60^\circ + 90^\circ$$
$$= \theta + \phi - \alpha + 120^\circ$$

The 2–3 relay has no swing component, because $I_{2S} = -{}^1/_2 I_{1S}$; therefore $I_{2S} - I_{3S} = 0$. But

$$\lambda_{23} = 180^\circ - 90^\circ + \phi + \theta - \alpha - 60^\circ - 90^\circ$$
$$= \theta + \phi - \alpha + 120^\circ$$

Hence, with outgoing power, the relay will trip in any phase where one of the following expressions is positive:

Relay 1–2: $K\cos(\gamma - 60^\circ) + \dfrac{\cos(\phi+\theta-\alpha+30^\circ)}{Z_{RA}}$

Relay 2–3: $\dfrac{\cos(\phi+\theta-\alpha+120^\circ)}{Z_{RA}}$

Relay 3–1: $K\cos(\gamma --150^\circ) + \dfrac{\cos(\phi+\theta-\alpha+120^\circ)}{Z_{RA}}$

where

$$K = \frac{\sqrt{3}\cos\frac{\theta}{2}}{Z_S\cos\left(\frac{\theta}{2}-\alpha\right)}$$

Referring to Figure 13, in a similar manner, with incoming power, that is, considering the B relay

$$\psi_{12} = \gamma - 60^\circ$$
$$\lambda_{12} = 180^\circ - 90^\circ + \phi - \theta + \beta + 30^\circ - 90^\circ$$
$$= \phi - \theta + \beta + 30^\circ$$

Here again $I_{23S} = 0$ and

$$\lambda_{23} = -90^\circ + \phi - \theta + \beta + 30^\circ + 180^\circ$$

that is

$$\lambda_{23} = \phi - \theta + \beta + 120^\circ$$

Likewise

$$\psi_{31} = -180^\circ - \gamma + 30^\circ = \gamma - 150^\circ$$

and

$$\lambda_{31} = 180^\circ - 90^\circ + \phi - \theta + \beta + 30^\circ$$
$$= \phi - \theta + \beta + 120^\circ$$

Hence, the relay will trip in any phase where one of the following expressions is positive:

Relay 1–2: $K\cos(\gamma - 60^\circ) + \dfrac{\cos(\phi-\theta+\beta+30^\circ)}{Z_{SB}}$

Relay 2–3: $\dfrac{\cos(\phi-\theta+\beta+120^\circ)}{Z_{SB}}$

Relay 3–1: $K\cos(\gamma - 150^\circ) + \dfrac{\cos(\phi-\theta+\beta+120^\circ)}{Z_{SB}}$

where

$$K = \frac{\sqrt{3}\cos\frac{\theta}{2}}{Z_S\cos\left(\frac{\theta}{2}-\beta\right)}$$

The expression for phase 2–3 has no load term, because the load components in the faulted phases are in phase and hence cancel out. In the case of phase 1–2 with incoming power the swing component becomes positive and tends to trip when θ exceeds 90 degrees, and the fault component tends to trip when θ exceeds 45 degrees. In phase 3–1 the load component always tends to trip and the fault component to open the mho-unit contacts.

With outgoing power both the swing component and the fault component in phase 1–2 tend to prevent tripping. In phase 3–1 the swing component tends to trip and the fault current to prevent tripping up to $\theta = 90$ degrees.

The worst of these cases is the first one, phase 1–2 with incoming power. The Midian relay trips when there are 30 degrees between the station voltages, that is, $\delta = 30$ degrees, and $\theta = 48$ degrees. From a chart similar to Figure 6, $\gamma = 180$ degrees and $Z_S = 620$ ohms. This is checked as follows:

$$K\cos(\gamma-60^\circ) + \frac{\cos(\phi-\theta+\beta+30^\circ)}{Z_{SB}}$$
$$= \frac{\sqrt{3}\cos 24^\circ \cos(180^\circ - 60^\circ)}{620\cos 15^\circ} + \frac{\cos(72^\circ - 48^\circ + 8^\circ + 30^\circ)}{370}$$
$$= \frac{\sqrt{3}\times 0.914 \times (-0.5)}{620 \times 0.966} + \frac{0.47}{370}$$
$$= -0.00132 + 0.00127 = -0.00005$$

This shows that the actual point of failure is slightly more than $\theta = 48$ degrees. It is not worth while checking with a slightly larger value however, because only a slight oscillation would be necessary to reach the larger value.

During the system tests double-ground faults were applied on the 66-kv and 132-kv busses at each end of the line with both incoming and outgoing power up to 35,000 kva, and no wrong operations of the mho units were observed. It was not possible at the time to repeat these tests to check the calculated threshold values.

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Oil-Impregnated-Paper High-Voltage Condenser Bushings for Circuit Breakers and Transformers

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Synopsis: For many years shellac or synthetic-resin-coated paper has been used in winding condenser bushings. Two years ago work on the development of oil-impregnated-paper condenser bushings was undertaken. When a shortage of shellac was imminent because of the war, effort to complete the development was greatly increased, resulting in its being completed well ahead of the date originally set. Manufacturing processes and impregnating treatments were developed and bushings built and tested. The processes and treatments are described, and the data obtained from tests on representative bushings are given.

Complete interchangeability for transformer and circuit-breaker bushings has been obtained for current apparatus designs in the voltage ratings in which the bushings are produced. To a large extent, the new bushings are also interchangeable with bushings of older design in the same voltage rating. The entire insulating portion of the bushing is enclosed in porcelain and metal and this enables the bushing to be held in storage without special provisions for keeping it dry.

The oil-impregnated-paper condenser bushings, built for voltage ratings 92 kv to 230 kv, are susceptible to careful control of quality in the processing operations. Exceptionally consistent performance and high dielectric strength are obtained. Consistently low power-factor readings on both hot and cold bushings were obtained. Radio-influence voltage measurements were below proposed standards.

CONDENSER-TYPE high-voltage bushings 92 kv and above are being built with insulating cores of uncoated high-grade paper wound on the conducting stud and impregnated with low-viscosity insulating oil. Previously, the condenser elements were wound from paper coated with adhesive material such as shellac or one of the synthetic resins.

The condenser construction is old in the art of bushing manufacture. Its ability to withstand both current and voltage surges has been demonstrated amply by its use for more than 30 years in both high- and low-voltage bushings. Therefore, the theory of bushing-condenser design is not repeated in this paper.

The oil-impregnated-paper condenser bushings retain the known advantages of uniform voltage gradient, high-capacitance potential tap, mechanical strength, and small external diameter. From these features accrue other values such as small diameter and high efficiency of current transformers and the possibility of securing the maximum of interchangeability between both new and old circuit-breaker and transformer bushings. Complete interchangeability has been attained between transformer and circuit-breaker bushings of 92-kv and higher-voltage ratings in current production, and, although the new oil-impregnated bushings cannot be made interchangeable with all old bushings, they can replace older designs to a greater extent than has been possible heretofore.

The development of oil-impregnated condenser bushings, although accelerated by wartime conditions, has been planned for several years. Plastics and oils were studied. A low-viscosity oil was selected to prevent formation of voids within the condenser at extreme winter temperatures and to facilitate the impregnation process, and also because of a slight superiority in electrical properties of the impregnated paper.[4,7]

The insulation in condenser bushings under normal service voltage to ground, is worked at an average potential stress of less than 50 rms volts per mil. In the 60-cycle withstand test, the gradient is approximately 200 volts per mil. Under surge conditions, with a 1.5 by 40-microsecond full wave, at the basic impulse level, the stress may reach an average value of 450 crest volts per mil of insulation thickness for a few microseconds. Steep-front surge flashovers have been made in the high-voltage laboratory,[1–3] in which the average stress at the time of crest voltage was just under 1,000 volts per mil.

The voltage gradients in these bushings are relatively low compared with those in other applications of oil-impregnated paper insulation. J. B. Whitehead[4] has published accelerated-life-test results for voltage stresses starting at 400 rms volts per mil and increasing 3.12 per cent every four hours until failure. The average failures occurred at about 715 volts per mil, after about 75 hours, in samples comparable to a single layer or step of a condenser bushing wound with 0.005-inch paper. The bushing condensers have an added advantage of being wound from a wide sheet of paper instead of from narrow strips of tape (except that, in the larger sizes, the inner layers more than 100 inches long are helically wound from paper tape).

In paper-insulated oil-impregnated cables,[6] in the same standard voltage ratings as these bushings, the normal operating stress is approximately 140 rms volts per mil. Foust and Scott[7] show that such insulation breaks down at 1,300 to 2,000 crest volts per mil on impulse voltage tests using a 1.5-by-40-microsecond wave.

In oil-impregnated paper capacitors, using capacitor paper and thinner insulating walls, continuous 60-cycle rms gradients of 250 to 300 volts per mil have been used and intermittent d-c gradients up to 1,000 volts per mil.

This experience and test data, substantiated by other laboratory tests omitted for brevity, indicate a large margin of insulation strength in the condenser, even when stressed with the

Table I

Gap	No. of Shots	Corrected Kilovolts	Time to Flashover (Microseconds)	Average Rate of Rise (Kilovolts Per Microsecond)	Effective Rate of Rise (Kilovolts Per Microsecond)
33-inch rod	20	982 980	0.94 0.91	1,045 1,070	1,760 1,630
42-inch rod	20	1,080	1.6	675	1,080
46-inch experimental	100	1,142 1,135	1.84 1.75	620 650	1,220 1,250
42-inch rod	20	1,567	0.70	2,240	2,340
42-inch rod	20	1,745	0.61	2,860	3,160

Flashovers occurred sometimes on the gap or bushing alone, but generally the flashover occurred on both bushing and gap.

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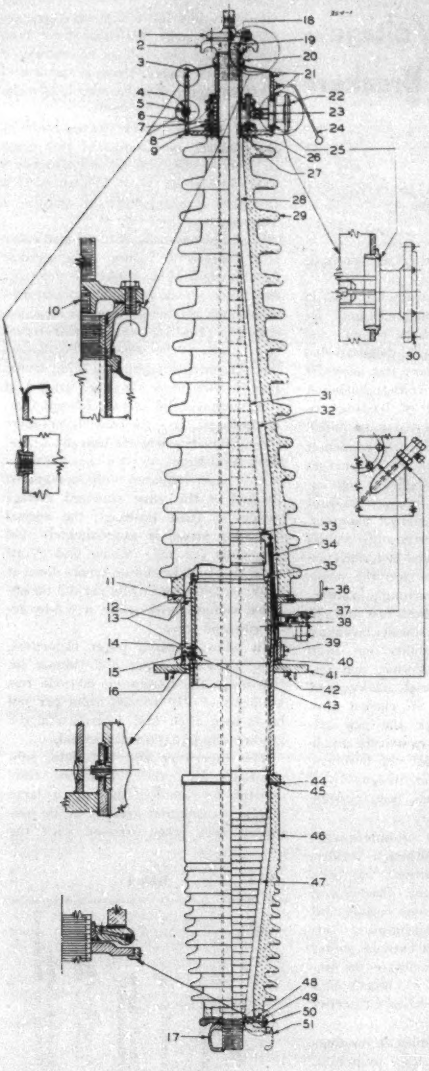

Figure 1. Typical outline of oil-impregnated condenser bushing

1—Adapter cap
2—Nut with lifting lugs
3—Spun copper. Differential expansion taken up by flexing
4—Brazed
5—Pipe plug soldered over
6—Steel shell
7—Gauge for determining spring setting
8—Coil-spring assembly places pressure on all gaskets for upper and lower porcelains. Six springs
9—Welded
10—Connection for fished-through lead for transformer
11—Asbestos Neoprene gasket
12—Grounding straps
13—Cork Neoprene gaskets
14—Stop to limit vertical movement and prevent rotational movement of condenser
15—Low-temperature solder
16—Cork Neoprene gasket for transformer
17—Ball static shield for transformers only
18—Cork Neoprene gasket
19—Low-temperature solder
20—Thimble
21—Brazed
22—Removable magnetic oil gauge—four-inch dial
23—Oil level at 75 degrees Fahrenheit
24—Gap electrode
25—Low-temperature solder—can be unsoldered to remove gauge
26—Asbestos Neoprene gasket
27—Cork Neoprene gasket
28—Upper oil chamber
29—Upper porcelain
30—Gauge dial and pointer assembly can be easily replaced
31—Copper foils
32—Oil-impregnated condenser (oil-filled Kraft paper)
33—Voltage tap band
34—Voltage tap connector
35—Ground foil band
36—Gap electrode
37—Voltage tap socket
38—Porcelain
39—Name plate
40—Sampling valve and tubing
41—Steel flange
42—Bevel ring and asbestos Neoprene gaskets for circuit breaker
43—Small hole—connecting passage between upper and lower oil chambers
44—Asbestos Neoprene gasket
45—Cork Neoprene gasket
46—Lower porcelain
47—Lower oil chamber
48—Cork Neoprene gasket
49—Asbestos Neoprene gasket
50—Drain plug
51—If this bushing is used as replacement of different style bushing, use the static shield or ball from the replaced bushing

most severe surges where the average gradient was 1,000 volts per mil.

Successful production of oil-impregnated-paper bushing insulators requires carefully developed and controlled manufacturing processes and treatments. At the same time, this construction is responsiv[illegible] careful control and treatment during production, resulting in a more uniform product than has been obtained heretofore. A large part of the equipment previously used for winding condensers from coated paper has been adapted, with minor changes, to production of the oil-impregnated condenser. Equipment for vacuum drying and degassing the bushings has been specially developed for this purpose. Oil conditioning and handling equipment likewise is new (Figure 2).

Bushing Construction

Figure 1 shows typical construction of the oil-impregnated condenser bushings, which are built in ratings of 92 to 230 kv inclusive. The conductor is a copper tube upon which the paper condenser is wound and serves as a stay bolt holding the entire assembly together under pressure of compression springs in the cap.

The stud or lead (the copper tube) is prepared by straightening, shot blasting, and coating with an adhesive material. Bare Kraft paper 0.005-inch thick, having a density of 0.82 maximum–0.67 minimum is wound on the stud to the specified diameter. During winding, at precalculated diameters, coated copper-foil sheets are inserted to provide a series of concentric condensers. Each foil is coated on both sides with thermoplastic adhesive which binds the foil to the adjacent layers of paper, and the condenser at intervals of approximately every quarter inch increase in diameter. When the specified number of condenser layers has been wound, a layer of paper treated with adhesive is wound on the outside to bind the condenser structure securely.

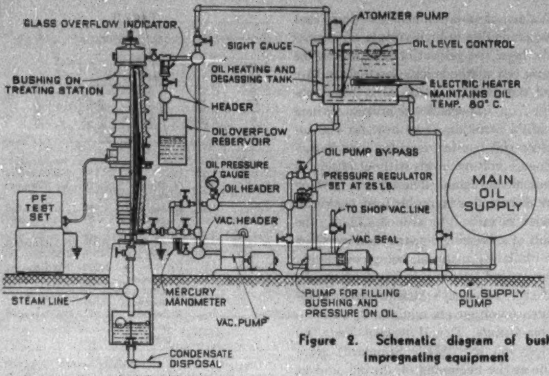

Figure 2. Schematic diagram of bushin impregnating equipment

During the winding operation, the paper is heated to 165 degrees centigrade ±5 degrees centigrade and rolled under tension and pressure. It is then machined in the same manner as the shellac Micarta condenser which it supersedes.

A steel sleeve is pressed on the machined condenser and connected electrically to the outer foil. This sleeve provides a means for attaching and mounting the condenser to the bushing flange, allowing limited movement longitudinally and rotationally between the condenser and the flange, as required for building and for differential expansion and contraction. A fabricated steel flange with provision for a potential tap insulator and oil-sampling valve supports the porcelains which encase both the lower and upper ends of the condenser. Gasketed joints with mechanical stops are provided between the porcelain and metal members. The pressure on all gaskets is equal and is obtained from springs within the cap. The condenser, being permitted to move longitudinally through the flange, within controlled limits, permits use of the same springs to provide pressure on all of the gaskets. All joints in the enclosing parts of the bushing, except the gasketed joints adjacent to the porcelains, are made by silver-alloy brazing or by soldering.

It was found during the impregnating period, when bushings are subjected to hot oil under pressure for several days, that many castings would show leaks, even several days after the treating operation began. Therefore, all metal parts of the bushing which form oil seals are now made from sheet or fabricated material. The insulating condensers are enclosed completely in metal and porcelain so as to remove all possibility of moisture absorption at any time. Oil-level indication is provided by a magnetic-type gauge soldered into the side of the cap. A valve is provided in the flange for obtaining oil samples, if required, and removable plugs in the bottom and top of the bushing permit draining or refilling.

Processing of Bushings

The assembled bushing is sealed and tested for leaks with an internal air pressure of 20 pounds per square inch and the complete bushing immersed in water for 30 minutes. During the test there must be no evidence or appearance of bubbles caused by escaping air. To expedite drying during the vacuum processing, the bushing is baked for approximately 12 hours at 95 degrees centigrade ± 5 degrees centigrade immediately before being connected to the treating system. During processing, the bushing is mounted on a support as shown in Figure 2, and steam at 120 degrees centigrade (approximately 14 pounds per square inch gauge pressure) is admitted to the inside of the copper tube on which the bushing is wound. The top end of the tube is closed and sealed. The steam gives off heat to the bushing, and condensation occurs within the copper tube and is removed from the system through a suitable trap. Connection is made to the vacuum system at both the top and bottom ends of the bushing. An absolute pressure of less than 5 millimeters of mercury is obtained within the bushing. The drying treatment is continued under high temperature and low pressure until degassing is complete and the power factor does not exceed 0.004. When this condition is obtained, a low-viscosity insulating oil, previously conditioned by heating and degassing, is admitted to the bushing at the lower end and permitted to rise until the bushing enclosure is filled completely. Pressure is then applied to accelerate complete saturation of the condenser with oil. Saturation is tested by reading the capacitance of the bushing with a Schering bridge and noting when no further increase occurs. It has been found that the fully treated bushing capacitance increases approximately 44 per cent over the dry bushing capacitance.

Mechanical Tests

Mechanical tests were made on a sample bushing to demonstrate ability to withstand handling, transportation, and service abuses. They are reported in detail in the appendix.

Electrical Tests

Electrical tests made on bushings of two voltage classes are reported in this paper. The first was a 196-kv 1,200-ampere experimental bushing. This bushing differed from standard in two respects:

1. There was no potential tap.
2. The lower porcelain was nine inches shorter than standard.

The short porcelain was used to avoid delaying tests while waiting for the standard porcelain of the new design to be manufactured.

The second bushing tested and described in this paper was rated 138 kv, 1,200 amperes. It was of standard design and materials in all respects.

Test results are given on these two ratings of bushings, because 138 kv is the most widely used transmission voltage, and 196 kv bushings are used on 230/196-kv apparatus where they are subjected to the 230-kv system voltage.

Electrical tests were made to determine if the bushings would meet all AIEE, National Electrical Manufacturers Association, or other electrical requirements. After these requirements were satisfied successfully, tests were continued for the purpose of obtaining information on the reserve insulation strength. An unusually large reserve was shown in both bushings, as indicated in the complete test results given in the appendix, but summarized in the following paragraphs.

196-Kv Bushing Tests

Before making high-potential tests, power-factor and capacitance measurements were made at room temperature

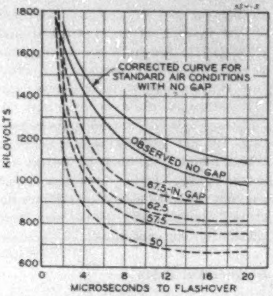

Figure 3. 196-kv experimental bushing flashover curves

Standard rod gap in parallel, 1.5x40-microsecond positive wave

Approximately 40 flashovers for each curve observed

Weather	Barometer (Inches)	Temperature (Degrees Fahrenheit) Dry	Wet	Relative Atmospheric Density	Humidity	Humidity Correction Factor	Total Correction Factor
50-inch gap	29.26	66	52	1.008	0.238	1.093	1.084
57.5-inch gap	29.16	76	56	0.980	0.235	1.093	1.115
62.5-inch gap	29.10	82.5	59	0.965	0.248	1.088	1.127
67.5-inch gap	29.10	70	53.5	0.985	0.233	1.093	1.109
No gap	29.24	86	62.5	0.965	0.313	1.071	1.109

(25 degrees centigrade) and at elevated temperature (the entire bushing heated to 80 degrees centigrade). The readings were:

25 degrees centigrade—power factor = 0.0049, capacity = 366 micromicrofarads

80 degrees centigrade—power factor = 0.0076, capacity = 371 micromicrofarads

Comparative power-factor and capacitance measurements were made up to 200 kv, using a high-voltage Schering bridge (see appendix). The readings taken before and at intervals during high-voltage testing showed that the curve of power factor and capacitance versus voltage was practically flat, and also that the bushing condenser was not being damaged appreciably by the tests.

Radio-Influence Voltage measurements were taken at 10-kv steps from 160 kv to 200 kv. No measurements were above 25 microvolts. The same values were read on the test circuit alone and with the bushing connected to the circuit. It therefore seems obvious that the value of radio-influence voltage of the bushing was below that of the circuit alone. At the conclusion of all high-voltage tests, after both porcelains had been destroyed and the condenser badly damaged, readings were taken up to 150 kv. No reading exceeded 150 microvolts.

Values of radio-influence voltage that are permissible in high-voltage apparatus have been the subject of study for some years; test methods have been agreed to; and considerable data have been obtained on important apparatus. There is still a question as to how far the industry can go in requiring low radio-influence voltage levels without imposing economic hardship. The form of bushing here discussed is favorable from the point of view of radio interference. This is the result of a bushing construction in which the insulating material, being completely impregnated with oil, is free from voids, and the discharges which produce radio-influence voltage are minimized.

One-Minute Dry Withstand Tests, 60 cycles, 465 kv (AIEE Standard) were made at the beginning of, during, and at the completion of the high-potential test program. The bushing did not give any indication of distress, although both porcelains were broken and considerable damage done to the condenser before the last withstand test was made.

The Standard Impulse Test for bushings of this voltage rating is three shots with a 1.5 by 40-microsecond full wave having a crest value of 900 kv. After the bushing was subjected to the standard test, additional impulse tests were made with positive and negative 1.5x40-waves having crest values ranging from 680 kv to 1,800 kv. A total of 203 impulses was imposed on the bushing. The values provided volt-time impulse-flashover curves shown in Figure 3. Each curve represents approximately 40 shots. In this set of tests, gaps were used in parallel with the bushing for the first 160 tests, as indicated in Figure 3. Forty tests were then run without a gap. In Figure 3 these tests are marked "no gap." When corrected for standard air conditions, the actual impulse-flashover values are indicated by the curve in Figure 3 marked "corrected curve for standard air conditions with no gap." Three 1.5 by 40-microsecond negative full-wave impulse tests were made. The third test broke a skirt of the lower porcelain. To conserve the porcelains for additional testing, gaps were again installed, and 77 steep-front impulse tests were made having crest values from 1,178 kv to 1,850 kv. During this series of tests, both porcelains were broken.

The condenser was apparently unharmed at the end of the tests just described. It was then removed from the porcelain enclosure and left standing with the upper end exposed to air from Friday

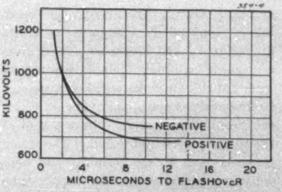

Figure 4. 138-kv bushing flashover curves with 43.2-inch standard rod gap in parallel, 1.5x40-microsecond positive and negative waves

Type-O condenser bushing

Weather	Barometer (Inches)	Temperature (Degrees Fahrenheit) Dry	Wet	Relative Atmospheric Density	Humidity	Humidity Correction Factor	Total Correction Factor
Positive	29.34	85	74	0.965	0.732	0.975	1.012
Negative	29.34	85	74	0.965	0.732	0.975	1.012

Table II

End Dropped	Drop Distance (Inches)	Position of Potential Tap	Number of Drops	Remarks
Top	$1^1/_8$	Up	105	No damage*
Top	$^5/_8$	Up	1,100	No damage†
Top	$^5/_8$	60 degrees clockwise	1,000	No damage†
Top	$^5/_8$	60 degrees counter-clockwise	1,000	No damage†
Bottom	$^5/_8$	60 degrees clockwise	1,000	No damage‡
Bottom	$^5/_8$	Up	1,000	No damage‡
Bottom	$^5/_8$	60 degrees counter-clockwise	3,000	No damage‡
Top	$^5/_8$	60 degrees counter-clockwise	2,000	No damage†

* Drop of $1^1/_8$ inch unjustifiably severe; $^5/_8$-inch drop comparable with similar tests previously made on samples of earlier types of bushings.

† Bottom end rebounded about $^1/_8$-inch high after each drop of top end.

‡ Top end rebounded twice (first bounce $^3/_{16}$ inch) at each drop of bottom end. Bottom end also rebounded slightly.

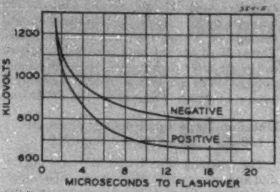

Figure 5. 138-kv bushing flashover curves with 46-inch special gap in parallel, 1.5x40-microsecond positive and negative waves

Type-O condenser bushing, $4^{1}/_{4}$-inch diameter toroid gap

Weather	Barometer (Inches)	Temperature (Degrees Fahrenheit)		Relative Atmospheric Density	Humidity	Humidity Correction Factor	Total Correction Factor
		Dry	Wet				
Positive	29.36	83	$71^{1}/_{2}$	0.970	0.656	0.990	1.031
Negative	29.32	81	74	0.972	0.770	0.969	0.996

night until Monday morning. The entire condenser was then immersed in a large tank of oil and given 40 impulse shots with a 67.5-inch gap in parallel. Voltages having a crest value of 1,880 kv were attained. This set of tests was followed by 25 steep-front impulses having a crest value of 1,900 kv. At the end of this series of tests, damage was indicated in 18 of the 30 steps in the condenser which, however, successfully withstood a 465-kv 60-cycle test for one minute. More detailed information is given in the appendix.

138-Kv Bushing Tests

Power-Factor and Capacitance measurements were made at 13.8 kv with the inverted Schering bridge. The power factor was 0.0037, and the capacitance was 399 micromicrofarads. High-voltage power-factor tests and capacitance measurements were made with a laboratory-type high-voltage Schering bridge. These measurements began at 47 kv and ended at 150 kv. The power factor at 47 kv was 0.0035, and the capacitance was 362 micromicrofarads. At 150 kv, the power factor was 0.0039, and the capacitance was 362 micromicrofarads. The high-voltage Schering bridge tests were repeated several times during the series of 60-cycle and impulse tests. No variation in these values occurred throughout the series of tests, indicating that the condition of the bushing at the end of the tests was substantially the same as at the beginning.

Radio-Influence Voltage measurements were made at several voltages up to 150 kv. No reading exceeded 150 microvolts, indicating that the radio-influence voltage was well within any limits proposed for bushings of this voltage rating.

Dry Flashover Tests. After the standard AIEE one-minute dry test (335 kv), had been made, it was decided to develop the dry flashover strength of the bushing. Ten flashovers were made from 403 kv to 438 kv.

Wet 60-Cycle Flashovers were made following the ten-second wet withstand test of 275 kv. Ten flashovers were made with voltages ranging from 340 kv to 369 kv.

Volt-Time Impulse Tests were made at the standard value of 650 kv with a 1.5x40-microsecond full wave. To obtain further data on the actual insulation strength of the bushing, additional impulse tests were made with positive and negative waves. A total of 160 positive and negative wave impulses were made with a 1.5 by 40-microsecond wave, both full and chopped, from the results of which Figures 4 and 5 were drawn. These tests were followed by 180 steep-wave-front flashover tests. Crest voltages ranged from 982 kv to 1,745 kv. Additional data on this series of tests are given in the appendix.

The insulation of the bushing was indicated to be unharmed at the end of this series of tests, and the bushing was taken to another laboratory for mechanical tests which are described in the appendix.

Conclusions

High-voltage outdoor-type condenser bushings have been built in which the insulation is oil-impregnated paper. The bushings are usable and interchangeable on either circuit breakers or transformers of current design and in nearly all cases can be used to replace bushings of earlier design. The bushings are assembled completely and treated in the factory, then permanently sealed before shipment. They are designed to pass through bushing-mounting flange openings and current transformers without the dismantling of any parts, since the entire condenser structure is hermetically sealed in metal and porcelain. No special storage requirements for protection against weather or atmospheric conditions are required. The design provides for emergency operation of a bushing over a considerable period of time, even though one of the porcelain casings might be broken or destroyed. Electrical tests required by the industry have been met with a wide margin of safety. Very high puncture strength has been demonstrated by the fact that no damage was done to the insulating members over a long series of electrical tests until after the enclosing porcelains had been completely destroyed. Power-factor measurements at both room temperature and elevated temperature are very low. Radio-influence voltage measurements were well within limits proposed.

The tests reported in this paper were made on new bushings under good conditions. Bushings in service cannot be kept clean at all times and should not be expected to maintain continuously the high values obtained in these tests.

Since 1936 the Detroit Edison Company has rebuilt a large number of 132-kv condenser bushings using the old condensers. Before the rebuilding, the varnish was removed, and the condensers were impregnated with insulating oil. The satisfactory service record of these rebuilt bushings, with the limitations of old materials, is convincing evidence that the use of oil-impregnated paper condensers is a valuable improvement in bushing construction.

While the construction and processing of the rebuilt bushings and those described in this paper are not identical, the authors received valuable encouragement and stimulation from the early work done by the Detroit Edison Company engineers in rebuilding bushings.

Appendix

Detailed Report of Special Tests on 196-Kv Experimental Bushing

Test 1—High-Voltage Schering-Bridge Tests at 200 Kv. Comparison standard capacitor comprising a stack of coupling capacitors, as read by 13.8-kv inverted Schering bridge; 0.0024 power factor, 642 micromicrofarads.

Apparent power factor and capacitance of bushing, mounted in large closed-top testing tank of oil, at 200 kv, assuming comparison standard power factor and capacitance to remain constant: 0.00305 power factor, 297.5 micromicrofarads.

This reading was rechecked, with insignificant small variations, at frequent intervals during the surge-testing program, and this served to prove that no appreciable damage was being inflicted on the bushing condenser by the tests.

This reading neglects the unknown difference between the capacitance of the comparison standard as read by the inverted Schering bridge and as used in the conventional high-voltage bridge circuit, because of the external capacitance to ground being included in the former and excluded in the latter.

Test 2—Radio-Influence Voltage Measurements to 200 Kv. Test made with standard radio-influence measurement circuit,[8,9] using 220-kv coupling capacitor.

Applied 60-Cycle Kilovolts	160	170	180	190	200
Microvolts, with bushing	10	25	15	20	15
System, without bushing	10	25	15	20	15

Test 3—Dry Withstand Test, 465 kv, 60 cycles, one minute—successful. (Schering-bridge check test at 200 kv.)

Test 4—Impulse-Flashover Curve, kilovolts versus microseconds 1.5 by 40 microseconds; positive wave, 50-inch standard rod gap in parallel. 680 kv to 1,150 kv at 2 microseconds. See Figure 3 (40 shots)—successful. (Schering-bridge check test at 200 kv.)

Test 5—Impulse-Flashover Curve, kilovolts versus microseconds 1.5 by 40 microseconds; positive wave, 57$^1/_2$-inch standard rod gap in parallel. 770 kv to 1,800 kv at 1.2 microseconds. See Figure 3. (40 shots)—successful.

Test 6—Impulse-Flashover Curve, kilovolts versus microseconds 1.5 by 40 microseconds; positive wave, 62$^1/_2$-inch standard rod gap in parallel. 820 kv to 1,650 kv at 2 microseconds. See Figure 3. (40 shots)—successful.

Test 7—Impulse-Flashover Curve, kilovolts versus microseconds 1.5 by 40 microseconds; positive wave, 67$^1/_2$-inch standard rod gap in parallel. 900 kv to 1,650 kv at 2 microseconds. See Figure 3 (40 shots)—successful. (Schering-bridge check test at 200 kv.)

No damage of any kind to the bushing resulted from any of the foregoing tests.

Test 8—Impulse-Flashover Curve, kilovolts versus microseconds 1.5 by 40 microseconds; positive wave without gap in parallel. Approximately 40 shots. 940 kv to 1,770 kv at 0.75 microsecond. See Figure 3—successful. (Schering-bridge check test at 200 kv.)

In test 8, a small piece was broken off the edge of one skirt near the middle of the weather casing.

Test 9—Impulse-Flashover Curve, kilovolts versus microseconds 1.5 by 40 microseconds; negative wave without a gap in parallel—test not completed. (Schering-bridge check test at 200 kv.)

After three shots, a skirt of the lower porcelain was broken by flashover in the tank oil, and negative-wave flashover tests were discontinued. The standard porcelain for this 196-kv bushing is 36$^1/_4$ inches long, but to avoid waiting for manufacture of the correct porcelain, this experimental bushing had been built with a porcelain 27$^1/_8$ inches long. Therefore, performance of this lower porcelain was not pertinent. With the broken lower porcelain, the testing was resumed.

Test 10—Steep-Front Impulse Test, positive wave, 63-inch standard rod gap in parallel. Ten shots 1,178 kv (uncorrected). Effective rate of rise kv per microsecond = 870. Flashed on gap.

Test 11—Same as Test 10 except 1,709 kv (uncorrected), and effective rate of rise = 3,200 kv per microsecond. Ten shots. Eight shots flashed gap and two shots flashed both gap and bushing.

Test 12—Same as Test 11 except 1,850 kv (uncorrected)and 3,360 kv. Per microsecond effective rate of rise. 57 shots. All shots flashed both gap and bushing.

Noises were heard in the test tank on 36th and 37th shots, indicating further shattering of the previously broken lower porcelain. Bridge readings were taken and found to be normal. Testing was resumed on the following day. On the 56th shot, a small piece was broken off the top end of the top porcelain. The 57th shot was taken, and a large piece was knocked out of the upper porcelain extending down to eighth skirt from the top. Apparently the bushing had lost oil because of breakage of lower porcelain, since none was found in weather casing when it broke.

The bushing was temporarily lifted above the test tank for inspection, and the lower porcelain was found badly shattered.

Bridge readings were taken at 200 kv and indicated the condenser to be unharmed. (0.0034 power factor; 297.5 micromicrofarads.)

The bushing was removed from the test tank.

The porcelains and flange were completely removed and the condenser examined. Slight markings were observed on the surface of the lower end, indicating that after the lower porcelain was broken, at least one flashover had occurred over the surface of the lower end of the condenser.

The unprotected condenser was left standing from Friday night until Monday morning with the upper end exposed to air, the lower end, from the normal flange location down, was immersed in oil. Without further treatment, test 13 was made.

Test 13—The condenser was suspended on a string of strain insulators in a 20-foot-diameter oil tank with the outer foil grounded. To reduce voltage disturbances over the surface of the oil, the cap was used as a shield on the upper end of the stud, about half of the cap being under oil. The condenser was given the one-minute dry 60-cycle withstand test of 465 kv. Results—successful.

Test 14—Still in oil tank, as in test 13, an impulse curve, kilovolts versus microseconds, to flashover was run with a 1.5 by 40-microsecond positive wave and a 67$^1/_2$-inch rod gap in parallel to 1,880-kv crest. About 40 shots were taken. The curve corresponded closely to the curve for the 67$^1/_2$-inch gap in Figure 3. Results—successful.

Test 15—With the same setup as for test 14, 25 shots were taken with a positive wave rising to 1,900-kv crest. Effective rate of rise 3,360 kilovolts per microsecond. During the series, some bubbles rose, indicating breakdowns were occurring either over or through the condenser insulation. On the 21st to 25th shots, the gap did not flash. Inspection showed 18 of the 30 steps in the condenser damaged.

Test 16—The condenser was again suspended in the tank as for tests 14 and 15. 465 kv, 60 cycle was applied for one minute. The badly damaged condenser passed the regular 60-cycle withstand test.

Test 17—The condenser was unwound, and all the breakdowns were observed to have occurred at the ends of the copper foil cylinders, some at the top and some at the bottom end. Each breakdown burned through or flashed over one to three steps. The ends of the bushing had been exposed to the air after the removal of the porcelain casings. The paper was found thoroughly saturated with oil throughout the condenser.

Detailed Report of Special Tests on 138-Kv 1,200-Ampere Bushing

The bushing tested was of standard design and materials in all respects. The processing equipment for production was not completed, and processing was done on experimental equipment. Tests as described in the following were made successfully:

Test 1—Bushing Mounted in Test Tank, room temperature:

(*a*). 13.2-kv inverted Schering bridge	= 0.00375 power factor
Capacitance	= 399 micromicrofarads
(*b*). High-voltage Schering bridge 47 kv	= 0.0035 power factor
Capacitance	= 362 micromicrofarads
(*c*). 150 kv	= 0.00395 power factor
Capacitance	= 362 micromicrofarads

Test 2—Dry 60-Cycle Flashovers (corrected to standard atmospheric conditions):

438 kv—411 kv—438 kv—432 kv—418 kv
426 kv—430 kv—416 kv—403 kv—410 kv

Test 3—Wet 60-Cycle Flashover (corrected kilovolts):

340 kv—353 kv—358 kv—350 kv—357 kv
362 kv—369 kv—357 kv—353 kv—359 kv

Precipitation—0.2 inch per minute
Water pressure—40 pounds per square inch
Resistivity—7,000 ohms per inch cube

Test 4—Schering-Bridge Test to 150 kv.

Test 5—Time to Flashover Impulse Curve with special toroid gap set at 46 inches in parallel, to flash at 670 kv, 1.5 by 40-microsecond positive wave—40 shots up to 1,200 kv. See Figure 5.

Test 6—Time to Flashover Impulse Curve with standard rod gap set at 43.2-inch (700 kv) 1.5 by 40-microsecond positive wave, 40 shots up to 1,200 kv. See Figure 4.

Test 7—Same as Test 5 except with negative wave. See Figure 5.

Test 8—Same as Test 6 except with negative wave. See Figure 4.

Test 9—Schering-Bridge Test at 150 kv indicated no damage.

Test 10—Steep Wave-Front Flashover Tests. For typical oscillograph data see Table I.

Test 11—Schering-Bridge Tests up to 150 kv, together with inspection, showed the condition of the bushing to be the same as at the beginning of the test program.

Drop Tests on 138-Kv 1,200-Ampere Bushing

Bushing horizontal, supported on fulcrum at one end and motor-operated lifting and dropping mechanism at other. Drops 21.5 times per minute (1,290 per hour). See Table II.

Elastic Bending Deflection of Bushing ($^5/_8$-Inch Drop)	At Top End of Flange	At Bottom End of Flange
When dropping top end	$^1/_8$ inch	$^1/_8$ inch
When dropping bottom end	$^3/_{16}$ inch	$^1/_8$ inch

Performance Requirements for Relays on Unusually Long Transmission Lines

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Synopsis: Analysis of fundamental bases of relaying made as part of the engineering for the 270-mile 154-kv interconnection between systems of Nebraska Power Company and Kansas Gas and Electric Company reveals that, of current, phase angle, impedance, and impedance angle, none alone offers satisfactory discrimination among open-circuit, short-circuit, and load conditions. Combination of input-impedance magnitude Z and impedance angle $\psi = \tan^{-1} X/R$ is studied by means of "impedance circle diagrams" of the interconnecting circuit. On these diagrams is charted the dynamic behavior of the interconnected systems during faults and surges of synchronizing power. The desirable areas of trip and no trip are mapped, and general specifications for the performance of relay systems are presented in terms of impedance magnitude and angle, $Z \angle \psi$. Thereby full advantage can be taken of all possible synchronizing power inherently available over the interconnection and at the same time faults and approach of out-of-step condition can be recognized properly.

RE-EXAMINATION of fundamental principles of transmission-line relaying for phase protection and the development of advanced methods for calculating and specifying relay performance for long lines and interconnections have been included in the engineering of a 270-mile 154-kv interconnection between systems of Nebraska Power Company and Kansas Gas and Electric Company.[1] This has led to development of new long-line-type relays described in a companion paper.[2] Installation and test of these relays under conditions of actual operation are described in another companion paper.[3]

The complete relay requirements for this interconnection are quite special, because the transmission line is unusually long; transformers are connected as parts of the line between terminal breakers, and automatic instantaneous reclosure of circuit breakers is employed to maintain synchronism after faults without benefit of other parallel ties.

Bases for Relaying

As introduction to the problems of relaying this transmission circuit, some of the principal bases of modern relaying and their applicability to long-line interconnections are reviewed.

CURRENT MAGNITUDE AND PHASE-ANGLE BASES

Figure 2 indicates as a function of length the magnitude of current which may be expected at the sending end of an open-wire transmission line with construction suitable for 154-kv 60-cycle operation for open circuit, for a representative load, and for short circuit. Study of these curves reveals that there is little or no hope of attaining discrimination between load and short circuit by measurment of current magnitude on lines much in excess of 100 miles in length. This virtually rules out consideration of overcurrent relays for long lines. On the 270-mile line under study, there would be absolutely no margin since the current on load swings exceeds the short-circuit current for faults at the far end of the line. It should be noted also that the line-charging current is nearly equal to the minimum short-circuit current.

In this discussion, a representative load is assumed to be an impedance equal to the characteristic impedance or surge impedance of the line,[4] $Z_0 = \sqrt{z/y}$. However, as explained in the paragraphs which follow, momentary requirements for transfer of synchronizing power between large systems easily may result in tie-line current considerably greater than surge-impedance load and even in excess of far-end short-ciacuit current.

The phase-angle function in transmission-circuit performance and its relation to relaying can be studied conveniently by beginning with the power circle diagram representing power input to the circuit terminal at the relay location. This diagram is plotted in Figure 3 from the sending-end power equation 1

$$P_s = \frac{E_s^2 \cdot D}{B} \underline{/(\Delta - \beta)} - \frac{E_s E_r}{B} \overline{/(\delta_{rs} + \beta)}$$

The form of this equation, and the notation used, are explained fully in Appendix I.

The first term of this equation depends only upon the circuit constants and the voltage E_s at the near end or sending end

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Deflection Tests of 138-Kv 1,200-Ampere Bushing

Bushing mounted vertically by its flange. Sidewise stress applied, and deflection measured to left and to right, seven times each, first at top end and then at bottom end of bushing. Deflections apparently were entirely elastic up to 220 pounds maximum applied.

Deflection per 100 pounds side stress:
Top end—0.00625 inch. Bottom end—0.0052 inch.

Vibration Test

Motor with flywheel unbalanced 0.474 pound-foot, attached to top of bushing, was run 84 hours at 1,300 rpm, exerting a centrifugal force of 273 pounds, vibrating the top of the bushing to a deflection of approximately 0.014 inch from the center line. No oil leak or other damage resulted.

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of the circuit and defines the location of the center 0′ of the power circle. The second term defines the magnitude and direction of the radius of the power circle.

When $E_r<DE_s$, the power circle radius E_sE_r/B becomes less than E_s^2D/B, which is the distance 0–0′ of the center to the origin, then the steady-state side of the locus P_s lies between the origin 0 and the center of the power circle 0′. With these conditions, the power-factor angle θ lies in the same general direction at normal loads as it does for short circuits on the line.

A numerical example will make this effect clear. For a line about 150 miles long, of uniform symmetrical construction and without terminal transformers, $D=A=0.95$ (see Figure 8). Assume first that $E_r=DE_s$, which means that far-end or receiving voltage is 95 per cent of the near-end or sending voltage. This is a normal condition. The power circle for this condition passes through the origin of the diagram ($P=0$, $Q=0$). For zero power flow ($P=0$), the power-factor angle θ is indeterminate. For small flow of power from the bus to the line ($P>0$), θ is nearly equal to $90-\beta+\Delta$ degrees. For a small reverse flow of power ($P<0$), θ is slightly greater than $270-\beta+\Delta$ degrees. This is a change of nearly 180 degrees in the value of θ for a very small change in load over the line which may be a tie between two systems operating in synchronism.

Next consider what happens upon a change in the relative voltage levels at the two ends of this same line. Assume now that $E_r=0.90E_s$. Since $E_r<DE_s$, the power circle for this condition crosses the vertical axis Q –Q *below* the origin. For condition of no power flow ($P=0$), the power-factor angle θ is 270 degrees, meaning that the near-end or sending system is supplying reactive volt-amperes ($-Q$) to the line and to the far-end or receiving system.

Assume last that $E_r=E_s$. Since $E_r>DE_s$, the power circle for this condition crosses the vertical axis Q –Q *above* the origin. For the condition of no power flow ($P=0$), the power-factor angle is 90 degrees, meaning that the near-end or sending system is obtaining reactive volt-amperes (Q) from the line equal to the charging kilovolt-amperes of half the line.

From this example, it may be seen that the power-factor angle θ may swing to any position within a complete circle of 360 degrees under normal load conditions. It therefore cannot be used alone as basis for discrimination between load and fault conditions.

To complete this analysis of phase angle, it is necessary also to investigate cases of short circuit, open circuit, and loads approaching stability limits.

Points corresponding to open circuit at the far end are not readily plotted on the near-end or sending power circle diagram until the sending-end power is determined from some other equation. The power for this condition is $P_s=E_s^2(C/A)$, which is the charging kilovolt-amperes of the circuit. For uniform open-wire lines, this falls in the first quadrant of the power circle diagram for all lengths up to $^1/_4$ wave length, or nearly 760 miles for 60-cycle power lines having low-resistance conductors. Thus the power-factor angle θ for this open-circuit condition on such lines is always less than 90 degrees and for most lines less than 600 miles long is very near 90 degrees.

For a short circuit at the far end of a circuit ($E_r=0$), the power $\boldsymbol{P}_s$ at the near end is found directly from equation 1,

$$\boldsymbol{P}_s=\frac{E_s^2\cdot D}{B}\ \underline{/(\Delta-\beta)}\ -\frac{E_s\cdot E_r}{B}\ \overline{/(\delta_{rs}+\beta)}.$$

Since $E_r=0$, the second term of this equation disappears, and the power is the first term which locates the *center* of the power circle 0′. Since all such centers for all values of E_s and for all lengths of uniform lines up to very nearly $^1/_4$ wave length, say to 700 miles for 60-cycle power lines having low-resistance conductors, fall in the fourth quadrant, the power-factor angle θ for short circuits on such lines is always between 270 and 360 degrees and is $\underline{/(\Delta-\beta)}$, corresponding to the length of line to the point of fault.

For analysis of cases near the stability limits of a circuit, it is necessary to refer again to the power circle diagram and power equation. For any pair of values of E_s and E_r, there is one near-end or sending power circle. The maximum power ($P_s$max) which can be put *into* the circuit from the near-end bus at these voltages is specified by a tangent to the power circle drawn to the right-hand side of, and parallel to, the Q –Q axis. The maximum power ($-P_s$max) which can be taken *from* the circuit into the same bus is specified by a similar parallel tangent drawn to the left-hand side of the Q –Q axis.

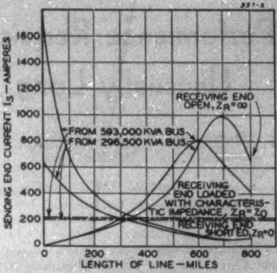


Figure 2. Current input to line from 154-kv bus as a function of length and far-end terminal load

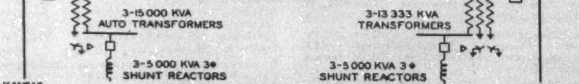


Figure 1. One-line diagram of interconnection

These two values of power ($P_s$max) and ($-P_s$max) are not steady-state power limits, as might be thought at first glance. The true steady-state power limits for an interconnection established by such a circuit would ordinarily be less than the values thus indicated, depending upon characteristics of the systems interconnected.[5] However, power loads of these magnitudes may be imposed momentarily on interconnection circuits during power swings following system disturbances. Figure 5 illustrates the moment-to-moment changes in angle between two systems and the two terminal voltages of the interconnecting circuit during a system disturbance as obtained from an a-c network analyzer.[6] The angle δ_{rs} is the link or factor connecting this figure with the power circle diagram, Figure 3. It is the angle between the terminal voltages E_r and E_s and must not be confused with either the phase angle θ between current and voltage or the impedance angle $\psi=\tan^{-1}(X/R)$, used in power and impedance diagrams respectively. This angle δ_{rs} is so important in the subsequent discussions that calibration scales of it are included in many of the figures, and diagrams are included for the purpose of identifying corresponding points as related by this angle throughout these discussions.

Referring again to the power limits on the power diagram, Figure 3, it is seen that on the upper or stable half of those

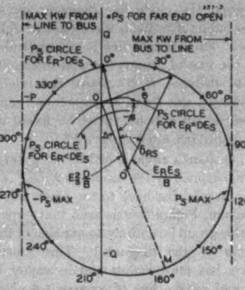


Figure 3. Construction of sending-end power circle diagram from equation 1

power circles which enclose the origin 0, that is, circles corresponding to $E_r > DE_s$, the phase angle θ swings continuously in one direction through more than 180 degrees as the power simultaneously swings through its full range from ($P_s$max) to ($-P_s$max). For fixed near-end voltage E_s and at one or both of the loads, ($P_s$max) and ($-P_s$max), the kilovolt-amperes and the current I_s measured at the near-end terminal of the circuit are greater than the kilovolt-amperes and current at the same terminal in case of short circuit at the far end.

The very wide range of power-factor angles θ and the lack of discrimination between magnitude of load and fault current previously discussed make overcurrent, directional, and volt-ampere relay elements unsuitable for the phase-to-phase protection of long lines when used alone, in combination with each other, or in combination with impedance-magnitude elements. This is likewise true for any tie circuit whether long or short, if the impedance of the tie is large compared with that of the systems. In other words, it is true in any case where the synchronizing power required to hold systems in step causes large angular separation (δ_{rs}) between terminal bus voltages.

INPUT-IMPEDANCE MAGNITUDE AND ANGLE

It is most helpful and enlightening also to look at the general transmission circuit from the impedance viewpoint, which is probably better understood by communication engineers than by most power engineers. The 60-cycle input impedances of uniform lines with a conductor size and spacing suitable for 154-kv operation are shown for all lengths from 0 to 800 miles in Figures 6 and 7. The three curves of Figure 6 give the magnitude of sending-end impedance with receiving-end open-circuited, loaded with surge impedance $\mathbf{Z}_0=\sqrt{z/y}$, and short-circuited respectively. The three curves of Figure 7 similarly give the angles $\psi=\tan^{-1}(X/R)$ of these same impedances.

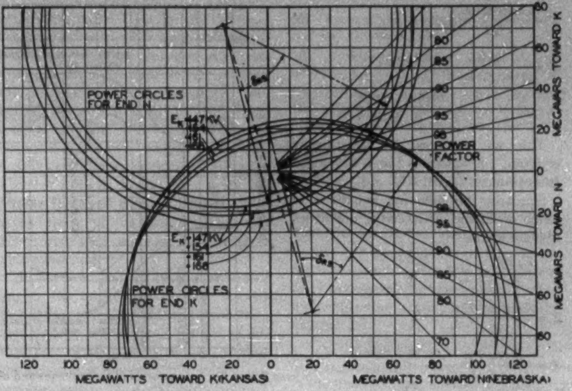


Figure 4. Power circle diagram of the 154-kv interconnection for condition $E_n = 154$ kv

Study of Figure 6 reveals that impedance magnitude alone, or measurement of ohms, does not give a good basis for successful discrimination between open, load, and short-circuit conditions close to the far ends of radial-feeder circuits having electrical length corresponding to more than about 200 miles of open-wire line. In contrast to this conclusion, study of Figure 7 shows that angle of the input impedance is radically different, depending upon far-end conditions. This suggests that measurement of *circuit-impedance angle*, $\psi=\tan^{-1}(X/R)$, should provide a basis upon which successful discrimination can be made between normal and fault conditions and upon which relays can be designed for long lines, possibly up to 600 miles in length. A little further consideration, however, indicates that this basis may also have limitations. The derivations of the impedance formula given in Appendixes II and III indicate that, for each point on the power circle diagram of Figure 3, there is a corresponding impedance magnitude and, further, that the impedance angle $\psi=\tan^{-1}(X/R)$ is the conjugate of the power-factor angle θ. We have already discussed limitations of power-factor angle θ as a relay quantity and, because of the conjugate relation, can infer that some of the same limitations might apply to the impedance angle ψ.

Combinations of impedance magnitude, impedance angle ψ, and impedance components present possibilities as bases for relaying which must be explored For analysis of these, it is necessary, or at least very desirable, to have circuit characteristics and performance completely expressed in chart form on resistance and reactance co-ordinates.[7]

Construction and Interpretation of Impedance Circle Diagrams

There are certain well-known advantages of impedance as a basis for relaying. It is independent of system voltage, and except in special cases, is independent of the number and location of generators in service, and is primarily dependent only upon circuit constants. Hence it was decided to study further the possible combinations of impedance magnitude and angle to determine the performance which would be required of impedance and reactance relay elements to protect properly the proposed circuit and to find what modifications, if any, would be necessary in conventional relay elements.

In this attack on the problem, it was desirable to obtain general formulas for the sending- and receiving-end impedances in terms of the *ABCD* general circuit constants and the sending and receiving voltages. With this goal in mind, the following equations for near-end or sending-end impedance in terms of the circuit constants and voltages were used:

$$P_s=\frac{E_s^2\cdot D}{B}\underline{/(\Delta-\beta)}-\frac{E_s\cdot E_r}{B}\overline{/(\delta_{rs}+\beta)}$$

(see Appendix I) (1)

$$\mathbf{Z}_s = \frac{(\text{conj } \mathbf{E}_s)\cdot\mathbf{E}_s}{\mathbf{P}_s} \qquad (2)$$

$$\mathbf{Z}_s = \frac{D\cdot B}{D^2-(E_r/E_s)^2}\underline{/(\Delta-\beta)} - \frac{B(E_r/E_s)}{D^2-(E_r/E_s)^2}\underline{/(\phi-\beta)} \qquad (3)$$

This was obtained by substituting equation 1 in equation 2 as is developed in Appendixes II and III.

Similarly the following equations in terms of far-end or receiving quantities also are useful.

$$\mathbf{P}_r = \frac{E_s\cdot E_r}{B}\underline{/(\delta_{rs}-\beta)} - \frac{E_r^2\cdot A}{B}\underline{/(\beta-\alpha)} \qquad (4)$$

$$\mathbf{Z}_r = \frac{(\text{conj } \mathbf{E}_r)\cdot\mathbf{E}_r}{\mathbf{P}_r} \qquad (5)$$

$$\mathbf{Z}_r = \frac{B\cdot(E_s/E_r)}{A^2-(E_s/E_r)^2}\underline{/(\phi+\beta)} - \frac{A\cdot B}{A^2(E_s/E_r)^2}\underline{/(\beta-\alpha)} \qquad (6)$$

This equation 6 is companion to equation 3 and is derived by the same method as given in Appendixes II and III.

Symbols appearing in the preceding equations and elsewhere in this paper are identified in Appendix VIII.

From a relay standpoint, equation 3 is of first interest, because it defines the impedance of a protected circuit as "seen" by relays at the near-end terminal of that circuit. Equation 6 is also of interest but is of less immediate significance, because a diagram plotted from equation 3 gives similar results.

Equation 3 when plotted on impedance co-ordinates is represented by a family of circles as in Figure 9. The mathematical relations between these circles and those of the power circle diagrams, Figure 3, are developed in the appendixes. The derivation of the impedance, equation 3, shows that the center of each circle representing a given ratio of terminal voltages E_r/E_s lies on line 0–0 (Figure 9) passing through the origin ($R=0$, $X=0$) at an angle$\underline{/(\beta-\Delta)}$ with the R–R axis. Five such circles are of particular interest in this discussion. These are the ones corresponding to two general and three special values of the ratio E_r/E_s, as follows:

$$E_s=0,\ \frac{E_r}{E_s}>D,\ \frac{E_r}{E_s}=D,\ \frac{E_r}{E_s}<D, \text{ and } E_r=0$$

The first of these circles which should be examined is one in which the near-end or sending voltage $E_s=0$. In this case ratio $E_r/E_s=\infty$. This corresponds to condition of short circuit at the location of the relays. Since impedance "seen" by the relays is $\mathbf{Z}_s=0$, the circle is a point which coincides with origin 0_1.

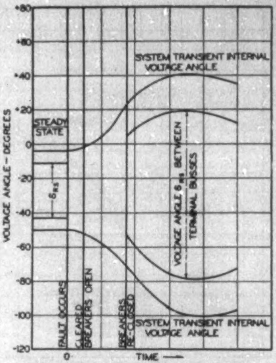


Figure 5. Swing curves of bus voltages and of equivalent internal voltages of generators during tripout and reclosure of interconnecting line

The second circle falls in the general group defined by condition $E_r/E_s>D$. (Referring back to the power circle diagram Figure 3, it is found that this condition defines power circles which enclose the origin.) By plotting one of this general group of circles, it is found that the center lies on the line 0–0 *below* the origin of the diagram, for example at 0_2. One particular case in this group of circles is that for which $E_r=E_s$. In this case, the distance from the center 0_2 to the origin of the diagram is $(D\cdot B)/(D^2-1)$ and the radius is $B/(D^2-1)$.

The third circle of interest is the special case in which $E_r/E_s=D$. In this case (reference to the power circle diagram, Figure 3, reveals that the corresponding power circle passes through the origin of the diagram indicating one point of zero power), the circle has diameter of ∞ and radius of ∞ and is therefore a straight line. This straight line is perpendicular to the line of centers 0–0 and intercepts it at distance $B/2D$ above the origin. However, this location cannot be found from equation 3 but must be found as explained in a later paragraph.

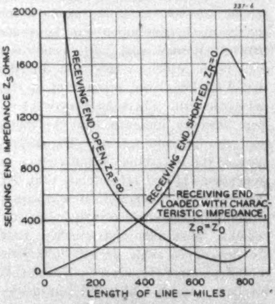


Figure 6. Magnitude of 60-cycle input impedance of a line suitable for 154-kv operation as a function of length and far-end terminal load

The fourth circle is one in the general group defined by the condition $E_r/E_s<D$. (Again referring back to the power circle diagram, Figure 3, it is seen that circles of this group lie entirely to one side of the origin of the diagram.) This fourth circle has finite radius and its center is located on the line of centers 0–0 *above* the origin, for example, at 0_4.

The fifth of these circles is the case where $E_r=0$, corresponding to short circuit at the far end of the line. This circle has no radius and is therefore a point. Its location is on the line of centers 0–0 at distance B/D upward from the origin at 0_5. For convenience this point may be called the far end of the circuit, and the origin 0_1 can likewise be called the near end.

In plotting these circles, the intersection of each with the line of centers 0–0 on the side nearest to the origin 0_1 is of greatest importance. Since these intersections are obtained from the impedance equation 3 as the difference between two quantities which may each be quite large, infinite in one case, it is often desirable to locate these intersections by other more accurate means and then to make the circles pass through these points by slight adjustments in location of the corresponding center or radius if necessary.

Referring back to the power circle diagram, Figure 3, the corresponding point in that diagram is located at M where power circle $\mathbf{P}_s$ crosses the line of centers 0–0′ extended. The power $\mathbf{P}_s$ at that point has scalar value equal to the *sum* of the radius, and the distance from the origin to the center 0–0′ which sum is $|P_s|_m=(E_s^2\cdot D)/B+E_sE_r/B$. The impedance corresponding to $|P_s|_m$ is therefore $E_s^2/|P_s|_m$. A numerical substitution of values corresponding to either actual or per-unit voltages permits easy calculation of this impedance which is then used to locate the intersection in question on the impedance circle diagram, Figure 9. It should be noted that all such intersections fall between the points 0_1 and 0_5.

As indicated in the appendixes, the authors have found no satisfactory way of relating the angle ϕ appearing in the impedance equation 3 to the angle δ_{rs} between sending- and receiving-end voltages. To date, the impedance circle diagrams similar to Figure 9 used in their calculations have all had these points

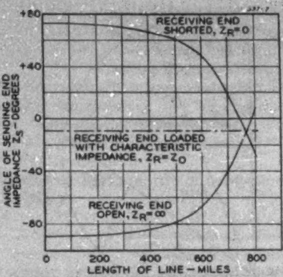


Figure 7. Angle of 60-cycle input impedance of a line suitable for 154-kv operation as a function of length and far-end terminal load

representing various values of δ_{rs} individually computed and located by the following method.

The impedance angle $\psi = \tan^{-1}(X/R)$ appearing on the impedance circle diagram, Figure 5, is the conjugate of the corresponding power-factor angle θ appearing on the power circle diagram. These angles θ are measured for each of several desired values of δ_{rs} on appropriate power circles of diagrams similar to Figure 3 and transferred to the corresponding impedance circle diagrams by locating the intersections of lines Z_s extending from the origin at the conjugate angles ψ and the corresponding impedance circles as in Figure 9.

Locus of Impedance During System Disturbance

If the impedance circle diagram, Figure 9, is used, it is quite enlightening to follow the dynamic behavior of an interconnection during a power swing as it is represented by the swing curves of Figure 5. Prior to system disturbance, the angle δ_{rs} is relatively small, as can be determined from load and terminal voltages by referring to the appropriate power circle diagram similar to Figure 4. Assume for the moment that the direction of the power flow is *from the bus* to the interconnecting circuit at the location of relays. For purposes of this discussion, we may assume the impedance "seen" by the relays to be at *A* on Figure 9. Upon occurrence of a short circuit on the interconnecting circuit close to the far end, the impedance instantly assumes a value closely corresponding to $E_r/E_s=0$. This may be assumed as being at *B*.

If relay and breaker action is correct, both ends of the interconnecting circuit are immediately tripped out, and the systems begin to drift apart as indicated in the swing curve, Figure 5. By the time the interconnection has been reclosed at both ends, terminal voltages will have drifted apart by several degrees so that the impedance "seen" by relays has moved around the impedance circle from original position *A* toward the line of centers 0–0 and now occupies position *C*, possibly on another circle representing a different ratio E_r/E_s. Further swing increases the angle δ_{rs} until the respective accelerations of the two systems have been overcome and δ_{rs} has reached maximum value.

As has been pointed out earlier in this paper, the angle (δ_{rs}max) may exceed 90 degrees, and the resulting impedances may fall in the general vicinity of *D*. Recovery of the system toward normal voltages and angles causes the impedance to retrace path *D*, *C*, *A* and oscillate about the original angle and impedance values at *A* until the systems finally resume their original relation.

A similar step-by-step analysis of behavior based upon the opposite flow of power, that is *from circuit to bus*, is represented by a similar group of points, *E*, *B*, *F*, *G*, and *E*, on the left-hand side of the diagram.

Impedance Referred to Far End of a Nonuniform Circuit

Figure 9, which is the plot of equation 3, has been used in the preceding discussion of conditions as "seen" when "looking into" the circuit from the near end or so-called, but somewhat misnamed, "sending end." To complete the study, it is necessary to investigate at the same time what takes place at the far end or receiving end of the circuit. Figure 10 is the plot of equation 6, which presents the same information in terms of far-end or receiving quantities. In comparing these two Figures 9 and 10, the first thing which attracts attention is the fact that the diagrams are similar in construction except for 180-degree rotation with respect to the axis of the diagram. The rotation corresponds to the change in viewpoint required to "see" the circuit from the opposite end. The dimensions of these two diagrams differ from each other only for *nonuniform* circuits in which $D<\Delta$ is not equal to $A<\alpha$. For uniform symmetrical lines, or for such lines terminated in transformer banks having identical electrical characteristics, it would be sufficient to use Figure 9 for far-end or receiving conditions by making the following changes:

(*a*). Rotate the diagram 180 degrees.

(*b*). Reverse the resistance scales, *R* for $-R$ and vice versa.

(*c*). Reverse the reactance scales, *X* for $-X$ and vice versa.

(*d*). Substitute reciprocal values for the ratios E_r/E_s.

(*e*). Substitute conjugate values for the angles δ_{rs}; that is, in place of $\delta_{rs}=45$ degrees substitute $\delta_{rs}=315$ degrees, and so forth.

Application of Impedance Circle Diagrams to Long-Line Relaying

The impedance circle diagrams, Figures 9 and 10, can be combined on one chart to give a very complete picture of impedance conditions affecting simultaneous relay performance at the two ends of an interconnecting circuit in the following manner:

1. Cut off each diagram along the straight lines representing the condition $E_r/E_s=D$ in the near-end sending diagram, Figure 9, and the condition $E_s/E_r=A$ in the far-end receiving diagram, Figure 10.
2. Fit the two diagrams together along these two lines and match lines of centers 0–0.

The co-ordinates of the two diagrams are thus offset from each other both horizontally and vertically.

Figure 11 is the result of such combination for the Kansas–Nebraska 154-kv interconnection. Because transformers at the opposite ends of this line are not alike, the *A* and *D* constants of the circuit are not equal. Consequently the co-ordinates are skewed slightly, and impedance values scaled across the boundary line *L–L* are not strictly accurate. In spite of these minor shortcomings, the combined diagrams furnish an excellent basis for relay studies.

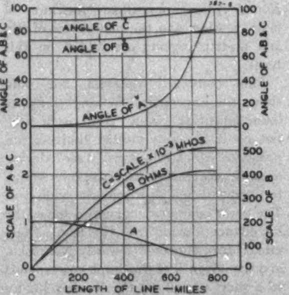


Figure 8. Values of general circuit constants *A*, *B*, and *C* as a function of length for a line having the following constants:

$r=0.252$ ohm per mile
$x=0.803$ ohm per mile
$b=5.22\times10^{-6}$ mho per mile

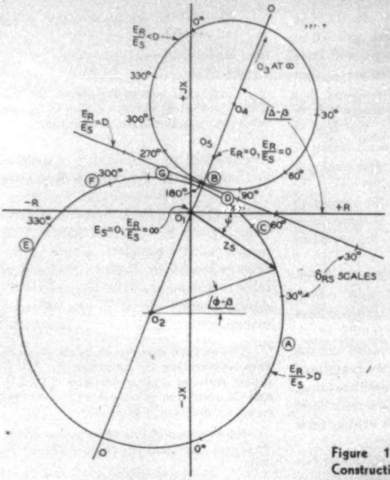


Figure 9 (left). Construction of sending-end impedance circle diagram from equation 3

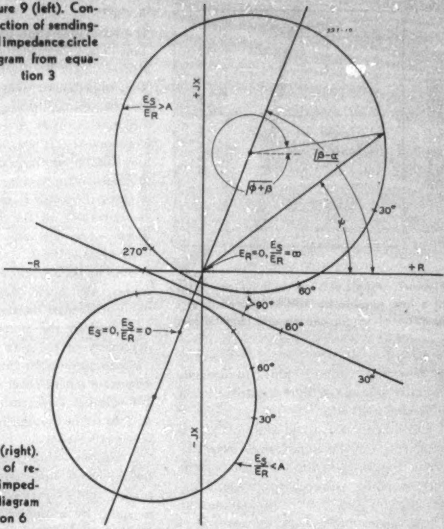

+JX
$\frac{E_S}{E_R}>A$
$\underline{/\beta-\alpha}$
$\underline{/\phi+\beta}$
30°
270°
$E_R=0, \frac{E_S}{E_R}=\infty$
-R
+R
60°
90°
$E_S=0, \frac{E_S}{E_R}=0$
60°
60°
30°
30°
-JX
$\frac{E_S}{E_R}<A$

Figure 10 (right). Construction of receiving-end impedance circle diagram from equation 6

It will be noted that for this circuit values of A and D are 0.824 and 0.769 respectively. In no case of steady-state operation is it likely to be desirable or practical to have ratios E_r/E_s or E_s/E_r as low as these A and D values. Therefore it is not necessary to consider seriously impedance circles having centers in the areas which were cut off when these two diagrams were combined. On the combined impedance diagram, Figure 11, circles corresponding to $E_r/E_s=1.00\pm0.10$, or a total range of 20 per cent in voltage ratio, are plotted to serve as approximate boundary lines between which normal operation is expected. Distances along the lines of centers between points K and N are approximate measures of fault location, and the boundaries of trip and no-trip regions are added on this basis.

This combined diagram not only shows the impedance "looking" into the interconnecting circuit from each end, but on it are also plotted limiting impedances which relays will "see" during open-circuit conditions, zero-power-transfer condition, and short-circuit conditions. The lower part of this diagram shows the impedance looking into the Kansas terminal transformers toward Nebraska, and the origin K of the graph corresponds to a short circuit on the Kansas 132-kv bus. Similarly, the upper part of the diagram "looks" into the circuit from the Nebraska 66-kv bus, and short circuit on the Nebraska 66-kv bus falls on the origin N. This combined diagram is replotted in Figure 12.

Analysis of the combined diagram shows that all solid phase-to-phase faults on the line or in the terminal transformers would establish impedances, as "seen" from the ends of the line, which would fall along the line of centers 0–0 between the origins or terminals N and K. If the fault is an arcing type such as one started by lightning, the fault impedance as "seen" from terminal K would fall in the shaded area B in Figure 12 to the right of the line K–N because of the arc resistance.

Similarly, for the same arcing fault, the impedance "seen" from terminal N would fall in the shaded area H to the left of the line K–N.

Normal operating impedances correspond to the shaded areas A and A' for power flow from K to N and to the areas E and E' for power flow from N to K. The exact impedances in these general areas are determined by the ratio E_r/E_s and the angle δ_{rs}. Both of these factors can be determined for any load condition from the power circle diagrams of the circuit.

During system disturbances, the impedances "seen" by the relays may move out of the normal operating areas A and A', or E and E', into the regions of maximum synchronizing power D and D', or G and G', as explained in a previous section "Locus of Impedance During System Disturbance." If the systems do not recover from the disturbance and thus fall out of step, this loss of synchronism is indicated by further movement of the impedance across the line N–K and through the areas corresponding to faults near the middle of the circuit.

Impedance magnitudes at the high-load edge of areas A, A', E, E' and in the areas of maximum synchronizing power are less than some impedance magnitudes in the fault areas B and H.

This clearly requires that a closely drawn border line must be established between the power-swing area, from which the systems may be expected to recover, and the actual fault zone. To obtain the maximum usefulness of an interconnection, it is necessary for the relays controlling the circuit to discriminate accurately between trip and no-trip conditions along this border line. On interconnections of this nature, there is no harm in permitting tripping on the out-of-step swing into the fault area after all hope of maintaining synchronism is lost. In fact, tripping before the angle δ_{rs} first reaches 180 degrees rather than waiting through two or three cycles of slip to recognize the out-of-step condition is of utmost importance to prevent propagation of further system disturbances.

SUMMARY

The characteristics of long lines and interconnections differ from those of short radial feeders and loops within close-knit networks and dictate special relaying treatment for these circuits. These characteristics may be expressed in terms of impedance magnitude and angle on diagrams similar to Figure 9 and Figure 11. If these impedance diagrams are used, it is possible to define completely and clearly the necessary relay requirements to protect and control a long power-transmission circuit or interconnection.

Required Performance of Long-Line Relays

Expressed in the language of these diagrams, relays for long power-transmission lines or for loose-linked interconnections should:

1. Close their trip-circuit contacts instantaneously at both terminals for all impedance values occurring between terminals *N* and *K* in areas *B* and *H* of Figure 12 caused by either power swings or faults.

2. Not trip at any time for values of circuit impedance corresponding to high-power swings or high kilovolt-ampere transfers in the areas *A*, *A′*, *D*, *D′*, or *E*, *E′*, *G*, *G′*.

3. Provide time-delay backup tripping for faults in areas *B* and *H* which are beyond the far end of the circuit.

4. Prevent immediate tripping of terminal breakers, for faults in the systems external to the protected circuit, by means of carrier-current blocking principles. There should be no objection to such tripping after proper time delay.

5. Provide instantaneous tripping of terminal breakers for ground faults on the protected circuit by means of carrier-current-controlled directional ground relays.

6. Provide time-delay backup protection independent of carrier-current control for all ground faults.

Limitations of Long-Line Protective Relays

From a very brief study of lines longer than the 270-mile interconnection cited in this paper, it appears that these same principles of protective relaying can be applied to transmission lines of greater length without intermediate switching up to any limit at which synchronism can be maintained between constant voltage systems. This limitation is imposed by the fact that the input-impedance angles $\psi=\tan^{-1}X/R$ for such long lines approach the characteristic impedance angle which is small.

In considering the field of unusually long-distance transmission of power, it appears that but little consideration has yet been given to methods of protecting the lines to differentiate between faults and power swings. It is hoped that this analysis of relay performance requirements will stimulate thought and discussion on this complex subject.

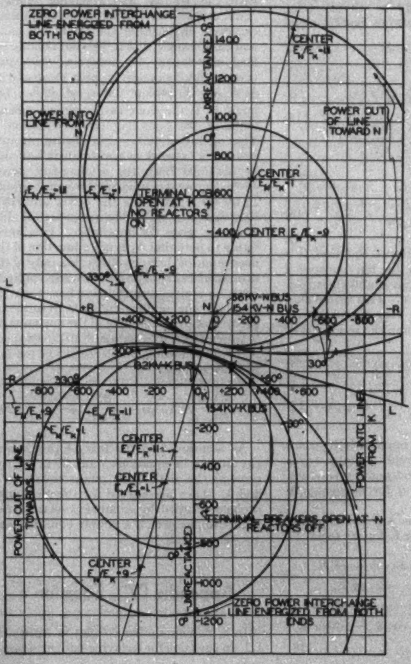


Figure 11 (left). Combined sending-end and receiving-end impedance circle diagrams for Kansas–Nebraska 154-kv interconnection including transformers at both ends

Figure 12 (below). Same as Figure 11 but showing areas of fault *BH*, swing *DD′* and *GG′*, and normal operation *AA′* and *EE′*

Appendix I. Power-Circle-Diagram Equations 1 and 4

The general *ABCD* equations for a circuit are

$$E_s=A\cdot E_r+B\cdot I_r \tag{7}$$

$$I_s=C\cdot E_r+D\cdot I_r \tag{8}$$

From equation 7, $I_r=(E_s/B)-(A\cdot E_r/B.)$

Substituting this into equation 8, we have

$$I_s=C\cdot E_r+D\cdot\left(\frac{E_s}{B}-\frac{\cdot E_rA}{B}\right)$$

$$=\frac{D\cdot E_s}{B}+E_r\left(C-\frac{A\cdot D}{B}\right) \tag{9}$$

$$I_s=\frac{D\cdot E_s}{B}+\frac{E_r}{B}\cdot(B\cdot C-A\cdot D)$$

Since $A\cdot D-B\cdot C=1$

$$I_s=\frac{D\cdot E_s}{B}-\frac{E_r}{B} \tag{10}$$

Using equation 10 and substituting for I_s in

$$P_s=\sqrt{3}\ (\text{conj }E_s)\cdot I_s=P\pm jQ$$

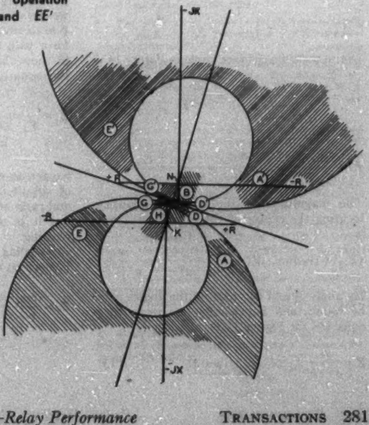
$$P_s=\frac{(\text{conj }E_s)\cdot D\cdot E_s}{B}-\frac{(\text{conj }E_s)\cdot E_r}{B} \tag{11}$$

If δ_{rs} represents the variable angle between terminal bus voltages, this equation may be expressed as follows (writing the circuit constants in polar form):

$$P_s=\frac{E_s^2\cdot D\underline{/\Delta}}{B\underline{/\beta}}-\frac{E_s\cdot E_r\overline{/\delta_{rs}}}{B\underline{/\beta}} \tag{1}$$

or

$$P_s=\frac{E_s^2\cdot D}{B}\underline{/(\Delta-\beta)}-\frac{F_s\cdot E_r}{B}\overline{/(\delta_{rs}+\beta)} \tag{1}$$



Similarly, solving for $\boldsymbol{E}_r$ and $\boldsymbol{I}_r$ from equations 7 and 8, equation 4 for receiving power may be obtained.

$$\boldsymbol{P}_r = \frac{E_s \cdot E_r}{B}\underline{/(\delta_{rs}-\beta)} - \frac{E_r^2 \cdot A}{B}\overline{/(\beta-\alpha)} \qquad (4)$$

In equations 7, 8, 9, and 10, voltages, currents, and circuit constants are on phase-to-neutral basis. In equations 11, 1, and 4 power is total three-phase power with voltages on a phase-to-phase basis, currents are phase currents, and circuit constants are on phase-to-neutral basis.

Appendix II. Basic Impedance Circle Diagram Equation

In order to derive equations 3 and 6, the following review is presented covering the method of inverting a complex quantity of the form:

$$\boldsymbol{Y}_s = a\underline{/\alpha} - b\overline{/(\delta-\beta)} \text{ (see reference 8)} \qquad (12)$$

This equation is an admittance form which can be converted to the impedance form using graphical resolution and some artifices.

A plot of admittance, equation 12, is shown in Figure 13. $a\angle\alpha$ locates the center of the circle, and $b\overline{/(\delta-\beta)}$ is the rotating vector with variable angle δ. Principal points in this figure are labelled 1, 2, and 3.

If the reciprocals of points on this circle are taken and plotted on a convenient scale, another circle will result as shown in Figure 14, which is the locus of the end of the impedance vector $\boldsymbol{Z}_s$ (the reciprocal of $\boldsymbol{Y}_s$), and points on it are conjugate impedance values from Figure 13. An equation may be derived for Figure 14 by first comparing points along the complex quantity passing through the center of the circle with corresponding points in Figure 13. If Y_{o1} is the distance from the origin to point 1, then, if the values from equation 12 for the distance to the center of the circle and radius of circle are used, $Y_{o1} = a-b$ and $Y_{o2} = a+b$.

The corresponding points in the impedance diagram Figure 14 are

$$Z_{o1} = \frac{1}{a-b}, \quad Z_{o2} = \frac{1}{a+b}$$

Diameter of impedance circle in Figure 14 is

$$Z_{o1} - Z_{o2} = \frac{1}{a-b} - \frac{1}{a+b} = \frac{2b}{a^2-b^2}$$

and radius is

$$R = \frac{\text{diameter}}{2} = \frac{Z_{o1}-Z_{o2}}{2} = \frac{b}{a^2-b^2}$$

The magnitude of the complex quantity locating the center of the impedance circle is

$$(Z_{o2}) + (\text{radius, } R) = \frac{1}{a+b} + \frac{b}{a^2-b^2} = \frac{a}{a^2-b^2}$$

By analysis and comparison the angles may be found, and hence complete impedance equation for Figure 14 is

$$\boldsymbol{Z} = \frac{a}{a^2-b^2}\overline{/\alpha} - \frac{b}{a^2-b^2}\underline{/(\phi-\beta)} \qquad (13)$$

It is interesting to note how the preceding equation 13 is related to the admittance equation 12. The constants a and b are now divided by (a^2-b^2). Also the conjugate of $\angle\alpha$ and $\underline{/(\delta-\beta)}$ are the angles of the fixed quantity and radius of the circle respectively. But angle δ is not the same as ϕ as discussed later in the following appendix.

Appendix III. Equation 3

The derivation of this impedance equation is based on the following fundamental equations:

$$\boldsymbol{Z}_s = \frac{(\text{conj } \boldsymbol{E}_s)\cdot \boldsymbol{E}_s}{\boldsymbol{P}_s} = \frac{E_s^2}{\boldsymbol{P}_s} \qquad (2)$$

$$\boldsymbol{P}_s = \frac{E_s^2 \cdot D}{B}\underline{/(\Delta-\beta)} - \frac{E_s \cdot E_r}{B}\overline{/(\delta_{rs}+\beta)} \qquad (1)$$

Equation 2 shows the relation between the three-phase power in any part of a circuit, the phase-to-phase voltage of that part of the circuit, and the phase-to-neutral impedance. Equation 1 is derived in Appendix I.

The reciprocal of equation 2 is the admittance form as follows:

$$\boldsymbol{Y}_s = \frac{1}{\boldsymbol{Z}_s} = \frac{\boldsymbol{P}_s}{E_s^2} \qquad (14)$$

Substituting equation 1 into the preceding equation gives the following general equation:

$$\boldsymbol{Y}_s = \frac{1}{Z_s} = \frac{\dfrac{E_s^2 \cdot D}{B}\underline{/(\Delta-\beta)} - \dfrac{E_r \cdot E_s}{B}\overline{/(\delta_{rs}+\beta)}}{E_s^2} \qquad (15)$$

on simplifying

$$\boldsymbol{Y}_s = \frac{1}{Z_s} = \frac{D}{B}\underline{/(\Delta-\beta)} - \frac{E_r}{E_s \cdot B}\overline{/(\delta_{rs}+\beta)} \qquad (16)$$

Since this is an admittance equation, it is not wholly satisfactory for our purpose if we discuss impedance relays and their relation to circuit impedance. This admittance form will, however, be very useful as will be shown later. By taking the reciprocal of this equation, as follows:

$$\boldsymbol{Z}_s = \frac{1}{\boldsymbol{Y}_s} = \frac{1}{\dfrac{D}{B}\underline{/(\Delta-\beta)} - \dfrac{E_r}{E_s \cdot B}\overline{/(\delta_{rs}+\beta)}} \qquad (17)$$

we get an equation for impedance in terms of circuit constants, circuit voltage angle, and ratio of receiving and sending voltages, but it is not in a form for conveniently plotting.

By going through some algebra, equation 17 may be written in the following form:

$$\boldsymbol{Z}_s = \frac{(b_1a_1+b_2a_2)+(b_2\sin\delta_{rs}-b_1\cos\delta_{rs})}{1+|A|^2-2a_1\cos\delta_{rs}-2a_2\sin\delta_{rs}} + j\frac{(b_2a_1-b_1a_2)+(b_1\sin\delta_{rs}-b_2\cos\delta_{rs})}{1+|A|^2-2a_1\cos\delta_{rs}-2a_2\sin\delta_{rs}} \qquad (18)$$

which also is not conveniently usable because of the large amount of arithmetical work required to plot the diagram representing it.

Instead of directly inverting equation 16, inverting an admittance equation to an impedance form as in Appendix II simplifies greatly the amount of work required and is a method commonly used. Admittance equation 16 was converted to the following impedance equation in this way:

$$\boldsymbol{Z}_s = \frac{D\cdot B}{D^2-\left(\dfrac{E_r}{E_s}\right)^2}\overline{/(\Delta-\beta)} - \frac{B\cdot\left(\dfrac{E_r}{E_s}\right)}{D^2-\left(\dfrac{E_r}{E_s}\right)^2}\underline{/(\phi-\beta)} \qquad (3)$$

This equation for impedance includes the general circuit constants, angles of these constants, ratio of the receiving and sending voltages, and the angle ϕ. The diagram for this equation is Figure 9 discussed in the text of the paper.

Again referring to equation 16, if $E_s = E_r$, then this equation takes the following simple form:

$$\boldsymbol{Y}_s = \frac{D}{B}\underline{/(\Delta-\beta)} - \frac{1}{B}\overline{/(\delta_{rs}+\beta)} \qquad (19)$$

which is the sending-end power, equation 1, with the sending and receiving voltages equal to one volt. Thus the admittance is expressed in terms of the circuit constants and circuit angle, and it does not include the load impedance. On analyzing this equation, we find it is a circle with the center at $D/B\underline{/(\Delta-\beta)}$ and a radius of $1/B$. The angle δ_{rs} is the same as the circuit voltage angle in the preceding power equation 1. This operation shows that the admittance value of a network or transmission line may be obtained from the sending-end power circle diagram for conditions where E_r equals E_s by simply dividing the power and reactive values by E_s^2. The reciprocal of this, of course, gives the impedance. It is interesting to note that Y_s is not a function of the magnitude of the line voltage, but only of the ratio of the sending and receiving voltages. Thus for a given transmission line, equation 19 gives the sending-end admittance, regardless of the magnitude of the line voltage.

In reference to equation 3, if E_r equal E_s, then the following simple equation for impedance results:

$$Z_s = \frac{D\cdot B}{D^2-1}\overline{/(\Delta-\beta)} - \frac{B}{D^2-1}\underline{/(\phi-\beta)} \qquad (20)$$

As mentioned in the text of this paper, the angle ϕ in equations 3 and 20 is not the same as the angle δ_{rs} in power equations 1 and 4. They are related for the particular problem studied by a curve similar to the Figure 15 for which a simple equation has not been found.

Appendix IV. Equation 6

In a manner similar to that in Appendix III, an equation for receiving-end impedance

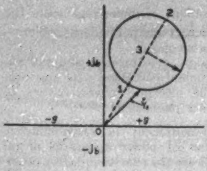


Figure 13. Admittance circle—Y_s

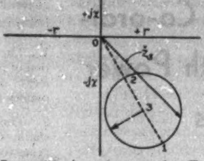


Figure 14. Impedance circle—Z_s derived from Figure 13

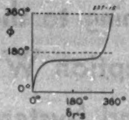


Figure 15. Variation of the angle ϕ with respect to the angle δ_{rs}

may be derived using the following fundamental formula for impedance:

$$\mathbf{Z}_r=\frac{(\text{conj }\mathbf{E}_r)\cdot\mathbf{E}_r}{\mathbf{P}_r}=\frac{E_r^2}{\mathbf{P}_r} \qquad (5)$$

and the following equation for receiving-end power derived in Appendix I:

$$\mathbf{P}_r=\frac{E_s\cdot E_r}{B}\underline{/(\delta_{rs}-\beta)}-\frac{E_r^2\cdot A}{B}\overline{/(\beta-\alpha)} \qquad (4)$$

we obtain the following equation for receiving-end impedance:

$$\mathbf{Z}_r=\frac{B\cdot\left(\dfrac{E_s}{E_r}\right)}{A^2-\left(\dfrac{E_s}{E_r}\right)^2}\overline{/(\phi+\beta)}-\frac{A\cdot B}{A^2-\left(\dfrac{E_s}{E_r}\right)^2}\underline{/(\beta-\alpha)} \qquad (6)$$

This equation may be plotted in the same manner used to plot equation 3 for sending-end impedance circle. Again if $E_r=E_s$, then the above reduces to

$$\mathbf{Z}_r=\frac{B}{A^2-1}\overline{/(\phi+\beta)}-\frac{A\cdot B}{A^2-1}\underline{/(\beta-\alpha)} \qquad (21)$$

Similarly to equation 16

$$\mathbf{Y}_r=\frac{E_s}{E_r\cdot B}\underline{/(\delta_{rs}-\beta)}-\frac{A}{B}\cdot\overline{/(\beta-\alpha)} \qquad (22)$$

Appendix V. Plotting Impedance Circle Diagrams

Impedance circle diagram in Figure 9 was plotted by first calculating the vector which locates the center of the circles and the radii of the circles, using the following equation for impedance:

$$\mathbf{Z}_s=\frac{D\cdot B}{D^2-\left(\dfrac{E_r}{E_s}\right)^2}\overline{/(\Delta-\beta)}-\frac{B\cdot\left(\dfrac{E_r}{E_s}\right)}{D^2-\left(\dfrac{E_r}{E_s}\right)^2}\underline{/(\phi-\beta)} \qquad (3)$$

Since the angle ϕ does not bear a simple relation to δ_{rs}, the degree points on the diagrams corresponding to values of δ_{rs} were obtained by taking the reciprocal of the admittance quantity obtained from equation 16 after ratios of approximate system voltages and circuit constants had been substituted into it.

Similarly equation 6 was plotted and degree points for δ_{rs} were located by using equation 22 to find the admittance for a given value of δ_{rs}.

Appendix VI. General Circuit Constants

General circuit constants used in the impedance, admittance, and power equations for the Kansas–Nebraska 154-kv interconnection including transformers are

$$\mathbf{A}=A\underline{/\alpha}=a_1+jb_2=0.824\underline{/3.25°}$$

$$\mathbf{B}=B\underline{/\beta}=b_1+jb_2=282\underline{/77.3°}\text{ ohms}$$

$$\mathbf{C}=C\underline{/c}=c_1+jc_2=1{,}337\times10^{-6}\underline{/90.9°}\quad\text{mhos}$$

$$\mathbf{D}=D\underline{/\Delta}=d_1+jd_2=0.769\underline{/3.56°}$$

Appendix VII. Auxiliary Formulas

Using the general $ABCD$ circuit equations 7 and 8, Appendix I, auxiliary formulas were derived for calculating limiting circuit impedances.

Sending-end impedance as function of load impedance is

$$\mathbf{Z}_s=\frac{\mathbf{A}\cdot\mathbf{Z}_r+\mathbf{B}}{\mathbf{C}\cdot\mathbf{Z}_r+\mathbf{D}} \qquad (23)$$

Assume short circuit at R end; that is, $E_r=0$, then

$$\mathbf{Z}_s=\frac{\mathbf{B}}{\mathbf{D}} \qquad (24)$$

with the circuit open at R end, that is, $I_r=0$, then

$$\mathbf{Z}_s=\frac{\mathbf{A}}{\mathbf{C}} \qquad (25)$$

The same general equations 7 and 8 and same reference angles being used, the receiving impedance Z_r in terms of the sending impedance Z_s is

$$\mathbf{Z}_r=\frac{\mathbf{D}\cdot\mathbf{Z}_s-\mathbf{B}}{-\mathbf{C}\cdot\mathbf{Z}_s+\mathbf{A}} \qquad (26)$$

Assume short circuit at S end; that is $E_s=0$, then

$$\mathbf{Z}_r=-\frac{\mathbf{B}}{\mathbf{A}} \qquad (27)$$

With S end open; that is $I_s=0$, then

$$\mathbf{Z}_r=-\frac{\mathbf{D}}{\mathbf{C}} \qquad (28)$$

Appendix VIII. Nomenclature

$\mathbf{Z}_s$—Equivalent or apparent phase-to-neutral impedance in ohms looking into the sending end assumed for purpose of establishing a convention for signs and reference angles during calculations.

$\mathbf{Z}_r$—Equivalent or apparent phase-to-neutral impedance in ohms at receiving end.

$\mathbf{Z}$—Impedance expressed in vector form.

$\mathbf{Y}_s$, $\mathbf{Y}_r$—Equivalent or apparent phase-to-neutral admittances in mhos.

ψ—$\text{Tan}^{-1}\dfrac{X}{R}$ of impedance Z.

ϕ—The angle of the variable in equations 3 and 6.

δ_{rs}—Angle in degrees by which the sending-end voltage leads the receiving voltage.

$\mathbf{A}$, $\mathbf{B}$, $\mathbf{C}$, $\mathbf{D}$—General circuit constants expressed in vector form.

$\mathbf{P}_s$—Sending-end power expressed as a vector $=P_s\pm jQ_s=P_s\llcorner\theta$.

$\mathbf{P}_r$—Receiving-end power expressed as a vector $=P_r\pm jQ_r=P_r\llcorner\theta$.

Θ—Power-factor angle of sending or receiving power.

E_s—Phase-to-phase volts at sending end. Phase-to-neutral volts in equations 7, 8, 9, and 10.

E_r—Phase-to-phase volts at receiving end. Phase-to-neutral volts in equations 7, 8, 9, and 10.

$\mathbf{E}_s$, $\mathbf{E}_r$—Vector forms of E_s and E_r, respectively

(conj $\mathbf{E}_s$), (conj $\mathbf{E}_r$)—Conjugate vector forms of E_s and E_r, respectively, that is, with angular rotation measured in *opposite* direction from reference line compared to direction of measurement for $\mathbf{E}_s$ and $\mathbf{E}_r$.

$(\text{conj }\mathbf{E}_s)\cdot\mathbf{E}_s=E_s^2$.

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Measurements Pertaining to the Co-ordination of Radio Reception With Power Apparatus and Systems

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I. Introduction

EXPERIENCE has shown that electric-power apparatus and circuits may, under certain circumstances, influence radio reception. Groups* representing electric-apparatus manufacturers, power companies, radio-apparatus manufacturers and service have been co-operating for several years on the control of this influence. Reports by these groups have summarized progress as this work proceeded. Standard measurement equipments and methods have been established. Papers have been presented also covering various aspects of the general problem. This paper gives the results of practical experience with standard equipments and methods for the measurement of radio influence factors. The various elements in the chain between measured characteristics of the power apparatus and the noise measured in the radio set are analyzed. Quantitative values for the various factors involved in average cases are given.

Problems attendant to interference control by general consent are approached with the attitude that we must have the services of both power and radio and that each must adjust itself to live with the other on the practical basis of general usefulness and reasonable economy. Power service groups have made progress in controlling interference phenomena in their circuits. In the radio field improved reception has been realized. Each group is continuing its endeavors to improve its own facilities. They are also working together co-operatively to understand the problems involved and by joint effort to arrive at the most satisfactory over-all solution.

II. General Situation

The general scope of the co-ordination problem ranges widely through many branches of the power and radio fields, and accordingly a comprehensive plan of attack might appear difficult. However, the general situation has naturally divided itself into four parts, each one of which falls automatically into the hands of a technical group whose regular interests prompt ready response to the attendant problems.

These divisions are as follows:

1. Power circuit generation, transmission and distribution and power equipment, including utilities, industrial and domestic classifications. These constitute the generating sources of the influence phenomena and the circuits which transmit them to points of exposure to the radio circuits.
2. The radio receivers and circuits which may be susceptible to the influence of the power apparatus.
3. Magnetic or electrostatic coupling between power circuits of 1 and radio circuits of 2.
4. Measuring instruments which are suitable for the quantitative evaluation of currents and voltages associated with interference or interference conditions.

The technical problems of the first division fall naturally to the electric-power group; those of the second to the radio group; those of the third to co-operative effort between the power and radio groups, and the fourth or instrument division to instrument technicians of both groups.

III. The Immediate Problem

Within this situation, as already outlined, the immediate practical problem of increasing control and reducing interference is that of recognizing the factors involved, determining their significant relations and evaluating them quantitatively. Experience seems to indicate that the general problem is concerned with the following factors and particular problems:

1. The design, construction, and calibration of instruments suitable for the measurement of the electrical characteristics of radio-influence phenomena.
2. The measurement of radio-influence characteristics of power apparatus of all types to indicate the relative influence possibilities when placed in service.
3. The measurement of radio-influence voltage on power lines to establish a relation between the value existing on the line and the measured characteristics of the apparatus connected to it as found in item 2.
4. The measurement of radio-influence voltage under the power line to establish the relation between the radio-influence voltage at some standard position adjacent to the line but not connected to it and the radio-influence voltage on the line as measured in item 3.
5. The measurement of radio-noise voltage on the receiver antenna to determine the relation between its value and the radio-influence voltage measured at the standard position under the power line in item 4.
6. The measurement of the susceptibility of the radio receivers and antennas to extraneous influences.
7. The determination of limiting values for the ratio of signal to noise for good reception.
8. The determination of limiting program signal strengths on which to base a criterion of good reception.
9. The design of suitable equipment or arrangements for the reduction of radio-noise phenomena where prevailing levels are above those consistent with good reception.

This paper will take up these factors and problems and endeavor to present pertinent data and show the present status of each.

IV. Radio-Noise Meters and Calibrations

Satisfactory instruments for the measurement of radio-interference phenomena have certain unusual requirements:

1. They must measure a standardized quantity significant to the noise output of broadcast receivers.
2. They must be extremely sensitive. Voltages as low as a few microvolts (millionths of a volt) must be measured.
3. They must cover a wide range of wave shapes and equivalent frequencies. Radio-interference phenomena vary from single-polarity impulses or damped oscillations of widely different recurrent times to continuous waves of constant frequency. Amplitudes also vary quite irregularly.

Progress in instrument design and in methods of operation have been made, and these developments have been reported

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* American Standards Association committee on radio electrical co-ordination; Joint Co-ordination Committee on Radio Reception of Edison Electric Institute, National Electrical Manufacturers Association and Radio Manufacturers Association; National Electrical Manufacturers Association committee on co-ordination of acoustic relations of power and communication apparatus and systems.

as fast as experience justified.[1-8] For some time now, two makes of radio-noise meters have been available commercially, and considerable experience has accumulated as to their operational performance. To summarize this experience briefly, some results on sine-wave calibration, square-wave calibration, and noise tests on several devices are reported here.

As regards instrument calibrations, both instruments are supplied with a self-contained calibrating signal source which utilizes the electron shot noise of a saturated diode as the calibrating voltage. Operating instructions on calibration call for the following:

1. Tune the noise meter to the desired frequency.
2. Adjust the calibration circuit to the reference point on the noise-meter scale.
3. Adjust the noise-meter amplification until the meter reads the scale value on the calibration curve for the frequency selected.

This calibration procedure has been followed closely, and, in addition, at frequent intervals and before each set of important noise measurements, a check calibration against a signal generator has been made, following the settings called for on the internal calibrator. Many of these complete calibrations have been on both instruments, and rather complete data are, therefore, now available. The general trend of these data is represented by the specimen curves of Figure 1. These were all made at 1,000 kilocycles.

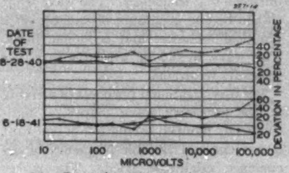
These data show that the accuracy provided by the use of the internal calibrator alone as recommended will be within ±20 per cent for the microvolt range of 10 to 100. For the higher range 100 to 100,000 microvolts, the deviation occasionally goes above ±50 per cent, particularly at the upper end of the measurement range. For any one instrument calibration a high or low trend persisting throughout the measurement range is quite evident. This trend results in lower accuracy for the higher magnitudes of microvolt levels.

Table I. Comparison of Noise Meters on Square Waves

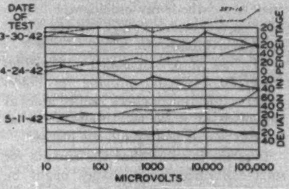
Square-Wave Reference Setting	Noise-Meter Measurements Type A	Noise-Meter Measurements Type B	Per Cent Deviation Based on B
0.14	58	45	+29
0.22	105	75	+40
0.38	210	150	+40
0.52	310	220	+41
0.65	370	290	+34
0.78	480	350	+37
0.90	580	410	+41
1.05	670	500	+34
		Average	+37

Each instrument has a tendency to read high or low on an average basis, and each one may vary from one calibration to another. This calibration experience is presented as representative of that obtained in a laboratory where operators are familiar technically with the details of such measurements.

If greater accuracy is desired, the radio-noise meter may be used by connecting it to the circuit in which the noise is to be measured and noting the reading. It is then connected to the signal generator and the output adjusted until the same reading is obtained on the radio-noise meter. The output of the signal generator then gives the radio-influence voltage in microvolts. Such a method of measurement is slower but results in accuracies within eight per cent.



(a). Tests of 8-28-40 and 6-18-41



(b). Tests of 3-30-42, 4-24-42, and 5-11-42

Figure 1. Radio-noise-meter calibrations

Solid lines all apply to an instrument of one make. Dotted lines all apply to an instrument of another make

The preceding comparison has been made on the basis of a sine wave of radio frequency produced by a signal generator. As the radio-influence voltage measured on electric apparatus is not usually sinusoidal in character but may consist of a very irregular frequency spectrum, it is desirable to have some idea of the behavior of the instruments on other types of waves. Since square-wave signal generators are available, it was decided to compare the performance of the radio-noise meters on this type of wave. An interesting specimen measurement wherein the two noise-meter types were compared for response is summarized in Table I.

These measurements were made with the two meters connected in parallel on the square-wave generator following calibrations of each instrument on a sine-wave signal generator at 1,000 kilocycles. A definite difference between the two instruments is indicated by these data over a wide range of voltage magnitudes. Type *A* reads on an average 37 per cent higher than type *B*. The obvious conclusion is that setting on the internal calibrator supplemented by sine-wave calibration may nevertheless result in considerable differences in readings for two instruments on different wave shapes. In view of the wide range of wave shapes and equivalent frequencies common to radio-noise phenomena, this circumstance shows a definite need for further work to reduce these differences.

As regards comparisons between the two types of meters on measurement of radio-influence voltage of power apparatus, both high and low voltage, data of Table II are interesting. These comparative tests were made following a sine-wave calibration on the signal generator. Measurements were made in each case with the two instruments connected in parallel.

As these data are backed up by sine-wave calibrations and were obtained with the two types of instruments connected in parallel and measured at the same time, they show directly a rather widely different instrument response. This difference in per cent ranges up to 35 per cent and persists over a wide range of noise-voltage magnitudes without any evident trend as to magnitude.

Both types of instruments used in these tests are arranged by their designers to be

Table II. Comparison of Noise Meters on Power Apparatus

Test No.	Test Voltage (60 Cycles) Kilovolts	Radio-Influence Voltage (Microvolts) Instruments Type A	Radio-Influence Voltage (Microvolts) Instruments Type B	Per Cent Deviation Based on Type B Instrument
1 Insulator	10	480	450	+ 7.0
	16	2,600	2,600	0
	22	4,200	4,900	−14.0
	28	5,200	6,000	−13.0
2 Insulator	10	2,100	1,900	+10.0
	16	7,900	8,200	− 3.0
	22	13,000	16,000	−19.0
	28	20,000	23,000	−13.0
3 Small gap	14	23,000	17,000	+35.0
4	0.115	290	250	+16.0
1st small motor	0.115	1,600	1,200	+33.0
5	0.115	1,300	1,600	−19.0
2nd small motor	0.115	1,900	1,750	+ 8.0

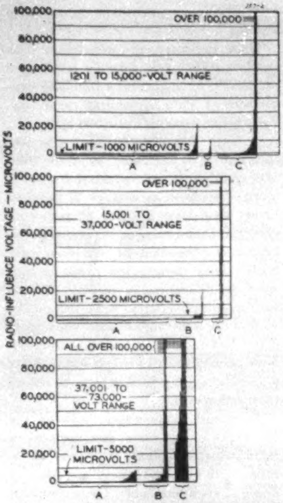


Figure 2. Summary charts of radio-influence voltage data on high-voltage apparatus

For 1,201- to 15,000-volt range each vertical line or dot represents five tests. For all other ranges each vertical line or dot represents one test

A—New apparatus

B—In service without complaints of radio noise

C—Removed because of radio noise and complaints

in general accord with the specifications.[5] Some investigational work has been directed toward the evaluation of instrument characteristics, such as detector-circuit time constants, to ascertain how variations in these might contribute toward measurement differences. To date this has not resulted in improved measurement accuracies.

Consideration of the calibration problem usually proceeds along the lines of specifying instrument-design details or specifying certain calibration waves. The present specifications cover only such over-all design features as seemed necessary to the duplication of radio-receiver characteristics pertinent to noise levels and the incorporation of good instrumentation qualities. Specific details of circuits were not recommended and to the designer was left the freedom of selecting these and improving them when progress permitted. This attitude insures a situation in which advances in design are stimulated.

However, from the above data which are believed to be generally representative, it is obvious that further specifications are necessary. In view of the indicated importance of wave shape, it is desirable that steps be taken toward the specification of calibrating waves of representative shape and equivalent frequency. This has been suggested by one of the authors,[7] and proposals along similar lines have been made by others.

The sine wave now occupies an important place in calibration, is readily obtained, and should be retained. To it might be added a square wave of variable duration and frequency. The wave front should be the maximum obtainable on a practical technical and economic basis. This wave should be available in either polarities to ground.

For investigational purposes in arriving at the necessary wave shapes, a high-frequency damped oscillation of constant decrement and variable frequency of occurrence might be tried. The waves might be examined by recording means such as a cathode-ray oscillograph. Suitable oscillographs of high-writing speed are now available.[8,9]

The use of recommended calibrating-wave shapes would provide for noise-meter calibration or check immediately at the place of noise measurement, or at least at one or more reference places.

It seems possible that the circuits for such calibrating-wave shapes might be standardized in sufficient detail (even to the length of leads, and so forth), so that the wave shapes would be very closely alike. Thus a certain degree of standardization could be accomplished as to wave shape while noise-meter design problems are left entirely in the hands of the instrument designers where they are handled most capably.

To use the radio-noise meter for measurement of radio-influence characteristics of apparatus, it is necessary in order to get comparable results to standardize on setup for making tests. A general recommendation for the making of such tests is given in National Electrical Manufacturers Association publication 107.[4] More specific test codes for particular types of apparatus are given in the NEMA publication for that type of apparatus.

V. Relation of Radio-Influence Voltages of Apparatus to Radio-Receiver Noise

Two methods have been used whereby the radio-influence voltage measured on power apparatus at the factory may be interpreted as having quantitative significance in terms of radio-receiver noise in an average case. In one we analyze the measurements made on new apparatus in the factory and the experience in the field with this apparatus to arrive at a reasonable limit. In the other we consider all the factors given in items 2 through 8 of section III of this paper to obtain values for the following:

Table III. Limits of Radio-Influence Voltage for High-Voltage Apperatus

Circuit Voltage Ratings (Range in Volts)	Insulation Classes (Kilovolts)	Limits of Radio-Influence Voltage (Microvolts)
1,201–15,000	1.2–2.5–5.0–7.5/8.7–15	1,000
15,001–37,000	23/25–34.5	2,500
37,001–73,000	46–69	5,000
73,001–145,000	92–115–138	10,000

1*a*. No limits are included for equipment at circuit voltage ratings above 145,000 volts because of present lack of data above this voltage class.

1*b*. These values do not apply to insulators, as an independent set of values has already been established for them by NEMA, or to motors and generators or thermionic devices, such as rectifiers and inverters.

1*c*. For switchgear assemblies there are not sufficient radio-influence voltage data on which to base a limit.

1*d*. For transformers the limits are based on insulation class only.

2. The radio-influence voltage limits given in this table apply to new apparatus tested in the factory in accordance with the report of the Joint Coördination Committee on Radio Reception of EEI, NEMA, and RMA, entitled "Methods of Measuring Radio Noise" (NEMA publication 107) 1940 and the apparatus test codes and test voltages for each type of apparatus, as approved by NEMA.

3. If apparatus of any given insulation class is used on a circuit of higher-circuit voltage rating, the radio-influence limit and the test voltage for the apparatus shall be that corresponding to its insulation class and not to the insulation class of the circuit on which it is used.

4. For special apparatus having more than one rating the radio-influence limit and test voltage corresponding to the highest rating shall apply.

1. The radio-influence voltage measured on the apparatus under standardized conditions (apparatus *RIV*).

2. The radio-influence voltage measured on a power line to which this apparatus is connected (line *RIV*).

3. The radio-influence voltage measured on or referred to a one-meter antenna at a standard position under this line (under line *RIV*).

4. The radio-noise voltage measured on an antenna in proximity to this power line and referred to a one-meter antenna (antenna *RNV*).

5. The signal strength of the radio program being received.

6. The signal to noise ratio in the receiving set.

7. The susceptibility of the receiver and antenna system.

1. Radio-Influence Voltage (*RIV*) of Apparatus

For several years we have been collecting measured radio-influence voltage values on a wide range of types of power

Table IV. Comparison of Apparatus to On-Line Radio-Influence Voltages

Test Voltage (Kilovolts)	Measured Radio-Influence Voltage on Apparatus (Microvolts)	Measured Radio-Influence Voltage (On-Line)			
		At Apparatus		At End of Line (2 Miles)	
		Microvolts	Ratio	Microvolts	Ratio
20	14,000	9,400	1.5	2,600	5.4

apparatus and circuit voltages. These have been classified in three groups, that is, new apparatus, apparatus in service with no complaints, and apparatus removed because of radio noise and radio-noise complaints. These data have been plotted on a summary chart classified into three voltage ranges, and this chart is shown in Figure 2. In the 1,201–15,000-volt range some 950 individual tests are summarized; in the 15,001–37,000-volt range, 130 tests, and in the 37,001–73,000-volt range, 94 tests. Radio-influence voltages ranging from a few microvolts to 100,000 and above are shown.

From a study of such data it is possible to arrive at recommended levels for the limiting value of radio-influence voltage of apparatus. This has been done, and the recommended levels are shown on the curves and are also given in Table III. Such an approach to the question of limiting values is a direct one. It depends upon accumulating sufficient field experience with apparatus to obtain statistical data on which a conclusion may be reached. As time goes on and more data are accumulated, these can be used to confirm the original conclusions or to suggest desirable modifications of them.

Another approach to the problem begins with the values on apparatus and proceeds through the several steps already outlined, such as radio-influence voltage on the power line, under the line, and on the antenna, and signal strength and signal to noise ratio. This approach requires the establishment of representative values for each of these quantities and then combines them all together to arrive at an over-all ratio. The ratio can then be used to interpret radio-influence voltage of apparatus in terms of the quality of radio reception. This method will now be outlined.

2. RATIO OF APPARATUS *RIV* TO ON-LINE *RIV*

Having the *RIV* of a given piece of apparatus when measured with the standard factory setup, we must be able to form an estimate of the resulting *RIV* when this apparatus is connected to a representative power line. The factory tests are made with 600 ohms across the apparatus, which resistance is intended to simulate line-surge impedance. However, a wide range of impedance will always be met with on power circuits. It is, therefore, necessary to resort to practical measurements wherein factory tests are followed by connection of the apparatus to line, and corresponding line radio-influence voltage measurements are taken. One such test is summarized in Table IV.

This test made on a two-mile test line believed to be representative showed a ratio of 1.5 to 1 between apparatus *RIV* and on-line *RIV*. It also indicated an attenuation of 50 per cent radio-influence voltage per mile, which figure agrees closely with a series of tests made with sine waves on the same line.

3. RATIO OF ON-LINE *RIV* TO UNDER-LINE *RIV*

Since in measurements in the field it is not possible to make measurements of the on-line *RIV* except in special cases, the common procedure has been to make measurements under the line. Such measurements of under-line *RIV* can be correlated with the on-line *RIV* by selection of a suitable place under the line which can be used as a field-strength reference point. The field-strength reference position which has been chosen as most significant and practical is one immediately under the line and midway between the poles. The *RIV* measured on an antenna of one-meter effective height placed at this reference point is designated in this paper as the under-line *RIV*. Since this measurement is referred to a one-meter antenna, it gives the same numerical value as the field intensity of the noise phenomenon at the reference point which has been used by some investigators. Corresponding measurements made on the line and under the line under specified conditions will provide a ratio which can be used later to evaluate under-line noise field strengths. To provide such a ratio, a series of measurements was made on the experimental line previously mentioned using sine-wave line-voltage excitation at 1,000,000 cycles. This line averaged 28 feet in height and was terminated to ground through a surge impedance of 600 ohms, and measurements were made at several points along the line as indicated in Table V.

Table V. Comparison of On-Line to Under-Line Radio-Influence Voltages

Test Point Along the Line in Feet	On-Line *RIV* (Microvolts)	Under-Line *RIV* (Microvolts)	Ratio On Line / Under Line
0	75,000	950	79
900	58,000	1,400	42
1,400	53,000	1,040	51
5,400	28,000	420	67
8,500	24,000	800	30
10,000	16,500	270	61
			Average 55

The on-line to under-line ratios range from 30 to 79 with an average of 55. Two factors at least are known to have contributed to this variation. These were uncertainties of electrical contact on the conductor for the on-line measurements and variations in line height. The test results of Table V, however, represent an average field situation and may, therefore, be used to provide the quantitative ratio necessary to this particular step between apparatus *RIV* and radio-receiver noise.

4. RATIO OF UNDER-LINE *RIV* TO ANTENNA *RNV*

Next in order, it is necessary to establish a ratio between under-line and antenna voltages. If the antenna voltage is expressed in terms of a one-meter antenna, as previously described for the under-line measurement, such a significant ratio will result. A series of tests made in Canada by the radio division of the Department of Transport and directed by H. O. Merriman include a large number of field

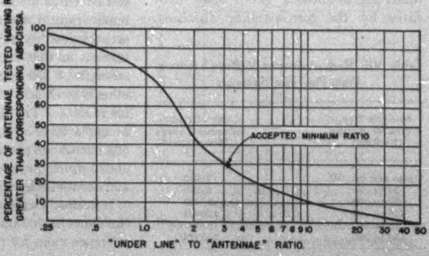


Figure 3. Under-line-to-antenna radio-influence-voltage ratios

Table VI. Test Voltages for Radio-Influence Voltage Measurements

I	II	III	Voltage Rating of Apparatus Normally Used				
			Distribution Cutouts		Lightning Arresters		
Circuit Voltage (Line-to Neutral/ Line-to-Line) (Volts)	Radio-Influence Test Voltage (Volts)	ASA Transformer Insulation Class (Kv)	IV Grounded (Kv)	V Un-grounded (Kv)	VI Grounded (Kv)	VII Un-grounded (Kv)	Switchgear VIII ..... (Kv)
1,440/2,500	1,580	2.5	2.5	2.5	3	3	2.5*
2,500/4,330	2,750	5.0	2.5*	5.0	3*	6	5.0
2,880/5,000	3,160	5.0	5.0*	5.0	6	6	5.0*
4,160/7,200	4,570	8.7	7.5	7.5	6	9	7.5*
5,000/8,660	5,500	8.7	7.5	15.0	6*	9	15.0
7,200/12,470	7,960	15.0	7.5*	15.0	9*	15	15.0
7,960/13,800	8,760	15.0	15.0	15.0	12*	15	15.0
8,650/15,000	9,500	15.0	15.0*	15.0	15*	20	15.0*
13,300/23,000	14,600	23.0			20*	25*	23.0*
19,990/34,500	21,900	34.5			30*	37*	34.5*
26,500/46,000	29,300	46.0			40*	50*	46.0*
39,500/69,000	43,900	69.0			60*	73*	69.0*
53,000/92,000	58,500	92.0			78*	97	115.0
66,500/115,000	73,100	115.0			97	121	115.0*
80,000/138,000	87,700	138.0			121*	145	138.0*
93,000/161,000	102,400	161.0			145*	169	161.0*
113,000/196,000	124,600	196.0			169*	205*	230.0
132,500/230,000	146,200	230.0			195*	242	230.0*
165,500/287,500	182,000	287.5			242*	302*	287.5*

* For the apparatus of columns IV through VIII an asterisk is placed on the line which gives the test voltage (column II) for each apparatus rating.

For transformers, test voltages shall be 110 per cent of the maximum rated line-to-neutral voltage but shall not exceed the maximum values (in column II) corresponding to the insulation class of the transformer of column III.

measurements bearing on this ratio. We are indebted to Mr. Merriman for permission to use these results. Measurements were obtained on 35 antennas ranging in effective heights from 0.2 to 9 meters and located from 20 to 4,500 feet from power lines. Line voltages ranged from 10 to 220 kv. It should be understood that the tests were conducted on outdoor antennas representing a selected group of installations in which radio interference from power lines was reported to be particularly troublesome. Under-line-to-antenna voltage ratios derived from these measurements ranged from 0.25 to 50. The higher this ratio the lower the field from the power line picked up by the receiver antenna and the better the antenna system for good reception. These values are plotted on Figure 3 against percentage of antennas tested as ordinate. This ordinate percentage gives the proportion of receiver stations tested having higher under-line-to-antenna ratios than indicated by the corresponding abscissas. Since this investigation was made mainly on "interference-affected" situations, the curve shows only 30 per cent of the antennas had a ratio higher than 3.2. The investigators estimated that, when all receivers were considered, approximately 90 per cent of them would have ratios higher than 3.2. In accordance with these tests, Canadian Practice[10] sets a value of 3.2 for the minimum permissible under-line-to-antenna ratio. This means that ratios less than this for line-to-antenna couplings do not measure up to the performance that reasonably may be required for good reception.

Table VII. Radio-Influence Voltage Limits o Plain Pin-Type Insulators

60-Cycle Dry Flashover Rating (Kv Rms)	Limit of Radio-Influence Voltage (Microvolts)
Up to and inc. 50	2,500
51 to 79	5,500
80 to 109	8,000
110 to 124	12,000
125 to 149	16,000
150 to 200	25,000

5. SIGNAL STRENGTH OF RADIO PROGRAM

The required signal strength of the radio program for good reception has been estimated by various engineers to range from 10,000 microvolts per meter in large cities to 100 microvolts for sparsely settled rural areas. The Federal Communications Commission recognizes the area for good reception of certain types of stations as that covered by a signal field strength of 500 microvolts and for some other classes field strengths as low as 100 microvolts. The Canadian regulations accept a level of signal field strength of 500 microvolts per meter as a lower limit above which good reception is practically and economically achievable.

It seems, therefore, reasonable to use the figure of 500 microvolts per meter as reference value for this study.

6. SIGNAL-TO-NOISE RATIO

Several series of listener tests utilizing a range of types of radio noise at various signal strengths and noise levels have indicated that a signal-to-noise ratio of 32 to 1 is an average ratio for use as a criterion of good reception. This ratio is accepted in the Canadian regulations.[11]

A ratio of 40 decibels has been suggested by International Electrotechnical Commission as a suitable signal-to-noise value indicating, however, at the same time, that values higher than 30 decibels have not been achieved in practice. Also, the FCC protects clear channel stations with a signal-to-noise ratio of 20 decibels. The test ratio of 32 to 1 (30 decibels), therefore, seems appropriate.

7. APPARATUS LIMITS DERIVED FROM THESE RATIOS

Having discussed the several significant ratios and values, we may now use them to arrive at a reasonable value for the radio-influence voltage of apparatus. If the signal strength of 500 microvolts per meter at the radio receiver is divided by the signal-to-noise ratio of 32 to 1, an antenna radio-noise voltage of 15.6 microvolts is obtained. Using this value with the under-line-to-antenna ratio of 3.2 gives a value of 15.6 times 3.2 equal to 50 microvolts per meter, which is, therefore, an upper limit of noise field intensity at the reference position under the line. This value can now be used with the other ratios to obtain an apparatus limit. For instance, on the 28-foot line previously referred to, with an apparatus-to-on-line ratio of 1.5 and an on-line-to-under-line ratio of 55, the apparatus limit for radio-influence voltage will be 50 times 1.5 times 55 equal to 4,100 microvolts; also for a 35-foot line correcting the 55 ratio proportional to line height now becomes 70 to 1 and gives an apparatus limit for radio-influence voltage of 50 times 1.5 times 70 equal to 5,300 microvolts.

These figures have direct significance when compared with the apparatus limits based on direct experience with factory-measured values as described in section V—1 of this paper and given in Table III.

Table VIII. Radio-Influence Voltage Limits for "Radio-Freed" Pin-Type Insulators

60-Cycle Dry Flashover Rating (Kv Rms)	Limit of Radio-Influence Voltage (Microvolts)
Up to and inc. 50	50
51 to 79	50
80 to 109	100
110 to 124	100
125 to 149	200
150 to 200	200

Table IX. Radio-Influence Voltage Measurements on Insulators (Effect of Field Configuration)

Radio-Influence Voltage in Microvolts

1 Test Voltage Applied (Kv—Rms)	2 Diagram A 10-Inch Sphere	3 Diagram A 5-Inch Sphere	4 Diagram B 10-Inch Sphere	5 Diagram C 10-Inch Sphere (2d Insulator Nearby)	6 Diagram C 10-Inch Sphere (2d Insulator Grounded)	7 Diagram C 10-Inch Sphere, 2d Insulator Moved to 72 Inches
80	7	55	185	7	120	17
100	29	78	225	22	205	35
120	52	98	225	55	270	70
140	92	135	290	110	360	120
160	144	155	1,100	230	840	360

The figures of column 3 show the increase in voltage obtained with the substitution of the smaller five-inch sphere for the ten-inch. The increase is particularly pronounced for the lower voltages. In the fourth column the ten-inch sphere was located immediately on top of the insulator, resulting in a marked increase in voltage due to discharge in the crevices between hardware and porcelain. The fifth column shows that just locating an insulator nearby gives a marked increase in voltage, presumably through changes in field configuration. Grounding the second insulator further increases the voltage as shown in column 6, and moving it further away gives the expected decrease of column 7. These results are typical and show how important are the details of circuit arrangements for such tests.

The greater part of the field data and experimental investigation represented in this analysis are derived from lines of about 66-kv construction. The limit of 5,000 microvolts for this circuit voltage of Table III is between the level for 28-foot- and 35-foot-high lines. It is apparent, therefore, that the limits reached through both approaches are consistent. On lower-voltage lines the apparatus limits of 2,500 and 1,000 microvolts appear to have the necessary margin of safety consistent with their frequent location closer to radio receivers. Also, the higher-voltage range at 10,000 microvolts should be acceptable, because distances to receivers are greater, and because up to this time interference experience has not been sufficient to indicate any practical necessity of a lower limit.

The problem of practical limits for factory tests of radio-influence voltage on apparatus has now been considered from two standpoints, first that of direct experience with factory measurements on apparatus, and second that of experimental determination of significant coupling ratios between apparatus and receiver set. The limits arrived at from these two standpoints are in good agreement.

The values given in Table III have been adopted as limits by many NEMA sections for high-voltage apparatus.

VI. Radio-Influence Test Voltages for Apparatus

Radio-influence voltages on apparatus generally increase with increasing applied voltage. Consequently suitable influence-voltage limits will of necessity be tied in with applied test voltages which will bear definite relation to the apparatus circuit voltage. When this is done, all apparatus used on the same circuit will have a common standard test voltage. Because different kinds of apparatus have different rating bases, it has been found necessary to work out a common basis for as wide a range of apparatus types as possible.

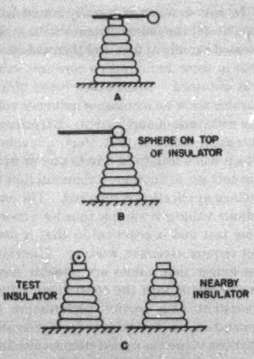


Figure 4. Radio-influence voltage tests on pedestal insulators

The circuit voltage determines the voltage stress to ground that is applied to the connected apparatus, and hence it appears logical that the circuit voltage should be used as the basis for establishing the test voltage. Since some apparatus is designed for use on several different circuit voltages, the standard test voltage for such apparatus should be based on the maximum circuit voltage for which such apparatus is rated.

It is recognized that in some cases the test voltage is less than the rated voltage of the apparatus. However, it will be found that in such cases the rating of the apparatus is not based upon the sustained voltage but upon abnormal conditions.

Table VI lists the various three-phase line-to-line circuit voltages with the corresponding line-to-neutral voltages. It gives a standard test voltage that is based on 110 per cent of the line-to-neutral circuit voltage and shows how standard apparatus normally would be applied and tested. For transformers the test voltage is 110 per cent of the maximum rated line-to-neutral voltage, which is the highest rated tap voltage for wye-connected transformers and the highest rated tap voltage divided by the square root of 3 for delta-connected transformers, but shall not exceed the highest values in column IV for the insulation class of the winding of column III. These values have been standardized by NEMA and appear to the extent required in the appropriate high-voltage-apparatus publications to which reference should be made for specific types.

VII. Insulators

Experience in the fields of apparatus and line insulators has indicated these to be occasional sources of radio interference. Probably the most common case is that wherein a radio-influence voltage results from the breakdown of a small air gap between tie wire and insulator or sparkover in a crevice of a defective insulator. Particular defective insulator units must be located and removed. Generally plain pin-type units will produce not more than the levels of influence voltage shown in Table VII.

In many localities well-served by broadcasting stations, plain pin-type insulators have been used satisfactorily. However, the values of Table VII are substantially above the apparatus limits of Table III. Accordingly, insulator users and manufacturers have directed attention to the reduction of radio-influence voltages for insulators. In this marked success has been achieved. One user in a locality

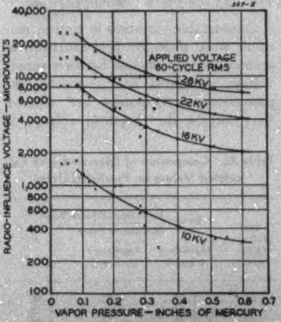


Figure 5. Variation of radio-influence voltage with humidity on high-voltage insulators

Table X. Radio-Influence-Voltage Tests on Insulators
(Effect of Paralleling Units)

Test Voltage Applied (Kv—Rms)	Insulator Units (Microvolts), Individual Tests								Paralleled Units (Microvolts)			Calculated Root-Sum-Square		
	1*	2	3	4	5	6	7	8	6-7	3, 5, 6, 7	All 8	6-7	3, 5, 6, 7	All 8
10	5,100	4,900	4,300	3,410	4,200	4,300	4,300	3,700	5,900	7,600	11,500	6,100	8,000	12,200
16	14,500	17,500	17,500	13,500	17,500	17,500	15,500	13,500	19,500	27,000	35,000	23,500	34,000	45,000
22	25,000	32,000	32,000	24,000	32,000	31,000	27,000	23,000	30,000	46,000	64,000	42,500	62,500	82,000
28	44,000	48,000	49,000	39,000	46,000	42,000	39,000	39,000	49,000	73,000	97,000	57,000	88,000	122,000

* The eight insulators used were numbered 1 to 8.

not well-served by broadcasting stations has worked out a method of applying an asphalt emulsion[12,13] to each insulator which gives very low levels. Insulator manufacturers have given considerable attention to the problem, and as a result "radio-freed" pin-type insulators are now available.

In an effort to set up reference standards significant to the levels achievable through this special design, the insulator group of NEMA has agreed upon the values of Table VIII.

Since we have two types of insulators, the plain and radio-freed, and two corresponding sets of limits for radio-noise-influence characteristics, it seems logical to assume that these limits are based upon the values it is possible to achieve under the manufacturing conditions pertaining to each type of insulator.

VIII. Radio-Influence-Voltage Variables in Apparatus Testing

As high-voltage apparatus testing for radio influence has progressed, experience has focused attention on several factors which affect apparatus levels in very pronounced measure. Among these are voltage field configurations, paralleled apparatus units, time of voltage application, and humidity.

1. Voltage Field Configurations

Considerable variation in testing may result when dielectric field distributions around test connections between supply voltage apparatus and test-piece and measurement equipment are altered. Very largely the threshold voltage at which considerable noise begins and the amount of influence voltage as dependent upon the number and extent of field-voltage gradients concentrated at particular points of small radius of curvature on the test piece or test circuit. Such field concentrations will be altered in degree with every change of circuit, and it is, therefore, important that attention be given to them in making a test setup. To illustrate this effect a brief description of a test wherein it is involved in a very conspicuous manner is given.

A pedestal insulator was being tested by arrangements shown in Figure 4. Voltages were applied to arrangements A, B, and C, and radio-influence voltages were measured. The results are given in Table IX

Table XI. Comparison of Measured with Calculated Values on Paralleled Units

Test Voltage (Kilovolts)	Ratio: Root-Sum-Square Value / Measured Value		
	Insulators 6 and 7	Insulators 3, 5, 6, 7	All 8
10	1.03	1.13	1.06
16	1.31	1.26	1.29
22	1.42	1.36	1.28
28	[illegible]	1.21	1.26
		Average ratio	1.23

2. Paralleled Units of the Same Apparatus

Radio-influence voltages generally increase when additional units of a particular type are added to the test circuit. The amount of this increase has not been confirmed by sufficient data to provide for good understanding on this point. According to local experience the custom has ranged from using the single-unit value to multiplying it by the number of units in parallel. Results of a practical test on this point are summarized in Table X.

It is obvious from these results that additional units increase the measured radio influence definitely, but that this increase is not in proportion to the number of units added. Some observation of high-voltage radio-interference phenomena by oscillograph has shown the voltage waves to be quite variable in duration, amplitude, and time of occurrence. When additional units are added, presumably crests do not correspond, or the new waves are added in time between the old. For practical purposes the method of estimating the results for added units by the root-sum-square method has been found satisfactory. This method is applied by adding the squares of individual unit results and taking the square root of this sum. Applied to the results of Table X, values as given in Table XI are obtained.

It is apparent that, calculated in this manner, values somewhat higher than the combined measured values are obtained. This test shows the calculated values to range some 23 per cent higher than the measured. A value thus obtained is considered to be a good conservative estimate.

3. Time Factor in Voltage Application

Radio-influence voltage varies with time of voltage application, particularly within the first few minutes. This is illustrated by a test summarized in Table XII made on a pin-type insulator at 22 kv and at 0.34 inch of mercury atmospheric vapor pressure.

Table XII. Radio-Influence-Voltage Variation With Time of Applied Voltage

Time (Minutes)	Radio-Influence Voltage (Microvolts)
0	7,700
1	6,000
2	5,600
3	5,000
4	4,500
5	4,400
10	4,200
15	3,900
20	3,800

In this case, as is usually found with insulators, the radio-influence voltage decreased rapidly at first and then slowly, so that in some 20 minutes a constant value was obtained. Some other types of apparatus show an increasing influence voltage with time of application. Experience, therefore, indicates that, to obtain values which will check from day to day or from one test set to another, a common time of voltage application is required. The one-minute value gives ample time for a short-time test and is practical in that it does not require excessive waiting. However, the longer time values are possibly more in agreement with the continuous values consistent with service performance of apparatus. A five-minute test generally has been viewed as a good compromise and is now contained in a number of apparatus test codes.

4. Humidity

Measurement experience has shown that in many cases atmospheric humidity has a substantial effect on radio-influence voltages of apparatus. A good example is that of porcelain insulators, with which this variation may be as much as 4 to 1 over the normal range of atmospheric humidity conditions. A specimen-test result showing influence-voltage variations at several voltage levels on a standard pin-type insulator is shown in Figure 5.

This test was made in a closed chamber in which the atmosphere could be artificially reduced by suitable dryers, so that a range from 0.03 inch of mercury vapor pressure to 0.6 inch was obtained. The humidity was measured with a dew point potentiometer, and data of radio-influence voltage variations were obtained at 10 kv, 16 kv, 22 kv, and 28 kv. The marked increase in voltage as humidity decreased is consistent at all voltage levels tested. The normal test voltage for this insulator is 22 kv, and the limiting value of radio-influence voltage, 12,000 microvolts from Table VII.

Adding moisture until precipitation takes place has a varied effect. When water droplets fill in small crevices between conductor and insulation, in which local breakdown occurs, the influence voltage will decrease, because the water shifts the voltage across the crevice to other parts of the dielectric in series. But the converse of a higher influence voltage may result, if droplets form projections of small radius on an otherwise smooth surface. In this case the voltage gradient at the droplet increases, and additional local disruption gives rise to increased radio-influence voltage. Humidity then is another factor contributing to influence voltage level which must be reckoned with in testing and considered in cases of field interference.

IX. Conclusions

1. Use of the present available radio-noise meters when utilized only with the internal calibrator gives results within ±50 per cent in measurements on sine waves of radio frequency.

2. Use of a sine-wave signal generator to calibrate the noise-meter results in marked improvement and permits obtaining checks on sine waves of radio frequency to within eight per cent.

3. Even when calibrated on a signal generator, the results with different radio-noise meters, when used to measure on a given square wave, may differ by 37 per cent. Differing types of apparatus noise also result in similar meter differences.

4. As an improved instrument-performance specification, it is recommended that one or more calibration waves of varied and representative shapes be adopted.

5. Limits for the radio-influence voltage of apparatus derived on the basis of apparatus measurement and field experience are given in Table III. These are the same as the values adopted by many sections of NEMA for high-voltage apparatus.

6. To bridge the gap between radio-influence voltages of apparatus measured in the factory and noise measured on a radio-receiver set, a series of values is found valuable. These ratios are—

(*a*). Apparatus-to-on-line ratio, 1.5 to 1.

(*b*). On-line-to-under-line ratio, 70 to 1.

(*c*). Under-line-to-receiver-antenna ratio, 3.2 to 1.

(*d*). Signal-to-noise ratio, 32 to 1.

7. Apparatus limits for radio-influence voltage given in Table III and derived on the basis of apparatus measurement and field experience agree with limits as found through the use of field measurements and ratios as in conclusion 6.

8. Based on these ratios and assuming an antenna signal strength of 500 microvolts per meter and a signal-to-noise ratio of 32 to 1, a relative radio-influence voltage level for apparatus limits is derived. This level agrees with those of Table III based on factory tests and field experience.

9. To provide for the testing of all apparatus used on the same circuit, a standard set of test voltages for many types of high-voltage apparatus has been standardized as given in Table VI.

10. Factors which must be controlled in the measurement of radio-influence voltage in apparatus are voltage field configurations, number of test parts in parallel, time of voltage application, and humidity. To provide good measurement checks, these factors should be included in individual apparatus test codes.

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Open-Delta Transformer Banks—Analysis of Circuit

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FELLOW AIEE

WHEN a three-phase transformer bank is operated in open delta, an unbalanced secondary voltage results, and the transformers themselves may be overloaded. The unbalanced voltage will cause a circulating current to flow which may cause overheating of the equipment in the secondary circuit to an extent depending upon the amount of the voltage unbalance and the load being carried. Wolf and Mattison[1] have described a method of overcoming the voltage unbalance, and this paper is written to mention briefly the effect of unbalanced voltages on the operation of induction motors and to present an analysis of the circuit, together with some correlated material.

Open-delta operation of transformers will set up negative-sequence voltages in the secondary circuit which may cause increased heating of induction motors. Whether or not this increased heating is serious depends upon the load being carried by the motors and their temperature-rise characteristics.

The unbalance of the secondary voltages can be reduced to a negligible value by the addition of capacitance to the circuit, and a method is given of estimating the required amount and its distribution.

Existing methods of calculating regulation of open-delta transformer banks are not suitable for the analysis of this problem, and a new method is presented together with a method of calculating the positive and negative components of the current necessary for the application of the new method.

Effect of Unbalanced Voltages on Motor Operation

The unbalanced voltages resulting from open-delta operation are detrimental to the operation of induction motors in the secondary circuit and result in a reduction in torque and an increase in heating. The amount of voltage unbalance depends, not only upon the reactance of the transformer, but also upon the magnitude and type of load. The greatest unbalance will occur when all the load is static and the least when all the load is rotating. However, a given negative-sequence reactance applied to an induction motor produces a definite negative-sequence current of a magnitude depending only upon the characteristics of the motor.

There is a reduction in torque, because the positive-sequence voltage applied to the motor is reduced. For example, in Figure 4 of the Wolf–Mattison paper, the positive-sequence line-to-neutral secondary voltage for the closed-delta case is 335.2 volts, with an applied line-to-line equivalent voltage of 615 volts, and it is reduced to 319.8 volts for the open-delta case, with line-to-line equivalent voltage of 622. This represents a reduction in torque of nine per cent, and, in addition, the negative-sequence voltage introduces a small (usually negligible) torque in the reverse direction. Since the required mechanical output is unchanged, the current must increase to maintain the load, and increased heating results.

The unbalanced regulation of the open-delta bank introduces a negative-sequence voltage which, in turn, causes negative-sequence current to flow, and a further increase in heating results. The negative-sequence impedance of induction motors may be in the order of 10 to 25 per cent, so that a relatively small per-unit negative-sequence voltage causes a relatively larger current to circulate. The increase in losses is illustrated in Figure 1 for two types of induction motors. A ten per cent negative-sequence voltage may cause an increase in total loss, ranging from 33 to 93 per cent at full load, depending upon the characteristics of the motor.

The extra loss is revealed as increased heating in the motor. If the excess loss were evenly distributed among the three phases, the overheating would be proportional roughly to the excess losses. However, the current unbalance is such that the overcurrent flows through one phase of wye-connected motors or through two phases of delta-connected motors, resulting in local overheating and relatively greater excess temperatures than indicated by the over-all excess losses.

Whether or not this overheating is serious will depend upon the loading of the motor and the closeness of the design. If the motor is underloaded or has a liberal margin in the temperature-rise performance, the increased heating probably will not be harmful. If, however, the motors are fully loaded and already operating at a temperature close to the maximum temperature for which a reasonable length of life may be expected, the overheating caused by the excess losses may be disastrous.

Regulation of Open-Delta Transformer Banks

The usual approximate formulas[2] given for the calculation of the regulation of open-delta transformer banks are derived upon the assumption of balanced voltages and currents. Since this condition does not exist in the secondary circuit of an open-delta bank, the results are in error to a greater or less degree, and the formulas cannot be used for an analysis of this nature.

The regulation of an open-delta transformer bank may be developed as follows. See Figure 2, Figure 3a, and Figure 8.

The voltage and current relationships are

$$\begin{aligned} E_{AB}&=E_{ab}+I_{ab}Z_t \qquad & I_{ab}&=I_B \\ E_{BC}&=E_{bc}+I_{bc}Z_t & I_{bc}&=0 \\ E_{CA}&=E_{ca}+I_{ca}Z_t & I_{ca}&=-I_C \end{aligned} \tag{1}$$

and from these

$$\begin{aligned} E_{AB}&=E_{ab}+I_BZ_t=E_{ab}+e_{ab} \\ E_{BC}&=E_{bc}+(I_C-I_B)Z_t=E_{bc}+e_{bc} \\ E_{CA}&=E_{ca}-I_CZ_t=E_{ca}+e_{ca} \end{aligned} \tag{2}$$

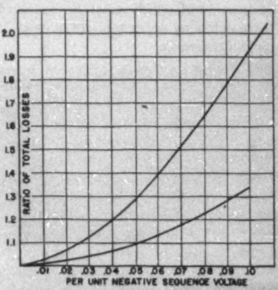

Figure 1. Variation in total loss of two types of induction motors caused by negative-sequence voltage

Motors maintain full mechanical output, and positive-sequence voltage decreases

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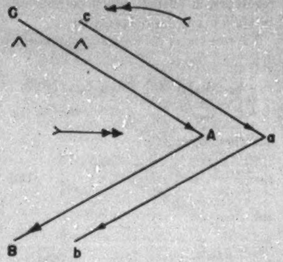


Figure 2 (left). Representation of open-delta transformer bank with direction of power flow indicated

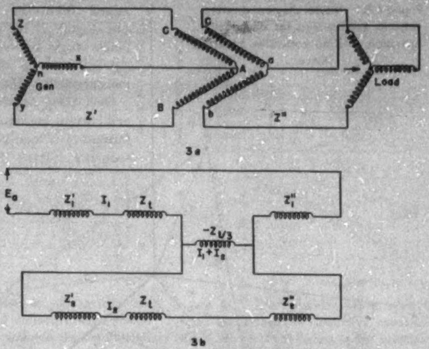


Figure 3 (right). Representation of complete circuit with open-delta transformer bank and the corresponding equivalent circuit

In the foregoing it is presumed that the phase angle of I_B in reference to E_B, and so forth, is known.

If we write

$$\begin{aligned} e_{ab} &= I_B Z_t && = (I_{B1}+I_{B2})Z_t \\ e_{bc} &= (I_C-I_B)Z_t && = (I_{C1}-I_{B1}+I_{C2}-I_{B2})Z_t \\ e_{ca} &= -I_C Z_t && = -(I_{C1}+I_{C2})Z_t \end{aligned} \qquad (3)$$

it can be shown easily from formula 3 and Figure 8 that

$$e_{ab1} = \frac{(-1+a^2)(2I_{A1}-I_{A2})}{3} Z_t$$

and

$$e_{a1} = \frac{2I_{A1}-I_{A2}}{3} Z_t \qquad (4)$$

$$e_{ab2} = \frac{(-1+a)(-I_{A1}+2I_{A2})}{3} Z_t$$

and

$$e_{a2} = \frac{-I_{A1}+2I_{A2}}{3} Z_t$$

and also

$$e_a = \frac{I_{A1}+I_{A2}}{3} Z_t \qquad = \frac{I_A}{3} Z_t$$

$$e_b = \frac{(2a^2-a)I_{A1}+(2a-a^2)I_{A2}}{3} Z_t = \frac{2I_B-I_C}{3} Z_t \qquad (4a)$$

$$e_c = \frac{(2a-a^2)I_{A1}+(2a^2-a)I_{A2}}{3} Z_t = \frac{2I_c-I_B}{3} Z_t$$

From formula 4 and Figure 3a we can write the expressions for the total positive and negative voltages in the circuit. They are

$$\begin{aligned} E_a &= I_1\left(Z_1+\frac{2Z_t}{3}\right)-I_{a2}\frac{Z_t}{3} \\ 0 &= -I_1\frac{Z_t}{3}+I_2\left(Z_2+\frac{2Z_t}{3}\right) \end{aligned} \qquad (5)$$

These expressions, formula 5, yield the equivalent circuit shown in Figure 3b. It is interesting to note that this is identical with the equivalent circuit for open-delta transformer banks originally developed by Edith Clarke in 1934 using an entirely different approach.

The sequence currents may be calculated as follows:

$$I_{a1} = E_a \frac{Z_2+\frac{2}{3}Z_t}{D} \qquad (6)$$

$$I_{a2} = I_{a1} \frac{Z_t/3}{Z_2+\frac{2}{3}Z_t} \qquad (7)$$

in which

$$D = \left(Z_1+\frac{2}{3}Z_t\right)\left(Z_2+\frac{2}{3}Z_t\right)-\frac{1}{9}Z_t^2 \qquad (8)$$

Z_1 = positive-sequence impedance external to the transformer

Z_2 = negative-sequence impedance external to the transformer

Again we write from formulas 2 and 3

$$\begin{aligned} E_{AB} &= E_{ab}+I_{B1}Z_t+I_{B2}Z_t \\ E_{BC} &= E_{bc}+(I_{C1}-I_{B1})Z_t+(I_{C2}-I_{B2})Z_t \\ E_{CA} &= E_{ca}-I_{C1}Z_t-I_{C2}Z_t \end{aligned} \qquad (9)$$

In the foregoing the voltage drop in the open-delta bank between the primary and secondary windings is made up of the drop resulting from two symmetrical currents. Also, we may look upon the primary voltages as those existing before the transformers are loaded, and therefore they are a set of symmetrical voltages.

This permits a simplification in the expression to the following:

$$\begin{aligned} E_{ab} &= E_{AB}-(I_{A1}Z_t\epsilon^{j(\phi+\theta_1+90)}+I_{A2}Z_t\epsilon^{j(\phi+\theta_2-90)}) \\ E_{bc} &= E_{BC}-(\sqrt{3}I_{A1}Z_t\epsilon^{j(\phi+\theta_1)}-\sqrt{3}I_{A2}Z_t\epsilon^{j(\phi+\theta_2)}) \\ E_{ca} &= E_{CA}-(I_{A1}Z_t\epsilon^{j(\phi+\theta_1-90)}+I_{A2}Z_t\epsilon^{j(\phi+\theta_2+90)}) \end{aligned} \qquad (10)$$

In this equation the angle ϕ is the angle of the transformer impedance, θ_1 and θ_2 are the phase angles of the positive- and negative-sequence currents, I_{A1} and I_{A2}, in reference to E_A. The angles θ and θ_2 are negative for lagging current and positive for leading current.

The foregoing equation 10 permits the determination of the voltage drop for each phase, the second term on right side of each expression. When this voltage drop is expressed on a per cent or per-unit basis, any of the conventional expressions[8] may be used to calculate the regulation, depending upon the degree of accuracy required.

In each of the foregoing equations 10 the reference vector for each secondary voltage is its respective primary voltage. On further expansion and with the usual approximations introduced, the positive-

Table I

	Measured	Derived		Measured	Derived	
Kw	982	982		975	975 (982)	
I_A	1,500	1,500	$\epsilon^{-j35.90}$	1,200	1,200	$\epsilon^{-j36.10}$
I_B	1,130	1,130	$\epsilon^{-j167.58}$	1,150	1,150	$\epsilon^{-j157.95}$
I_C	1,110	1,110	$\epsilon^{j96.17}$	1,150	1,150	$\epsilon^{j84.95}$
E_{ab}	578	578	$\epsilon^{-j152.24}$	602	602	$\epsilon^{-j130.30}$
E_{bc}	538	538	$\epsilon^{j35.90}$	595	595	$\epsilon^{j38.90}$
E_{ca}	545	545	$\epsilon^{-j79.30}$	592	592	$\epsilon^{-j36.16}$
E_A		328.8			345.1	
E_B		324.9	$\epsilon^{-j124.38}$		346.8	$\epsilon^{-j120.94}$
E_C		305.3	$\epsilon^{j118.40}$		341.0	$\epsilon^{j119.28}$
I_a				700	700	$\epsilon^{j89.5}$
I_{bc}				350	350	$\epsilon^{-j1.20}$
I_c				900		
I_{bt}					840	$\epsilon^{-j145.40}$
I_{ct}					829	$\epsilon^{j130.05}$

Figure 4A. Vector diagram showing voltages and currents for open-delta operation with no corrective capacitance

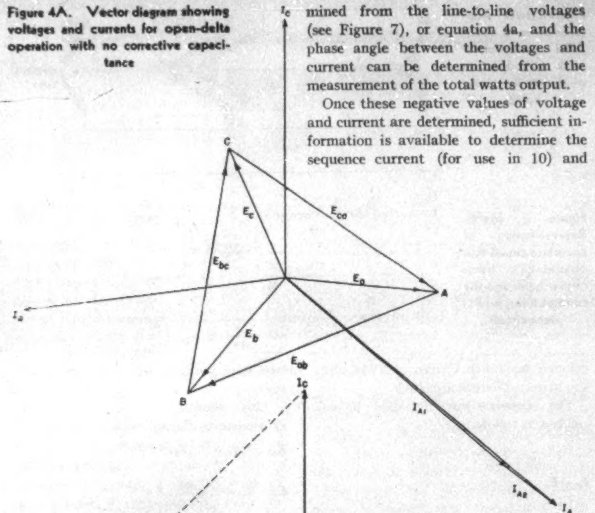
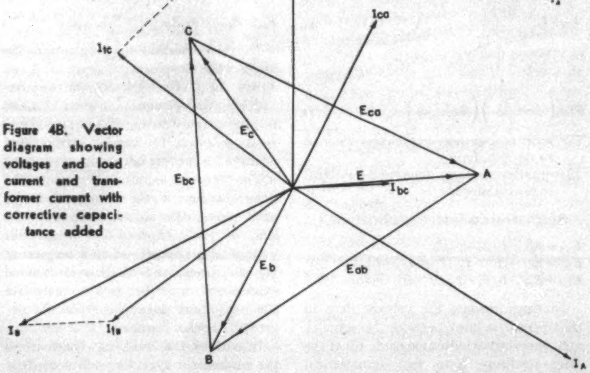


Figure 4B. Vector diagram showing voltages and load current and transformer current with corrective capacitance added



sequence parts will yield the conventional form[2] given for the calculation of the regulation of open-delta transformer banks.

Assume three voltage values E_{ab}, E_{bc}, and E_{ca}, together with three current values I_A, I_B, and I_C, have been measured. Then we can write (see Figure 7)

$$\begin{aligned}\mathbf{E}_{ab}&=E_{ab} & \mathbf{I}_A&=I_A\\ \mathbf{E}_{bc}&=E_{bc}\epsilon^{-j(180-ab)} & \mathbf{I}_B&=I_B\epsilon^{-j(180-AB)}\\ \mathbf{E}_{ca}&=E_{ca}\epsilon^{j(180-ca)} & \mathbf{I}_C&=I_C\epsilon^{j(180-CA)}\end{aligned} \qquad (11)$$

in which the angles are determined as indicated in Figure 7, but it should be noted fully that the voltage angle ***ab*** need not be the same necessarily as the current angle ***AB***. The line-to-neutral voltages, $\mathbf{E}_a$, $\mathbf{E}_b$, and $\mathbf{E}_c$, may be determined from the line-to-line voltages (see Figure 7), or equation 4a, and the phase angle between the voltages and current can be determined from the measurement of the total watts output.

Once these negative values of voltage and current are determined, sufficient information is available to determine the sequence current (for use in 10) and then to determine the per-unit impedance voltage (12 and 12a) in magnitude and phase angle, to be used in the calculation of regulation. On the other hand, if the constants of the circuit are known, the sequence currents may be evaluated by use of equations 6 and 7.

New Formula for Precise Calculation of Transformer Regulation

Some of the conventional approximate formulas[3] for regulation are subject to appreciable error when the per-unit drop is above the usual values. Accordingly, the following new method of calculating regulation is offered, which permits calculation to any desired precision.

Express one of the foregoing expressions as:

$$\begin{aligned}E_s&=E_p-(I_{b1}Z_t\epsilon^{j(\phi+\theta_1+30)}+I_{b2}Z_t\epsilon^{j(\phi+\theta_2-30)})\\ &=E_p-E_{ps}\epsilon^{j\theta}\end{aligned} \qquad (12)$$

anb then, if we let

$$p=\frac{E_{ps}}{E_s} \qquad (12a)$$

and then

$$\frac{E_p}{E_s}=1+p\epsilon^{j\theta}$$

there results

$$\frac{E_p}{E_s}=1+p\cos\theta+jp\sin\theta \qquad (13)$$

Per-unit regulation,

$$r=\frac{E_p-E_s}{E_s}=\frac{E_p}{E_s}=1$$

so that from equation 13

$$r=a-\frac{a^2}{2}+\frac{a^3}{2}-\frac{5}{8}a^4+\frac{7}{8}a^5-\frac{21}{16}a^6+\frac{33}{16}a^7-+\ldots \qquad (14)$$

$$a=p\cos\theta+\frac{p^2}{2}$$

The expression $p\epsilon^{j\theta}=p\ (\sin\theta+j\cos\theta)$ represents the per-unit impedance drop in magnitude and position. For this particular problem the proper values to use are obtained from equation 10.

By this method the regulation may be calculated to any precision justified by the supporting data, and this method has been incorporated by the writer in the latest revision of American Standard for Transformers, *C*-57.1, and was included in the report of the subcommittee responsible for the revision of section 4 of *C*-57.

Calculation of Regulation

At this point the regulation for the open-delta condition of case 2 of Figure 4 of the Wolf–Mattison paper will be

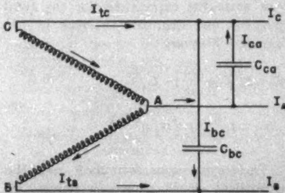


Figure 5. Secondary side of open-delta transformer bank showing phases to which corrective capacitance is added

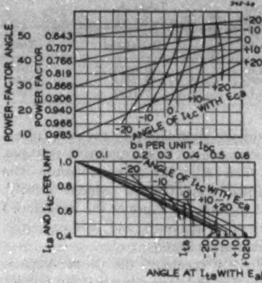


Figure 6A. Chart showing relationship between the amount of corrective capacitance added, the resultant current in the transformer, and the phase angle between the transformer current and the transformer voltages

For $n=\frac{I_{ca}}{I_{bc}}=1^{1}/_{3}$

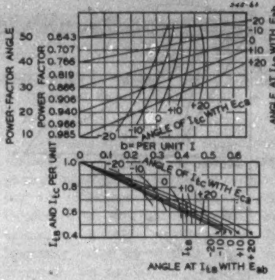


Figure 6B. Chart showing relationship between the amount of corrective capacitance added, the resultant current in the transformer, and the phase angle between the transformer current and the transformer voltages

For $n=\frac{I_{ca}}{I_{bc}}=2$

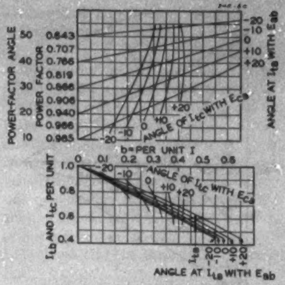


Figure 6C. Chart showing relationship between the amount of corrective capacitance added, the resultant current in the transformer, and the phase angle between the transformer current and the transformer voltages

For $n=\frac{I_{ca}}{I_{bc}}=2^{1}/_{2}$

calculated. The per-unit impedance of the transformer is 0.0592 at 522 amperes, and the impedance angle ϕ is 82.52 degrees. (All numerical values of angles are in degrees.)

Then

$$I_1=(1,230-j8.7)\epsilon^{-j35.29}=1,230\epsilon^{-j35.70}$$
$$I_2=(270+j8.7)\epsilon^{-j35.29}=270.1\epsilon^{-j33.44}$$
$$Z_t=0.0592\epsilon^{j82.52}$$

In the preceding equation for I_1 and I_2, the reference vector is E_a, whereas in equation 10 the reference vector is E_A. The angle between E_A and E_a may be found from the following:

$$\tan\beta=\frac{I_AZ_t\sin(\phi+\theta)}{3+I_AZ_t\cos(\phi+\theta)}$$

which gives

$\beta=0.78$

and then $\theta_1=36.48$, and $\theta_2=34.22$, so that

$\phi+\theta_1+30=76.04$

and

$\phi+\theta_2-90=41.70$

$\phi+\theta_1=46.04$

and

$\phi+\theta_2=48.3$

$\phi+\theta_1-30=16.04$

and

$\phi+\theta_2+90=138.30$

$\frac{1,230}{522}\times0.0592=0.1395$

$\frac{270.1}{522}\times0.0592=0.03064$

Then for phase AB

$$p\epsilon^{j\theta}=0.1395\,\epsilon^{j76.04}+0.03064\epsilon^{-j41.70}$$
$$=0.0566+j0.115$$
$$a=0.0648$$

Per-unit regulation $=0.0628$ for phase AB

And in a similar manner for the other phases there results the data shown in Table A.

This is a reasonably good check. The per-unit values are all on the basis of the rated output voltage—575 volts.

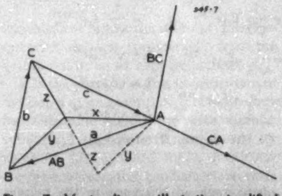


Figure 7. Vector diagram illustrating simplified method of finding line-to-neutral voltage and the positive- and negative-sequence components from a set of unbalanced line-to-line voltages, when no zero-sequence voltage is present

Analysis of Data, Figure 4 of the Wolf–Mattison Paper

In Table I is shown an analysis of the data in Figure 4 of the Wolf–Mattison paper, giving the measured and certain derived values. The phase angle between E_a and I_A is predicated upon the requirement that the calculated output of the two transformers must equal the measured value. This information is shown in Figures 4a and 4b.

The positive- and negative-sequence components for the open-delta operation, Figure 4a, are without capacitance.

$E_{a1}=319.4-j16.38$
$I_{A1}=(1,230-j8.7)\epsilon^{-j35.29}$
$E_{a2}=9.4+j16.83$
$I_{A2}=(\ 270+j8.7)\epsilon^{-j35.29}$

and with corrective capacitance added

$E_{a1}=344.3-j3.31\qquad I_{A1}=1,166.5\epsilon^{-j36.50}$

$E_{a2}=0.85+j3.31\qquad I_{A2}=33.5\epsilon^{-j36.50}$

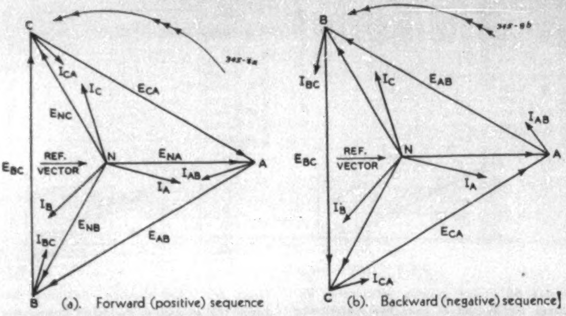
$$\cos ab=\frac{a^2+b^2-c^2}{2ab}\qquad \cos xy=\frac{x^2+y^2-z^2}{2xy}\qquad \cos cx=\frac{c^2+x^2-z}{2cx}$$

Other angles in similar manner. All angles acute.

$$X^2=\frac{1}{9}(2a^2-b^2+2c^2),\ a^2=2x^2+2y^2-z^2,\text{ and so forth}$$

$$E_{AB}=E_{AB}\qquad E_a=E_a\qquad E_{AB}=E_{AB}\epsilon^{-j(180-xa)}$$
$$E_{BC}=E_{BC}\epsilon^{-j(180-ab)}\qquad E_b=E_b\epsilon^{-j(180-xy)}\qquad E_{BC}=E_{BC}\epsilon^{j(xa+ab)}$$
$$E_{CA}=E_{CA}\epsilon^{j(180-ca)}\qquad E_c=E_c\epsilon^{j(180-zx)}\qquad E_{CA}=E_{CA}^{-}\epsilon^{j(ca-xa)}$$

$$E_{AB1}=\frac{1}{2}E_{AB}+j\frac{1}{2\sqrt{3}}(E_{BC}-E_{CA});E_{AB2}=\frac{1}{2}E_{AB}-j\frac{1}{2\sqrt{3}}(E_{BC}-E_{CA})$$
$$E_{a1}=\frac{1}{2}E_{AB}+j\frac{1}{2\sqrt{3}}(E_B-E_C);E_{a2}=\frac{1}{2}E_a-j\frac{1}{2\sqrt{3}}(E_B-E_C)$$



(a). Forward (positive) sequence (b). Backward (negative) sequence

Figure 8. Chart showing certain sequence relationships used in the text

Positive Sequence

$$E_a = E_a\epsilon^{j0} = E_a \qquad I_a = \qquad = I_a\epsilon^{j\theta}$$

$$E_b = E_a\epsilon^{-j120} = a^2E_a \qquad I_b = I_a\epsilon^{j(\theta-120)} = a^2I_a\epsilon^{j\theta}$$

$$E_c = E_a\epsilon^{-j240} = aE_a \qquad I_c = I_a\epsilon^{j(\theta-240)} = aI_a\epsilon^{j\theta}$$

$$E_{ab} = (-1+a^2)E_a = \sqrt{3}E_a\epsilon^{-j150} \qquad I_{ab} = \frac{I_a}{-1+a} = \frac{1}{\sqrt{3}}I_a\epsilon^{j(\theta-150)}$$

$$E_{bc} = (-a^2+a)E_a = \sqrt{3}E_a\epsilon^{j90} \qquad I_{bc} = \frac{I_a}{-a+a^2} = \frac{1}{\sqrt{3}}I_a\epsilon^{j(\theta+90)}$$

$$E_{ca} = (-a+1)E_a = \sqrt{3}E_a\epsilon^{-j30} \qquad I_{ca} = \frac{I_a}{-a^2+1} = \frac{1}{\sqrt{3}}I_a\epsilon^{j(\theta-30)}$$

$$E_a = \frac{1}{\sqrt{3}}E_{ab}\epsilon^{-j150} \qquad I_a = \sqrt{3}I_{ab}\epsilon^{j\theta}$$

$$E_b = \frac{1}{\sqrt{3}}a^2E_{ab}\epsilon^{j150} \qquad I_b = \sqrt{3}a^2I_{ab}\epsilon^{j\theta}$$

$$E_c = \frac{1}{\sqrt{3}}aE_{ab}\epsilon^{j150} \qquad I_c = \sqrt{3}aI_{ab}\epsilon^{j\theta}$$

Use subscript 1, for example, E_{a1}

Negative Sequence

$$E_a = E_a\epsilon^{j0} = E_a \qquad I_a = \qquad = I_a\epsilon^{j\theta}$$

$$E_b = E_a\epsilon^{-j240} = aE_a \qquad I_b = I_a\epsilon^{j(\theta-240)} = aI_a\epsilon^{j\theta}$$

$$E_c = E_a\epsilon^{-j480} = a^2E_a \qquad I_c = I_a\epsilon^{j(\theta-480)} = a^2I_a\epsilon^{j\theta}$$

$$E_{ab} = (-1+a)E_a = \sqrt{3}E_a\epsilon^{j150} \qquad I_{ab} = \frac{I_a}{-1+a^2} = \frac{1}{\sqrt{3}}I_a\epsilon^{j(\theta+150)}$$

$$E_{bc} = (-a+a^2)E_a = \sqrt{3}E_a\epsilon^{-j90} \qquad I_{bc} = \frac{I_a}{-a^2+a} = \frac{1}{\sqrt{3}}I_a\epsilon^{j(\theta-90)}$$

$$E_{ca} = (-a^2+1)E_a = \sqrt{3}E_a\epsilon^{j30} \qquad I_{ca} = \frac{I_a}{-a+1} = \frac{1}{\sqrt{3}}I_a\epsilon^{j(\theta+30)}$$

$$E_a = \frac{1}{\sqrt{3}}E_{ab}\epsilon^{j150} \qquad I_a = \sqrt{3}I_{ab}\epsilon^{j\theta}$$

$$E_b = \frac{1}{\sqrt{3}}aE_{ab}\epsilon^{-j150} \qquad I_b = \sqrt{3}a\epsilon^{j\theta}$$

$$E_c = \frac{1}{\sqrt{3}}a^2E_{ab}\epsilon^{-j150} \qquad I_c = \sqrt{3}a^2E^{j\theta}$$

Use subscript 2, for example, E_{a2}

In these expressions E_a is the reference vector.

From these data it is seen that open-delta operation in this particular case introduces 6 per cent negative-sequence voltage, which in turn introduces a 22 per cent negative-sequence current.

In Figure 4b the voltage and load currents are seen to be closely balanced, and this has been accomplished by the addition of capacitance current in certain proportions (see Figure 5) as described by Wolf and Mattison.

The current I_B in the leading phase *AB* has been advanced slightly in phase (Figure 4b), and thus its phase angle is reduced in reference to the voltage *AB*, and at the same time it is reduced in magnitude. The current I_C in the leading phase has been advanced greatly in phase, and this also reduces its (reversed) phase angle in reference to the voltage *CA* and, at the same time, its magnitude. After this modification has been made, current I_{bt} leads its voltage E_{ab} by 1.99 degrees and the current I_{ct}, reversed, lags its voltage E_{ca} by 13.19 degrees. Since the power factor is now practically unity, the difference in regulation is so slight that the negative-sequence voltage introduced has a negligible effect.

The relative values of capacitance current that need be added will depend upon the power factor and magnitude of the load. The required amount and its distribution cannot be determined readily by a study of the unbalanced condition. Rather the line currents I_B and I_C *for balanced three-phase conditions* must be modified so that the respective transformer currents, I_{tB} and I_{tC}, are not only approximately equal in magnitude, but also have approximately the same phase relationship to their respective voltages, E_{ab} and E_{ca}. By this procedure the unequal regulation is reduced greatly, and thereby the cause of the unbalanced current is removed and, consequently, its harmful effects.

Table A

Phase	Calculated	Test
AB	0.0628	0.0765
BC	0.1407	0.146
CA	0.1120	0.1373

Strictly speaking, the proper capacitance for one power factor is not correct at other power factors. In Figure 6 are given curves (see also Figure 5), by which the amount of corrective capacitance may be estimated, and it should be borne in mind that precision is not necessary.

In these curves *b* is the per-unit capacitive current, based on actual load current, added across phase *bc*, and *nb* represents the capacitive current added across phase *ca*, with *n* having values of $1^2/_3$, 2, and $2^1/_3$.

These charts are used as follows: Assume the power factor of the load is 0.77, assume *b* to be 0.35 as the first trial, and *n* as $1^2/_3$. Then the intersection of the line for power factor = 0.77 and *b*=0.35 indicates an angle of about +1.5 degrees for I_{tC} and of about −24 degrees for I_{tB} on the upper chart. Now locate these angles respectively on the lower chart for the assumed values of *b*=0.35; I_{tC} is indicated as about 0.71, and I_{tB} is indicated as about 0.65. This amount of correction probably would not be satisfactory, and a new trial should be made with a different *b* or different *n*, or with both different.

For a further example, assume a load having a power factor of 0.81 or θ=36 degrees. Then we can get the results from the curves as shown in Table II.

With the possible exception of the first entry, any of the foregoing will give sufficient reduction in the unbalance to assure satisfactory operation of the induction motors on the secondary circuits. Incidentally, the fourth entry

Table II

n	*b*	θ_{tB}	θ_{tC}	I_{tB}	I_{tC}
$1^2/_3$	0.3	+4	−21	0.730	0.725
$1^2/_3$	0.35	+6	−13	0.690	0.710
$1^2/_3$	0.4	$+8^1/_2$	− 3	0.655	0.755
2	0.3	+4	$-13^1/_2$	0.730	0.700
2	0.35	+6	− 3	0.690	0.730
2	0.4	$+8^1/_2$	$+ 6^1/_2$	0.655	0.750
$2^1/_3$	0.3	+4	$- 5^1/_2$	0.730	0.665
$2^1/_3$	0.35	+6	+ 4	0.690	0.640
$2^1/_3$	0.4	$+8^1/_2$	$+15^1/_2$	0.655	0.660

Skin Effect in Bimetallic Conductors

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Synopsis: Mathematical expressions for a-c impedance and current distribution are derived for bimetallic conductors with circular symmetry. The results are illustrated by application to a copper-covered steel conductor in the frequency range up to 160 kilocycles. Measurements of the a-c resistance in the same frequency range provide a check on the theoretical results. The studies made on the copper-covered steel conductor indicate that its resistance is substantially that of its tubular copper portion alone and, for all but the lowest frequencies, may be taken from the simpler formulas and curves already available for tubes.

BIMETALLIC conductors consisting of a copper covering welded to a steel core are widely used for power and communication lines, because the great tensile strength of these conductors permits long pole spans. The resistance of such bimetallic conductors at power frequencies is known.[1] A knowledge of the resistance at higher frequencies, in the range up to 150 kilocycles, has become important for designing and for determining the performance of carrier-current communication systems operated over bimetallic conductors.

A previous study[2] of this subject was published in 1915 by J. M. Miller, who gave an approximate method of calculating a-c resistance and compared calculated with experimental results for several specimens of conductors in the frequency range up to three kilocycles. However, his method of calculation is based upon the assumption that the current density in the outer layer of copper is uniform, which, although it is an excellent approximation at low frequencies, departs considerably from the actual condition at high frequencies.

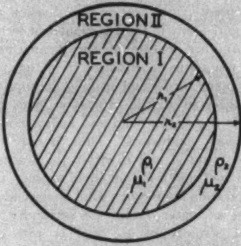

Figure 1. Section of bimetallic conductor

It is the purpose of this investigation to develop a more exact theory of skin effect for bimetallic conductors with circular symmetry, to illustrate the theory by using it to calculate for a particular copper-covered steel conductor the ratio of a-c resistance to d-c resistance and the current distribution over a range of frequencies, and to check the theory by an experimental determination of the resistance ratio for the same conductor over the selected range.

Theoretical Expression for A-C Impedance

In what follows, a general relation is developed for the a-c impedance of a conductor which has the section shown in Figure 1. The interior region I has resistivity ρ_1 and permeability μ_1; the exterior region II, ρ_2 and μ_2. The developed relation applies to all other bimetallic conductors of circular section, including aluminum-covered steel, as well as to copper-covered steel.

In developing the theory it is assumed that:

1. Each region is homogeneous; that is, within each region the permeability and resistivity do not vary from point to point.
2. At any point the permeability and resistivity do not vary with time.
3. The bounding surface between regions and the outer surface are circular cylinders.
4. The conductor carries a sinusoidal current.
5. The conductor is very long and straight and the return conductor or conductors are either concentric tubes or at such remote distances that they produce no appreciable field within the conductor considered.

The derivation of the expression for the ratio of a-c impedance to d-c resistance is outlined in Appendix A. The expression itself, in convenient form for calculation, is:

$$\frac{Z_{ac}}{R_{dc}}=\frac{br_2}{2j}P\,\mathrm{conj}\left[\frac{A-\dfrac{F}{H}C-IGA+\dfrac{IE}{H}C}{B-\dfrac{F}{H}D-IGB+\dfrac{IE}{H}D}\right] \quad (1)$$

in which P is the per-unit conductivity (per cent conductivity divided by 100), and each of the nine quantities A to I is a Bessel function expressed as a complex number, except for G and I, which are quantities simply obtained from Bessel functions. Other forms of Bessel functions may be employed, the present forms being selected for convenience in

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The authors acknowledge the contributions of two previous Buhl research fellows: H. E. Webb, Jr., who made a preliminary experimental study of the subject in his master of science thesis, and A. G. Williamson who made numerous measurements and aided in the development of the experimental technique. The conductors used in the tests were supplied by the Copperweld Steel Company.

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is the case covered by Figure 4 of the Wolf–Mattison paper, and the check is very good. In selecting the particular values of corrective capacitance the power factor and range of load are the factors to be considered.

Appendix

1. If there is no zero-sequence voltage in the system, then the proper neutral point of the system is the intersection of the medians of the closed line-to-line voltage triangle. This can be proved easily by geometric relationships and is true, because the line-to-neutral voltages must form a closed triangle of themselves and therefore fix the line-to-line triangle in such a way that any line-to-neutral voltage if extended, will bisect the opposite side. These relationships are set forth in Figure 7, which gives a direct method of calculating the line-to-neutral voltages from the line-to-line voltages when there is no zero-sequence voltage. Also, a more direct method of determining the positive- and negative-sequence components is given.

2. In Figure 8 are shown certain sequence relationships to which reference was made in the test.

References

1. Mobile Capacitor Units for Emergency Loading of Transformers in Open Delta, H. B. Wolf, G. G. Mattison. AIEE Transactions, volume 62, 1943, February section, pages 83–6.

2. Transformer Engineering (book). John Wiley and Sons, New York, N. Y. Page 181.

3. American Standard for Transformers, *C*-57.1.

Table I. Equivalent Forms of Bessel Functions

$I_0(j^{3/2}z)=\text{ber } z+j\text{ bei } z$	$=$	$=J_0(j^{3/2}z)$	$=\text{conj } J_0(j^{1/2}z)$	$=M_0\epsilon^{j\theta_0}$
$I_1(j^{3/2}z)=\frac{1}{j}(\text{ber}_1 z+j\text{ bei}_1 z)$	$=\frac{1}{j^{1/2}}(\text{ber}' z+j\text{ bei}' z)$	$=\frac{1}{j^{1/2}}\frac{d}{dz}J_0(j^{3/2}z)$	$=j\text{ conj } J_1(j^{1/2}z)$	$=\frac{1}{j}M_1\epsilon^{j\theta_1}$
$K_0(j^{1/2}z)=\text{ker } z+j\text{ kei } z$	$=$	$=$	$=\frac{\pi}{2j}\text{ conj } H_0{}^1(j^{1/2}z)$	$=N_0\epsilon^{j\varphi_0}$
$K_1(j^{1/2}z)=j(\text{ker}_1+j\text{ kei}_1 z)$	$=j^{1/2}(\text{ker}' z+j\text{ kei}' z)$	$=$	$=-\frac{\pi}{2}\text{ conj } H_1{}^1(j^{1/2}z)$	$=jN_1\epsilon^{j\varphi_1}$

See references 3, 6, and 7.

using the Jahnke–Emde tables.[3] The quantities are defined as follows:

$$\left.\begin{array}{ll} \omega=2\pi f & A=J_0(j^{3/2}br_2) \\ j=\sqrt{-1} & B=j^{3/2}J_1(j^{3/2}br_2) \\ & C=H_0{}^{(1)}(j^{3/2}br_2) \\ a=\sqrt{\dfrac{\omega\mu_1}{\rho_1}} & D=j^{3/2}H_1{}^{(1)}(j^{3/2}br_2) \\ & E=J_0(j^{3/2}br_1) \\ b=\sqrt{\dfrac{\omega\mu_2}{\rho_2}} & H=H_1{}^{(1)}(j^{3/2}br_1) \\ F=J_1(j^{3/2}br_1) & \\ G=\dfrac{H_0{}^{(1)}(j^{3/2}br_1)}{H_1{}^{(1)}(j^{3/2}br_1)} & \\ I=\sqrt{\dfrac{\mu_2\rho_2}{\mu_1\rho_1}}\times\dfrac{J_1(j^{3/2}ar_1)}{J_0(j^{3/2}ar_1)} & \\ P=1-\left[\dfrac{r_1}{r_2}\right]^2\left[1-\dfrac{\rho_2}{\rho_1}\right] & \end{array}\right\} \qquad (2)$$

In the above expressions the meter-kilogram-second system of units[1] is employed.

The real component of Z_{ac}/R_{dc} is the ratio of a-c to d-c resistance; the imaginary component is the ratio of the reactance arising from the flux within the conductor to the d-c resistance.

The first terms of the numerator and denominator, A and B, are the largest terms for the cases that have been calculated and can be taken from the tables directly in Cartesian form. The other terms within the brackets, which are products and quotients, are obtained conveniently in polar form. Thus the arrangement in equation 1 is such that accuracy is not lost in the most important terms by writing them as the products of various factors, each of which, taken from a table, has some uncertainty in the last decimal place. Also if a slide rule is to be used, multiplication and division are required only in the less significant terms.

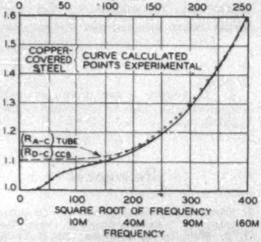

Figure 2. Ratio of a-c to d-c resistance-number 10 American wire gauge copper-covered steel, 40 per cent conductivity wire at 29 degrees centigrade

Another feature of the arrangement of the terms in equation 1 is that, by dropping terms and making appropriate changes in P, the expression gives the ratio Z_{cc}/R_{dc} for solid and hollow homogeneous conductors. Thus, if only the first terms A and B within the brackets are used, and if P is set equal to unity, the expression applies to a solid homogeneous wire of outer radius r_2, resistivity ρ_2, and permeability μ_2: that is, with the characteristics of the outer material. Likewise, if only the first two terms of the numerator and the first two terms of the denominator are used with P corresponding to an infinite value of ρ_1, the expression applies to a tubular conductor of inner and outer radii r_1 and r_2 made of the outer material.

Application of the Theory

To illustrate the use of equation 1 and to obtain calculated values of the ratio of R_{ac} to R_{dc} for comparison with experimental ones, the equation is applied to a particular specimen of number 10 American wire gauge copper-covered steel 40 per cent conductivity wire supplied by the Copperweld Steel Company.

The quantities r_1, r_2, ρ_1, ρ_2, and μ_1 were carefully determined for the conductor used. The value of r_2, the outer radius, was found from the average of a large number of micrometer measurements; ρ_1, the resistivity of the steel core material, was found from Kelvin-bridge measurements after the copper was removed chemically from the composite wire; ρ_2, the resistivity of the copper sheath, was taken from tables. The value of r_1 was calculated from the measured d-c resistance of the composite conductor and the previously determined values of r_2, ρ_1 and ρ_2. The permeability μ_1 of the core was determined from inductance measurements. For this purpose short lengths of the steel core were drilled and arranged for stringing, as cylindrical beads, on a small insulated wire with coaxial return. The inductance of the wire with and without the steel beads was measured by means of an inductance bridge, and from these measurements and the dimensions the permeability was calculated.

The following values were found:

$r_1=0.1024\times10^{-2}$ meter
$r_2=0.1303\times10^{-2}$ meter

$\rho_1=27.91\times10^{-8}$ ohm-meter
$\rho_2=1.830\times10^{-8}$ ohm-meter

$\mu_1=78.8\times4\pi\times10^{-7}$
$\mu_2=1.0\times4\pi\times10^{-7}$

The calculated ratio of a-c to d-c resistance as a function of frequency for a conductor with the above constants is shown by the solid curve in Figure 2. Values of frequency were chosen that would minimize the necessity for interpolation in the tables of Bessel functions.

When the scale of abscissas is expressed in terms of $\sqrt{f/R_{dc}}$ the curve of Figure 2 will apply to a conductor of any size, provided it is made of the same materials as the one for which the figure was calculated and has the same ratio of r_1 to r_2. These conditions also imply the same per cent conductivity. Such a scale is given

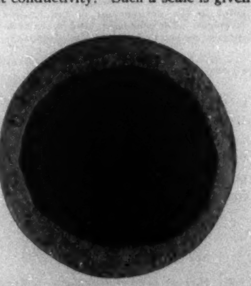

Figure 3. Photomicrograph of section of copper-covered steel wire

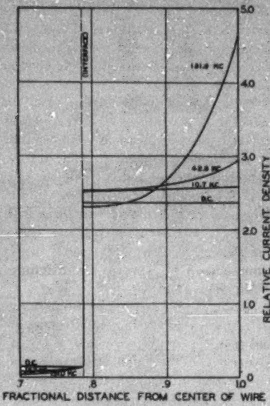

Figure 4. Variation of current density with distance from center of wire

at the top of the figure, R_{1000} being the d-c resistance in ohms per thousand feet.

Experimental Verification

The points in Figure 2 were obtained experimentally by the method described in Appendix B.

The agreement between calculation and experiment is everywhere at least within $1^1/_2$ per cent, at most frequencies is closer, and thus is well within most commercial tolerances. The experimental results are believed to be accurate to one quarter of one per cent; the calculations to one tenth of one per cent. The small discrepancies between calculated and test values are thought to have their origin in the departure of the actual conductor from the assumptions made in the analysis. In the particular specimen studied the section of the steel core is not exactly circular as may be seen from the photomicrograph of Figure 3. Also, micrometer measurements of the steel core, from which the surrounding copper had been removed chemically, showed a section of roughly elliptic form of major axes 0.075 and 0.085 inch. Another important departure from the assumptions lies in the nonhomogeneity of the copper. In the manufacture of copper-covered steel conductors, molten copper is poured around a steel core, and the resulting billet is rolled into rod and then drawn into wire. There is evidence that some iron migrates into the copper at the junction of the two metals, and the presence of even small amounts of iron causes an increase in the resistivity of the copper, this increase, however, probably being restricted to the immediate neighborhood of the interface.

Current Distribution

The current distribution over the section of the conductor, that is, the current density as a function of radial distance, is given by expressions that are derived in Appendix A. The result is shown in Figure 4, in which there is given a curve proportional to the magnitude of current density for frequencies of zero, 10.7, 42.8, and 131 kilocycles. Current densities are shown in only the outer portion of the conductor, the current density being small and unimportant in the interior regions at most frequencies. The curves are drawn to show the relative current densities on the assumption that the total current is the same at each of the four frequencies. Thus, as the frequency is increased, the current density in the interior falls with respect to that at the outer surface, and, to maintain the same total current, the density at the outside surface must increase, and this change results in a larger voltage drop per-unit length. In fact, the impedance drop per-unit length, exclusive of that part originating in flux linkages outside of the conductor, is proportional to the current density at the outside surface. Since the current is assumed to be the same for all frequencies Z_{ac}/R_{dc} is also proportional to this density, shown by the ordinate at extreme right of Figure 4.

The scale of ordinates for Figure 4 is chosen so that if the total current were distributed uniformly over both the steel and copper of the conductor, unit current density would result. In terms of this unit, at zero frequency the current density in the steel is 15.5 per cent, in the copper 236 per cent, the average, weighted with respect to area, being 100 per cent. On the other hand, at the highest frequency of the figure, 131 kilocycles, the current density at the center is substantially zero, on the steel side of the interface it is 15 per cent (nearly the same as before), on the copper side of the interface 231 per cent, and at the outside surface 464 per cent. If the current densities were averaged over the area of the conductor and account were taken of the variation of phase as well as magnitude, the result would again be 100 per cent.

The regularity of the changes with increase of frequency is not apparent from Figure 4; therefore, curves, showing the magnitude of the current density G_2 at the outside and G_1 at the copper side of interface, as functions of frequency are given in Figure 5, in which the current densities are expressed in terms of the average current density as used in Figure 4. Figure 5 also gives the phase of the current density at the interface and at the outer surface with respect to the total current in the conductor. For the interface the angles are lagging; for the surface, leading.

Since the steel carries but a small fraction of current, the most convenient criterion of the extent of skin effect is the ratio of the extreme magnitudes of current density in the copper; that is, of the ratio of interface density to surface density. This is given in Figure 6, together with another curve showing the angle by which the interface current density lags the surface density. Thus, as is shown in the figure, the change of phase from the inside to the outside of the copper is zero for direct current, 10 degrees for 10.3 kilocycles, and 82 degrees for 131 kilocycles. The corresponding change of phase, not shown in the figure, from the interface to the center of the steel core is zero for direct current, 182 degrees for 10.3 kilocycles, and 727 degrees for 131 kilocycles.

Figures 4, 5, and 6 are expressed in terms of frequency or its square root and apply to the specific conductor under consideration. However, they, like Figure 2, could be generalized to apply to any other conductor of different radius but of the same materials and with the same per cent conductivity.

The study of current distribution throws light upon the reasons for the small departures of the theoretical curve from

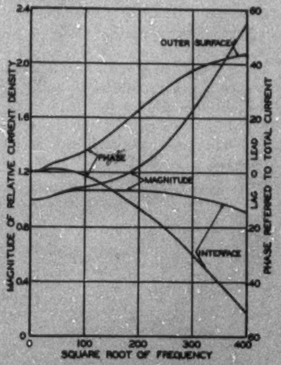

Figure 5. Magnitude and phase of current densities at interface and outer surface

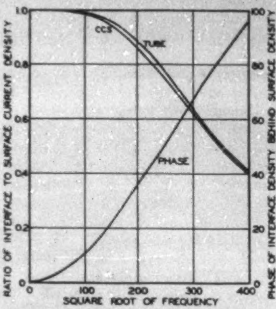

Figure 6. Ratio of interface to surface current density, and corresponding phase difference

the experimental points in Figure 2. The fact that these departures occur at intermediate frequencies rather than at the extremes indicates that they probably are caused by inaccuracies associated with the interface. At zero frequency there is no error, because the constants are made to agree with d-c measurements. At low frequencies, below ten kilocycles, the rise in the resistance ratio is caused by a decrease of current density in the steel core. The decrease is greatest at the center, and there is little change at the interface or in the copper; thus, conditions at the center determine the shape of the curve at the low frequencies. On the other hand, at the highest frequencies, 100 to 160 kilocycles, the current density in the steel is negligible, and that in the interface region is overshadowed by the higher current densities at the outside surface; thus, conditions at the outside surface are predominant in determining the curve at the highest frequencies. However, at intermediate frequencies there is appreciable current density on both sides of the interface, and it is the change of this density with frequency that determines the shape of the resistance ratio curve. Thus, at the intermediate frequencies the geometric irregularity and interfacial rise in the copper resistivity would have their greatest effect. The small discrepancies at the highest frequencies may be caused by the lack of agreement between the table value and the actual value of the resistivity of copper. At these frequencies inaccuracies in the constants ρ_1 and μ_1 of the iron have but little effect on the calculated values of R_{ac}/R_{dc}.

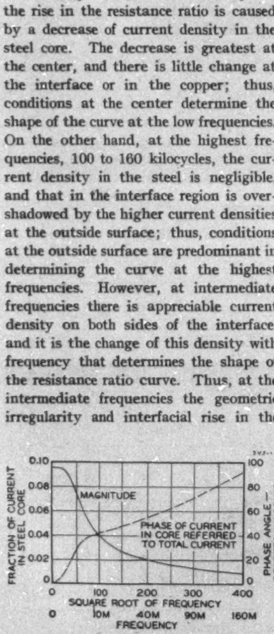

Figure 7. Magnitude and phase of the fraction of total current carried by steel core

Approximate Method of Determining A-C Resistance

The fact that but a small portion of the current flows in the steel core of the conductor in the special case studied, only 9.5 per cent even at zero frequency, suggests a simple approximate method of determining the a-c resistance of such a conductor, a method in which the current in the steel is neglected entirely and the resistance of the copper-covered steel conductor taken as the resistance of the tubular copper shell alone. This latter resistance is well known, is obtainable from relatively simpler formulas,[4] and is to be found conveniently in general curves.[4,5]

The accuracy of this method depends upon two conditions: that the current in the steel core contributes but little to the total current, and also that it has a negligible effect on the current distribution in the copper.

The fraction of the total current that is carried in the steel core is shown in Figure 7; also is shown the lagging phase angle of the total current in the core behind the phase angle of the current caused by the conductor as a whole. Starting at 10.5 per cent of the total current at zero frequency, the core current falls to 4 per cent at 10 kilocycles and to 1 per cent at 150 kilocycles. Meanwhile the phase angle of the core current behind the total current increases rapidly from zero to about 40 degrees in the first 10 kilocycles, and then more slowly to 90 degrees in the range from 10 to 150 kilocycles.

For the special conductor studied, the current in the core has a negligible effect upon the current distribution in the copper. Removing the core would cause a discernible change of current density only near the interface and this change is shown by the dashed curve in Figure 6. The difference in the phase change through the copper caused by removing the core is too small to show in Figure 6.

Finally, the total effect of treating the copper-covered steel conductor as a copper tube alone is shown in Figure 2, where the a-c resistance of the tube is referred to the d-c resistance of the composite conductor. Above 10 kilocycles there is less than four per cent difference between the a-c resistances of composite conductor and tube, above 20 kilocycles the approximate treatment is as good as the accurate one.

Thus, for many purposes it may be sufficiently accurate to treat the copper-covered steel conductor as a copper tube at the higher frequencies. The d-c resistance is known, and it should be possible to estimate a transition curve from this value to the curve for the tube with an error of only a few per cent.

Comparison of Several Conductors

Finally, it is of interest to compare the a-c resistances of the bimetallic conductor and the copper tube with that of a solid copper wire of the same outside diameter. This is done in Figure 8, which gives the ratios of the a-c resistances of the respective conductors to the d-c resistance of the copper-covered steel. Thus the ordinates are also proportional to the a-c resistances of the conductors for any common length. At zero frequency the resistance of the solid wire is 42 per cent, that of the tube 111 per cent of the resistance of the copper-covered steel. As the frequency increases, the resistance of the solid wire increases most rapidly, and at 85 kilocycles all conductors have approximately the same a-c resistance. At frequencies above 85 kilocycles the solid wire has the largest resistance as shown on the curve, but at still higher frequencies beyond the range of Figure 8 the curves would again coincide, when the current in all cases would be confined to a thin skin at the outermost boundary. From zero to 85 kilocycles the copper-covered steel has a lower resistance than the copper tube, but, between 85 and 160 kilocycles, the latter frequency being the highest calculated, the copper-

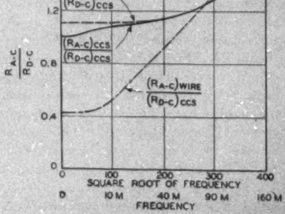

Figure 8. Comparison of a-c resistance of copper wire, copper tube, and copper-covered steel conductor of same outside diameter

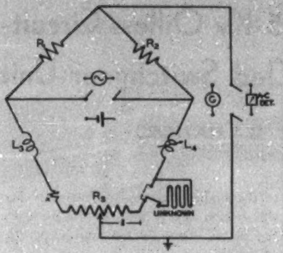

Figure 9. Circuit for measuring resistance ratio

covered steel has a slightly higher resistance than the tube alone.

Appendix A. Derivation of Equations

The current density in each of the regions must satisfy the differential equation

$$\frac{d^2G}{dr^2}+\frac{1}{r}\frac{dG}{dr}-j\frac{\omega\mu}{\rho}G=0 \tag{3}$$

in which G is a complex number representing the magnitude and phase of the current density at any distance r from the axis. The derivation of the equation may be found in numerous treatments of skin effect, for example, the textbook by Woodruff.[1] The solution must also satisfy the boundary conditions:

1. At $r=0$, G remains finite.
2. At $r=r_1$, ρG is continuous.
3. At $r=r_1$, $(\rho/\mu)(dG/dr)$ is continuous.

The last two conditions come from the field equation

$$\text{Curl }\mathbf{E}=\frac{\partial \mathbf{B}}{\partial t} \tag{4}$$

which for spatial circular cylindrical symmetry, axially flowing currents, and quantities varying sinusoidally in time reduces to

$$\frac{dE}{dr}=-j\omega B \tag{5}$$

E being the electric field, and equal to ρG. Thus

$$\frac{d(\rho G)}{dr}=-j\omega B=-j\omega\mu H \tag{6}$$

Since B is not infinite, ρG must be continuous. On either side of the interface, ρ is constant so

$$\frac{\rho\, dG}{\mu\, dr}=-j\omega H \tag{7}$$

Although B is not necessarily the same on both sides of the interface, H and ω are the same, hence $\rho/\mu\; dG/dr$ is continuous at the interface.

The solution[6] of equation 3 in each of the regions is

$$G_{\text{I}}=C_1I_0(j^{1/2}ar)+C_2K_0(j^{1/2}ar)$$

$$G_{\text{II}}+C_3I_0(j^{1/2}br)+C_4K_0(j^{1/2}br) \tag{8}$$

in which the C_1, C_2, C_3, and C_4 are constants to be determined, and $I_0(j^{1/2}ar)$, $K_0(j^{1/2}ar)$, and so forth, are modified Bessel functions of the first and second kind respectively.

By the first boundary condition $C_2=0$. The other two conditions permit the evaluation of C_3 and C_4 in terms of C_1 which, since $I_0(0)=1$, is the current density at the center. There results

$$G_{\text{I}}=C_1I_0(j^{1/2}ar) \tag{9}$$

$$G_{\text{II}}=C_1j^{1/2}br_1\frac{\rho_1}{\rho_2}I_0(j^{1/2}ar_1)K_1(j^{1/2}br_1)N(br) \tag{10}$$

where $N(br)$ has been introduced as the abbreviation

$$\begin{aligned}N(br)=I_0(j^{1/2}br)+\frac{I_1(j^{1/2}br_1)}{K_1(j^{1/2}br_1)}K_0(j^{1/2}br)+\\ \frac{b\rho_2K_0(j^{1/2}br_1)I_1(j^{1/2}ar_1)}{a\rho_1K_1(j^{1/2}br_1)I_0(j^{1/2}ar_1)}I_0(j^{1/2}br)-\\ \frac{b\rho_2I_1(j^{1/2}ar_1)I_0(j^{1/2}br_1)}{a\rho_1I_0(j^{1/2}ar_1)K_1(j^{1/2}br_1)}K_0(j^{1/2}br)\end{aligned} \tag{11}$$

The current density at the interface is obtained by substitution of r_1 for r in $N(br)$ as it occurs in equations 10 and 11,

$$G_1=C_1j^{1/2}br_1\frac{\rho_1}{\rho_2}I_0(j^{1/2}ar_1)K_1(j^{1/2}br_1)N(br_1) \tag{12}$$

Similarly, at the outer surface where $r=r_2$

$$G_2=C_1j^{1/2}br_1\frac{\rho_1}{\rho_2}I_0(j^{1/2}ar_1)K_1(j^{1/2}br_1)N(br_2) \tag{13}$$

The total current is

$$I=2\pi\int^{r_1}Gr_{\text{I}}dr+2\pi\int_{r_1}^{r_2}Gr_{\text{II}}dr \tag{14}$$

which, upon substitution of equations 9 and 10, integration, and simplification, gives

$$I=2\pi C_1r_1r_2\frac{\rho_1}{\rho_2}I_0(j^{1/2}ar_1)K_1(j^{1/2}br_1)D(br_2) \tag{15}$$

where $D(br)$ is the abbreviation

$$\begin{aligned}D(br)=I_1(j^{1/2}br)-\frac{I_1(j^{1/2}br_1)}{K_1(j^{1/2}br_2)}K_1(j^{1/2}br)+\\ \frac{b\rho_2K_0(i^{1/2}br_1)I_1(j^{1/2}ar_1)}{a\rho_1K_1(j^{1/2}br_1)I_0(j^{1/2}ar_1)}I_1(j^{1/2}br)+\\ \frac{b\rho_2I_1(j^{1/2}ar_1)I_0(j^{1/2}br_1)}{a\rho_1I_0(j^{1/2}ar_1)K_1(j^{1/2}br_1)}K_1(i^{1/2}br)\end{aligned} \tag{16}$$

The constant coefficients of $D(br)$ are the same, except for sign, as the corresponding coefficients of $N(br)$; also in $D(br)$ the variable Bessel functions I_1 and K_1 replace I_0 and K_0, respectively, in $N(br)$. $N(br_2)$ becomes the numerator, and $D(br_2)$ the denominator of the important factor in the expression for the resistance ratio.

In these manipulations the relation

$$I_0(j^{1/2}br)K_0'(j^{1/2}br)-I_0'(j^{1/2}br)K_0(j^{1/2}br)=-\frac{1}{j^{1/2}br} \tag{17}$$

(in which the prime indicates differentiation of the function with respect to the argument) proves to be useful in reducing the number of terms.

The impedance per-unit length (the reactance originating in that part of the flux only which is interior to the conductor) is the voltage drop per-unit length along the surface b divided by the current[6] or

$$Z_{ac}=\frac{\rho_2G_2}{I}=\frac{j^{1/2}\rho_2b}{2\pi r_2}\frac{N(br_2)}{D(br_2)} \tag{18}$$

The d-c resistance is the net resistance of core and tube in parallel, and its reciprocal is

$$\frac{1}{R_{dc}}=\frac{\pi r_2^2}{\rho_2}\left[1-\frac{r_1^2}{r_2^2}\left(1-\frac{\rho_2}{\rho_1}\right)\right]=\frac{\pi r_2^2}{\rho_2}P \tag{19}$$

where P is the per-unit conductivity, the ratio of the conductance of the actual conductor to that of a solid homogeneous conductor of radius r_2 and resistivity of the same length. Thus

$$\frac{Z_{ac}}{R_{dc}}=j^{1/2}\frac{br_2}{2}P\frac{N(br_2)}{D(br_2)} \tag{20}$$

The Bessel functions used in these equations are given in various forms in different tables.[3, 6, 7] The forms used in the present derivation are related to other forms in Table I. The Jahnke–Emde tables, which give the forms $J_0(j^{1/2}Z)$, $j^{1/2}J_1(j^{1/2}Z)$, $H_0^1(j^{1/2}Z)$, $j^{1/2}H_1^1(j^{1/2}Z)$, were found convenient to use, because both Cartesian and polar forms were given, and because less interpolation was required than in other tables that were available. Equation 1 gives the resistance ratio in terms of these forms.

Appendix B. Experimental Determination of Resistance Ratio

The ratio of a-c to d-c resistance of the specimen of copper-covered steel conductor was measured by means of the bridge circuit shown in Figure 9. The bridge, similar to that used by Kennelly and Affel[8] contains resistance in two arms, and both resistance and inductance in the other two. The bridge is arranged for either alternating or direct current.

R_1 and R_2 are matched resistances of 100 ohms each. L_3 is a toroidal fixed inductance of 35 microhenrys. L_4 is a variable inductor, with a maximum inductance of 36 microhenrys. These inductances are designed to balance the bridge with the unknown in and out of the circuit. The conductor under test has an inductance of 25 microhenrys. R_3 is a Manganin slide wire, selected to have negligible skin effect for the range of frequency used, and r is an auxiliary resistance for adjusting the position of the sliding contact on R_3 for balance at different frequencies. The conductor tested is switched out of the bridge by a copper bar resting in mercury cups.

The specimen of conductor under test, number 10 American wire gauge copper-covered steel of nominal 40 per cent conductivity, is mounted on an upright wooden frame six by six feet in size. The frame holds 77 feet of wire strung up and down with a spacing six inches between traverses.

The source of alternating current, a laboratory oscillator, is connected through a shielded transformer which serves to isolate the generator and to match its output to the relatively low impedance of the bridge. The alternating current through the unknown under test conditions varied between 0.04 and 0.16 ampere over the frequency range from zero to 160 kilocycles. The a-c null detector is a General Radio-wave analyzer, the frequency range of which was changed by removing condensers. An external amplifier is used to increase the sensitivity of the detector. For d-c balance a battery and galvanometer are employed. The experimental work was done in a closed room where the temperature was 29 degrees centigrade and remained constant to $^1/_2$ degree centigrade.

Measurements are made in the following manner: with the bridge unbalanced and the unknown in the circuit, a given frequency is applied to the bridge. Then L_4 and the sliding contact of R_3 are changed alternately to obtain a closer balance, and the sensitivity of the detector is increased progressively. When both an inductive and a resistive balance are obtained, the setting of the sliding contact on R_3 is noted. This is expressed as the distance l_1 of the contact from a reference point. Then this procedure is repeated for the same frequency with the unknown disconnected from the bridge, and the corresponding reading l_2 is noted. The resistance of the unknown at the given frequency is[8]

$$k(l_1-l_2)$$

where k is a constant which includes the resistance per-unit length of the slide wire. The bridge is balanced immediately then on direct current, and the difference of the two readings is noted. The ratio of the difference of the two a-c readings to the difference of the two d-c readings is the ratio R_{ac}/R_{dc} for the unknown at a given frequency.

Features which tend to minimize errors in the experimental method are:

1. All the components which are varied during a given measurement are placed in the same arm of the bridge. Thus, since each measurement involves a balance, once with the unknown in the circuit and once with the unknown out, the residual errors in the three unaltered arms cancel.

2. Coaxial connections to the various components are used to minimize stray magnetic coupling. Also a coaxial line is used in the detector circuit, for which purpose the single unit containing R_1 and R_2 is mounted on the sliding contact arm of R_3.

3. Components are placed far enough apart to minimize the effects of stray fields.

4. The fixed inductance L_3 and the variable inductance L_4 are made with no metal in their supporting structures. In the variable inductor L_4 litz wire is employed and the shaft, dial assembly, screws, and so forth, are all nonmetallic.

The over-all accuracy of the experimental method was checked by using it to measure the resistance ratio for ordinary copper wire. Values were obtained which agree with the known published values to an accuracy of one tenth of one per cent over the whole range of frequency, and this figure is taken as indicating the order of accuracy of the method.

The results for the copper-covered steel conductor are shown in Figure 2.

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A New 50,000-Kva 5-Kv Oilless Circuit-Breaker and Metal-Clad Switchgear Unit

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50,000-Kva Oilless Circuit Breaker

THE obvious advantages of air circuit breakers have led to their widespread application, particularly in the 2,500- and 5,000-volt class for 100,000- and 150,000-kva interrupting rating. This has in turn brought a demand for lower kilovolt-ampere ratings in these voltage classes. In the past the oil circuit breaker has been the only device economically applicable in this particular field. However, progress in design and manufacturing facilities has made it possible to build a 50,000-kva unit lower in cost than would have been considered feasible two years ago and one which is expected to meet the operating requirements in this field.

The interrupter is of the magnetic De-ion type and is illustrated schematically in Figure 1. Briefly, the theory of operation of this type of breaker embodies the use of spaced insulating plates of nongas-forming material having tapered slots into which the arc is moved by a magnetic field. As the arc moves into the narrow portion of the slot, electrons in the arc stream, attaining a high velocity from the magnetic field, bombard the gas particles of the arc stream and require the arc continuously to ionize fresh gas in considerable quantities. When current zero is reached, this action continues sufficiently to deionize the plurality of short lengths of arc near the edges of the plates, thereby establishing sufficient dielectric strength to interrupt the circuit.

In the design of the 50,000-kva breaker the insulating plates are made of a high heat-shock resisting material as in previous designs.[1,2] On account of the lower short-circuit currents for this rating, it was possible to reduce the thickness and width of these plates to two-thirds that of the higher interrupting ratings. For this reason also it was possible to space the plates about 30 per cent closer together.

In order further to reduce the size of the arc chute, the number of plates was reduced. The required volts-per-plate interrupting ability was then obtained by increasing the magnetic field. In doing this, the number of turns on the blow-in coil was increased from 6 to 18. This gave a considerable increase in the magnetic field which in turn increased the voltage per plate.

This combination of design features sufficiently reduced the size of the arc chamber to permit the over-all breaker design to be comparable in size with the corresponding oil breaker.

Figure 2 illustrates the breaker as built for metal-clad use. It will be noted that the contacts represent a departure from previous practice on small air circuit breakers in that they are of the parallel blade type, resembling disconnecting switch construction. The hinged joint at the bottom of the blades is of the spring-pressure type with silver surfaces.

The main contact surfaces of the blades are of a type previously developed for disconnecting switches and have proved very reliable in that service. The main contact surfaces are integral with the blades, and hence joints between these parts are eliminated.

The movable contact blades are operated through a simple linkage by a

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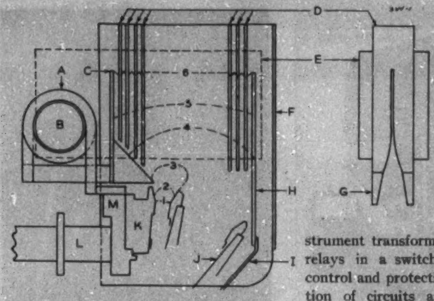


Figure 1. Schematic illustration of typical magnetic De-ion interrupter

The dotted lines 1, 2, 3, 4, 5, and 6 indicate progressive positions of arc interruption

A—Magnet coil
B—Iron yoke
C—Panel-end horn
D—Insulation plates
E—Laminated iron shoes
F—Outline of arc chamber
G—Arc shield
H—Arcing horn
I—Shunt strap
J—Moving contact arm and arcing contact
K—Arcing and secondary-contact platform
L—Micarta bushing
M—Upper stud

mechanically trip-free solenoid mechanism previously applied to oil breakers. Thus, the control circuits may follow established practice in all respects.

The unit illustrated in Figure 2A and 2B is designed to be rolled into the operating and test positions on wheels located on the main frame of the breaker only a few inches below the disconnecting contacts. This gives a close mechanical relationship between the disconnecting contact load, the drive-in force, and the load on the rails.

Tests

In line with modern switchgear-design practice where high-power laboratory facilities are available, numerous interrupting tests were made in the development of the breaker. Table I shows representative results of a typical series of three-phase tests on the breaker in its final form. It will be noted that the maximum kilovolt-amperes interrupted is approximately 60,000 at 4,800 volts, three phase. Figure 3 shows a typical oscillogram taken at 7,300 amperes and 4,800 volts, three phase.

The interruption of low currents at low power factor, corresponding to magnetizing currents, was investigated. The arcing time of the breaker extends to approximately 30 cycles on account of the low magnetic forces for moving the arc from the contacts into the arc chute. Since the arc chute is mounted directly above the breaker contacts, convection air currents assist in moving the arc, and this results in positive and consistent operation. Figure 4 shows a typical oscillogram taken at 20 amperes and 4,100 volts on a single pole.

Metal-Clad Switchgear Unit

To utilize the new 50,000-kva oilless breaker, a new horizontal drawout metal-clad switchgear unit has been designed. It incorporates the associated bus, instrument transformers, instruments, and relays in a switching structure for the control and protection of power distribution of circuits and associated electric apparatus. Figure 6 shows the air-breaker unit on the transfer truck in position to be placed in the housing.

Design Specification

In designing such a unit the following essential requirements had to be met:

(*a*). Compact design, still retaining best performance of circuit breaker under normal current-carrying and interrupting duty and mounting security.

(*b*). Safety and convenience in installation and operation.

(*c*). Accessibility for maintenance.

(*d*). Simplicity and minimum cost.

The metal-clad structure design should be such as not to reduce the effectiveness of the circuit breaker as measured by its performance unenclosed. To accomplish this, clearances and ventilation must be adequate and the arc gases freely vented. The mounting for the circuit breaker must secure it accurately and rigidly in its operating position to insure correct alignment of disconnecting devices and to prevent undue vibration and movement in service.

The horizontal drawout mounting is particularly adapted to air circuit breakers, since it permits a horizontal contact break and utilizes the definite tendency of the arc to rise and thus move naturally into the deionizing chamber. It also gives more ready accessibility to the breaker to permit quick superficial inspection.

All live parts should be enclosed in

Table I. Interrupting Tests on 50,000-Kva Air Breaker

Test Voltage	Current Interrupted				Megavolt-Amperes	Operation
	Phase 1	Phase 2	Phase 3	Average		
4,800	6,000	7,900	8,100	7,300	60.8	*O*
4,800	6,600	7,900	7,200	7,200	60.0	*O*
4,800	6,700	7,700	6,700	7,000	58.2	*O*
4,800	6,900	7,700	9,700	8,100	58.2	*CO*
4,150	8,600	6,800	6,800	7,400	53.2	*CO*
4,160	6,200	7,700	7,900	7,300	60.8	*CO*
4,800	7,900	5,800	6,800	6,800	56.6	*CO*
4,800	6,000	6,200	7,900	6,700	55.8	*CO*
4,700	2,400	1,700	1,900	2,000	16.6	*CO*
4,800	2,100	1,700	1,800	1,900	15.8	*CO*
4,800	490	430	580	470	3.9	*CO*
4,800	540	360	480	460	3.8	*CO*
4,800	1,030	1,010	970	1,000	8.3	*CO*
4,800	2,000	2,200	2,200	2,100	17.5	*CO*
2,200	17,400	12,600	18,700	16,200	61.8	*CO*
2,200	18,300	13,100	15,500	15,700	60.0	*CO*
2,200	16,200	12,100	17,300	15,200	58.0	*CO*
2,200	16,200	12,600	17,300	15,400	58.8	*CO*
2,200	17,400	14,400	14,100	15,300	58.2	*CO*
5,000	4,700	5,100	4,700	4,800	41.6	*CO*
5,000	5,200	3,800	5,300	4,800	41.6	*CO*

O—Open.
CO—Close-open.

Table II. Interrupting Tests on 50,000-Kva Breaker Assembled in Metal-Clad Switchgear Unit

Test Voltage	Current Interrupted				Megavolt-Amperes	Operation
	Phase 1	Phase 2	Phase 3	Average		
5,000	5,515	5,890	6,495	5,967	51.6	*CO*
5,000	5,290	6,240	6,940	6,217	53.9	*CO*
5,000	5,890	6,450	7,710	6,683	57.8	*CO*
5,000	7,530	5,760	5,850	6,380	55.2	*CO*
5,000	7,550	5,690	7,200	6,880	59.5	*CO*

CO—Close-open.

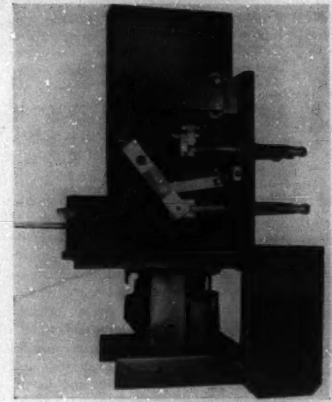


Figure 2A. Removable breaker for 600-ampere 5,000-volt 50,000-kva metal-clad switchgear unit

One arc chute shown removed at the lower right

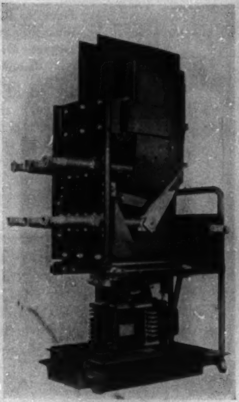


Figure 2B. Rear view of removable air-circuit-breaker unit

Figure 3. Oscillogram showing interruption of 7,300 amperes at 4,800 volts, three phase, 60 cycles on the 50,000-kva oilless circuit breaker

A—Voltage, phase 3
B—Current, phase 3
C—Voltage, phase 2
D—Current, phase 2
E—Voltage, phase 1
F—Current, phase 1

grounded metal compartments, and covers should be provided to give access to each portion of the circuit separately, without exposing any other circuit. The structure must be built ruggedly to withstand shipping stresses and to permit handling as a complete assembly without causing misalignment, so that it can be shipped in as large unit groups as possible.

Operation should be simple, since the application for this size breaker is quite largely in small stations where highly trained personnel is not available.

General Description of Metal-Clad Unit

The steel housing consists of a welded structural frame with sheet-steel barriers dividing the unit into three principal high-voltage compartments for the bus, circuit breaker, and current transformers, together with the main cable terminals. The complete units are separated from each other by sheet-steel barriers, and the bus opening through this barrier is entirely covered by the Micarta insulating support in order to divide the bus compartment into unit lengths.

The busses and connections are insulated completely with Micarta and varnished cambric as shown in Figure 7. The main bus joints are insulated with molded enclosures, which are clamped over the joint by means of tapered keys, and are filled with insulating compound to seal the joints and make the bus insulation continuous. All joints in busses and connections are silver-surfaced to maintain permanently high conductivity.

The highly important primary disconnecting devices consist of flexible finger assemblies mounted directly on the ends of the removable breaker-unit bushings and cylindrical studs mounted in insulating tubes in the housing to provide insulation between phases and to shield them from accidental contact, as Figure 8 shows. The flexible finger assembly consists of bridging segments which make point contacts on the breaker bushing at one end and on the cell stud at the other end, when in the engaged position. A new form of segment is used which is profiled, so that each segment makes two points of contact at each end to reduce millivolt drop. High unit pressure is obtained by flat springs located between an encircling ring and the contact segments in order to exert radial pressure inward on each segment. It will be noted that since each spring exerts radial pressure on its own segment independently of the others, the contact pressure exerted is independent of the number of segments used, so that various contact capacities can be obtained by using different multiples of the same segments and springs. Contact surfaces are heavily silver-plated to insure permanently low resistance.

The hinged panels on the front provide space for mounting instruments, relays, control switches, and other panel devices to form a completely self-contained switching unit as shown in Figure 9. The control and secondary circuits are completed to the breaker unit by means of a multipoint plug and receptacle-type contact. Figure 10 shows the plug assembly on the breaker unit mounted on a novel sliding member with sufficient travel to permit re-engaging it for test when the breaker unit is moved to the test position with the main contacts disengaged. The contacts engage automatically in the operating position, and the sliding mounting is latched, so that they disengage

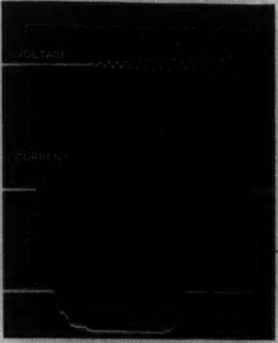
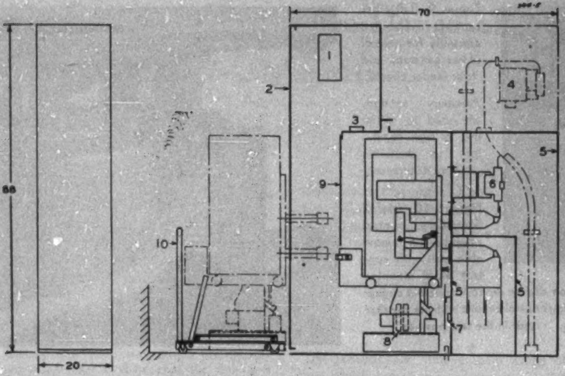


Figure 4. Oscillograms showing typical low-current low-power-factor interruption

Current 20 amperes at 4,100 volts across a single pole



A. Front view

B. Sectional side view

Figure 5 (above). Outline of metalclad unit

Front view and section drawing showing isolation between high-voltage compartments of housing

1. Control panel
2. Hinged door (instrument panel)
3. Cross wiring
4. Potential transformer
5. Removable cover
6. Current transformer
7. Terminal block
8. Secondary contact
9. Hinged barrier
10. Transfer truck

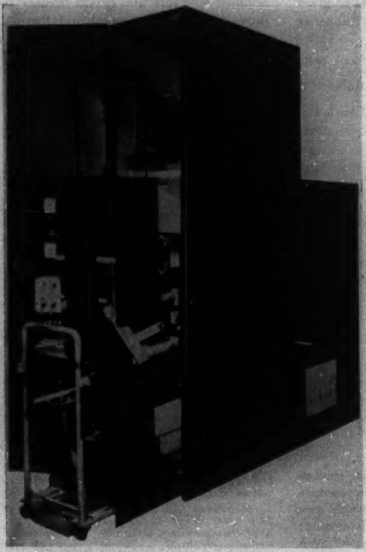


Figure 6 (left). Metal-clad switchgear housing with air circuit breaker on transfer truck in position to enter

Lever operated elevating device of transfer truck permits lowering unit to floor or picking up

Figure 7. Insulated bus and connections assembly

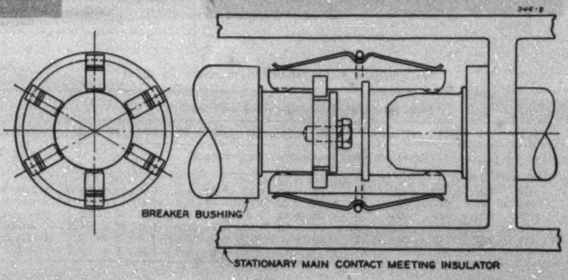
normally when the unit is withdrawn to the test position.

The removable unit, comprising the circuit breaker and its frame support, is carried on flanged rollers which engage the track in the housing. It is transported and placed in position by means of an auxiliary transfer truck equipped with a simple lever-type elevator of limited movement, which will raise the unit from the floor and lower it to position on the housing rails. From this point, a single rugged worm-and-gear levering device is used to move the breaker unit into and out of the operating position. The worm mechanism is self-locking and holds the removable unit securely in the operating position. A notched cam on the levering-device shaft prevents moving the breaker into or out of engagement with the disconnecting contacts, unless the breaker is in the open position, and permits breaker operation only in the operating and test positions. (See Figure 11.)

Tests on Assembled Switchgear

Supplementing the detail tests made on the separate circuit breaker, tests were made in the metal-clad housing at maximum interrupting rating. In addition, a series of repetitive moderate interrupting-duty tests was made to simulate typical motor-inching duty where starting current is opened and closed several times in rapid succession. High-current tests were made as shown in Table II. Twenty-five close-open tests with nine-second intervals were

Figure 8. Details of primary disconnecting contact showing both stationary and moving members



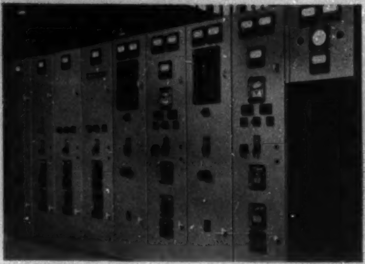


Figure 9 (left). A typical metal-clad assembly for control of two generator and four feeder circuits

Auxiliary sections are used to mount the voltage regulator and excitation control equipment

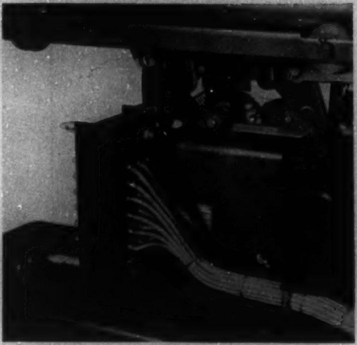


Figure 10 (right). Secondary contact assembly on removable unit with contact block mounted on sliding support to permit engaging control circuits with the breaker unit in the test position

made at approximately 1,500 amperes to simulate repetitive duty on typical motor-starting inrush current. These tests were made too rapidly to permit taking oscillographic records of current.

Careful examination and inspection during and after the tests showed that the enclosing housing has adequate clearance and ventilation to insure the best performance of the circuit breaker and that the connection and bus structures and breaker support are entirely adequate. Figure 11 shows the condition of the breaker contacts after the series of tests.

In addition, momentary current tests at the maximum short-time rating of 25,000 amperes were made on the assembled structure and circuit breaker to prove that the mechanical and thermal capacity was adequate. Dielectric tests at 19 kv, 60 cycles for one minute and 60 kv impulse were made in accordance with the proposed insulation levels for this class of equipment.

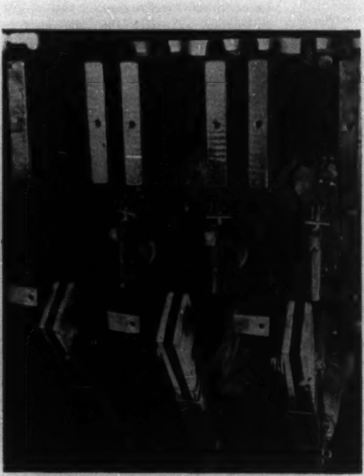


Figure 11. Breaker contacts after series of 73 close–open operations, 26 of which were at or above rated interrupting capacity

Conclusion

Extensive development work and careful cost-analysis work on the larger sizes of air circuit breakers have made it possible to produce a 50,000-kva unit comparable in size, weight, and use of critical material with the corresponding oil-circuit-breaker unit. This makes the obvious advantages in maintenance, safety, and convenience of air-type interrupting devices available for applications to smaller generating and distribution stations, industrial plants, and unit substations.

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Factors Affecting the Design of D-C Magnets

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Synopsis: This paper outlines some of the factors which should be considered in the design of d-c magnets. The usual types of steel used are briefly discussed, including their influence on "sticking" or residual forces. Calculated curves are presented which show the influence of pole-face area on the force developed by the magnet. Flux measurements on a number of magnets show that theoretical calculations are often in great error because of factors such as leakage and saturation which are usually neglected. Data show that the flux in a d-c magnet with a small air gap may vary by as much as 300 per cent between one part of the circuit and another.

D-C magnet design is of tremendous importance today because of the almost exclusive use of d-c power in mobile equipment such as tanks and airplanes. Magnets are the backbone of the d-c control systems, for, when energized by push buttons or interlocks, they furnish the power to operate contactors and relays of all descriptions or to perform strictly mechanical functions, such as actuating firing pins or locking devices. With applications such as these, new requirements have arisen. Chief of these are light weight, minimum volume, insensitivity to vibration with the magnet open or closed, and higher operating temperatures.

Although d-c magnet design is an old subject, there appears to be very little literature on it which is of help to the designer. Text books[1] give an adequate presentation of theoretical principles involved, but assumptions which must be made to solve a problem theoretically are more often than not the dominant considerations in practical design. The tremendous diversity of design requirements has led to a large number of different forms of magnets dictated by device operation and requirements. It is impossible to find a design procedure suitable for all cases, but the same factors, such as saturation, leakage, residual forces, and kind of steel, must be considered in each. Of these, leakage appears to bear an importance not previously suspected. Flux measurements have been taken on a large number of magnets to show their magnitude. The results of some of these measurements are discussed.

Steels

At present, the four general kinds of steel which are considered for use in the usual design of d-c magnets are:

1. Cold rolled or stamping steel.
2. Silicon steel.
3. Low metalloid steel or ingot iron.
4. High-saturation steel such as 50 per cent cobalt, 50 per cent iron.

Tables I and II list the magnetic properties of these steels. The term, "aging factor," which appears in Table I is defined as the per cent increase in hysteresis loss which occurs to a steel with time. As applied to d-c magnet design, an increase in the hysteresis-loop area is significant principally in the second quadrant, where an increased area means an increase in external energy and in coercive force. An accepted laboratory method of finding the aging factor is to measure the d-c hysteresis loop before and after holding the steel at 100 degrees centigrade for 600 hours.

Common stamping steel is the most widely used for d-c magnets because of its low cost and its desirable mechanical qualities. It is readily drawn, punched, or formed, and is available in a large variety of forms and sizes. Its main disadvantage is that it is not sold as a magnetic steel; the mills do not guarantee magnetic qualities. Large variations, therefore, can and do occur in the magnetic quality of this steel. In general, in the annealed condition, it has a better magnetization curve for the higher densities and a higher saturation value than the silicon steels. However, its coercive force is high; its available external energy is high, and its aging factor is large. A design using this material must be checked carefully, not only for residual forces which exist at the time of manufacture, but also for increased forces which may develop because of aging.

As the percentage of silicon in steel increases, the saturation curve becomes poorer, and the intrinsic saturation falls. The silicon steels are also harder to fabricate. These disadvantages are offset by the fact that these steels are sold with specified magnetic properties and are of consistent quality. As the silicon content increases, the aging factor decreases, the largest aging factor for a silicon steel being considerably less than for common stamping steel. The coercive force and external energy are likewise considerably less.

Ingot or Armco iron is the purest form of iron sold commercially. When properly

Table I. Magnetic Properties of D-C Magnet Steels

Kind of Steel	Alloying Constituent (Per Cent)	Intrinsic Saturation (Kilolines Per Square Inch)	Aging Factor (Per Cent)	Residual Induction (Kilogausses)	Coercive Force (Oersteds)	External Energy (Maximum)
Common	0.25 silicon maximum	133	15	7.9	1.5	4,650
Silicon *A*	0.25–0.75 silicon	132.3	8	8.37	0.85	3,450
Silicon *B*	0.75–1.25 silicon	131.5	5	8.0	0.70	2,400
Silicon *C*	2.50–3.0 silicon	126	3	8.1	0.67	2,450
Ingot iron	None	135.5	Varies	7.5	1.05	2,330
Cobalt	50 cobalt	148		16.5	3.5	25,800

Table II. Magnetization Data for D-C Magnet Steels

Kind of Steel	Total Flux Density (Kilolines Per Square Inch)												
	10	30	50	70	80	85	90	95	100	105	110	115	120
	Magnetizing Force (Ampere Turns Per Inch)												
Common	1.98	3.55	5.1	8.1	11.1	14.1	19.2	28	45	77	136	235	355
Silicon *A*	1.17	2.05	3.35	6.2	10.3	14.8	22.5	38	66	120	192	290	420
Silicon *B*	1.20	2.05	3.3	6.4	10.9	15.0	22.5	39	69	120	195	300	430
Silicon *C*	1.10	1.79	3.1	6.4	11.8	18.5	34	65	116	187	280	410	570
Ingot iron	1.2	2.3	3.5	5.2	6.6	7.5	8.9	12.0	21.5	55	115	165	257
Cobalt	2.6	4.4	5.3	6.0	6.3	6.6	6.9	7.4	8.1	9.0	10.5	12.5	15.8

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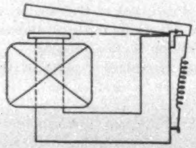
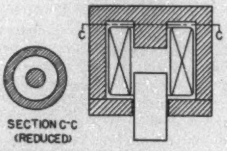
annealed, it has the best magnetization curve and the highest intrinsic saturation value of the usual steels. It is ductile and tough, but readily forged, machined, or welded. Its external energy curve is low. Its magnetic qualities tend to be influenced considerably by the type of anneal which it receives. Its main disadvantages are its relatively high cost and lack of availability in various forms and sizes.

The use of special high-saturation steels is not so widespread as might be supposed, because of the relative difficulty of obtaining them, together with their high cost, poor machining properties, and high coercive force and residual induction. However, as Tables I and II show, a 50 per cent cobalt, 50 per cent iron has a remarkably good induction curve and high intrinsic saturation.



A. Clapper or pivoted armature



B. Solenoid

Figure 1. Types of d-c magnets

In all d-c designs, especially in those using stamping steel, it is essential for consistent performance that an air gap be in the circuit between the armature and pole face. The order of magnitude of residual forces is often underestimated. In one case a magnetic circuit of ingot iron weighing five pounds two ounces was found to have 33 pounds residual force when there was no air gap in the circuit. In another case a small relay using annealed stamping steel weighing 32 grams had a measured residual force of 112 grams. When a magnet operates, the repetitive shock of one part on another, together with the simultaneous application of magnetic field, tends to increase the coercive force. It has been found in practice that this increase may be considerably more than the aging factor would indicate.

Classification of D-C Magnets

D-c magnets can be classified into two general groups:

1. Clapper or pivoted armature type.
2. Solenoid or pot type.

A typical form of clapper magnet is shown in Figure 1A. An armature is pivoted on one face of a U-shaped piece and rotates through an air gap to strike a pole face. The coil may be placed on any leg of the core. In Figure 1B is shown a solenoid-type magnet in which a plunger, usually circular, moves within a coil to strike a core or head finally.

Design requirements for a magnet are usually:

1. It should give a force-versus-stroke curve suitable for the particular requirement.
2. It should utilize to the fullest extent every ounce of iron and copper in the magnet.
3. It always should release under the action of the restoring spring when de-energized.

These requirements determine whether a clapper or solenoid magnet will be used. The shape of the pull curve of a clapper magnet can be changed by varying the pole-face area; that of a solenoid type by using some variation of a stepped plunger or conical plunger.

Force Formula

In order to find the flux in a circuit consisting of an iron path in series with a short air gap, a trial-and-error method is usually employed. A value of flux is assumed; the various magnetomotive-force drops are found, and their total compared to the applied magnetomotive force. A result sufficiently accurate can be obtained usually in two or three trials. When the flux is found, the force between faces is obtained from

$$F=\frac{B^2A}{8\pi} \qquad (1)$$

where F is the force in dynes, B the air gap flux-density in gausses, and A is the area in square centimeters. This formula is the one occurring most frequently in texts and is apt to be misleading when the assumptions on which it is based are neglected. When saturation in the iron is negligible, equation 1 reduces to

$$F=\frac{B^2A^2}{8\pi A}=\frac{\phi^2}{8\pi A}=\frac{(0.4\pi NI)^2A}{L^2} \qquad (2)$$

where ϕ is the total flux crossing the air gap, NI the ampere turns applied to the circuit, and L the length of the air-gap centimeters.

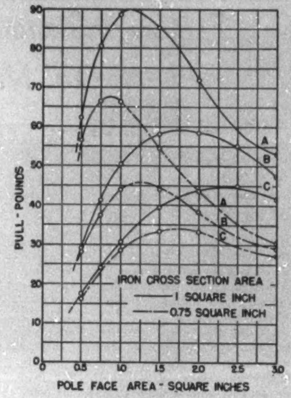


Figure 2. Effect of pole-face area on pull for various air gaps

A. 0.060-inch gap
B. 0.090-inch gap
C. 0.120-inch gap

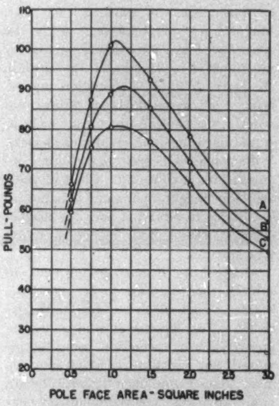


Figure 3. Effect of pole-face area on pull for various lengths of magnetic paths

A. 7.5 inches
B. 10.0 inches
C. 12.5 inches

Equation 2 is a much more accurate form to use when drawing conclusions of the effect on the force of changing physical dimensions. This equation states that if there is no magnetomotive-force drop in the iron, the force varies directly as the face area, directly as the magnetomotive force squared, and inversely as the air gap squared.

Optimum Pole-Face Area

It is seldom economical to operate a magnet so low in flux density that the magnetomotive-force drop in the iron can be neglected. In general, then, when saturation is taken into account, it is possible to show analytically that there is a pole-face area which will give the maximum pull for any one particular set of conditions. The method involves finding a suitable expression for the B, H curve, solving for B in the particular circuit, and then substituting for B^2 in the force equation. This equation may be differentiated then with respect to the pole-face area to find the area for maximum pull. This method is long and tedious. It is faster and more accurate, as soon as a little experience has been gained, to solve a circuit by a trial-and-error method, using the saturation curve of the steel involved. To illustrate the results obtained by this latter method, the following example was worked out: A magnetic circuit was assumed to consist of a ten-inch iron path in series with an air gap of 0.060 inch, 0.090 inch, and 0.120 inch with 2,000 ampere turns applied to the circuit. Two assumptions were made:

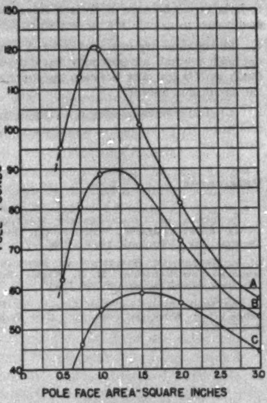


Figure 4. Effect of pole-face area on pull for various impressed magnetomotive forces

A. 2,500 ampere turns
B. 2,000 ampere turns
C. 1,500 ampere turns

1. The ampere turns per inch of iron circuit were constant.
2. There was no fringing in the air gap.

In view of data which are presented later, it will be seen that these two assumptions introduce large errors. They are, however, necessary for a theoretical solution which must be arrived at without the aid of any experimental data. Figure 2 shows the results obtained. It should be noted that the area for maximum pull varies for different air gaps. This point is of great importance for it means that each magnet design can be shaped for a particular requirement.

In the operation of d-c contactors it is a desirable characteristic to have the forces such that the armature will not hesitate when the contact tips first touch. By using the proper core head, the force generated can be kept low in the fully open position and made a maximum at the point where the contact forces begin, so that, when the armature once starts to move, it carries the tips through their kiss-and-wipe position without hesitation. Figure 3 shows the result of varying the length of iron path for the case when the iron cross section is one square inch, the applied ampere turns 2,000, and the air gap 0.060 inch. Figure 4 shows the effect of impressing various magnetomotive forces on a magnetic circuit of ten-inch iron length, 0.60-inch air gap, and one square inch cross section. As the applied magnetomotive force is increased, the pole-face area for maximum pull decreases.

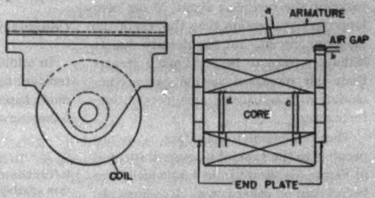


Figure 5. Form of d-c magnet used in studying flux variations

In certain devices such as telephone relays, where the flux density is often very low, the pull of a magnet follows equation 2. The size of the head or pole face can then be increased until the leakage, which is introduced because of the larger head, becomes a limiting factor. An example of this case using an equivalent electric-circuit analogy is treated in the literature.[3]

Air-Gap and Leakage Flux—Experimental Data

Figure 5 shows a type of clapper magnet whose proportions are to scale, which was investigated to obtain data on the distribution of flux in the circuit. Search coils consisting of five turns of 0.005*E* wire were wound on the frame and armature as shown at positions *a*, *b*, *c*, and *d*. The coil at *b* was wound very carefully as close to the air gap as possible and gave flux values which were taken as the air-gap flux. Data were taken on three such magnets, of the same proportions but of different sizes, which weighed about 8, 12, and 20 ounces. Various ampere turns were applied to the main coil; the flux in each of the search coils was then read for various air gaps. All flux data were taken in an engineering laboratory with a ballistic galvanometer. Using the measured values of density, the magnetomotive-force drop in the iron was found and subtracted from the applied magnetomotive force. The net magnetomotive force was used to calculate the flux which should cross the air gap, assuming no fringing.

The results obtained are shown in Figure 6 where *A*, *B*, and *C* represent the three different sizes of magnet. It should be noted that the equivalent fringing factor which may be taken as the ratio of measured-to-calculated flux is not constant but decreases with increasing density—probably because of local saturation at the edges of the pole face. For magnets *A* and *B* the pole-face thickness, which, as shown on Figure 5, is the end-

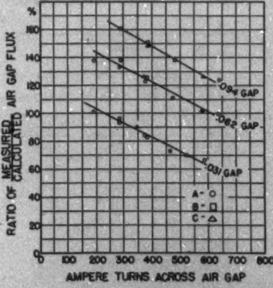


Figure 6. Ratio of measured to calculated air-gap flux for various impressed magnetomotive forces

plate thickness, was 0.094 inch. For magnet *C* the thickness was 0.125 inch. Because of the limited amount of data taken so far on this type of structure, no attempt has been made to determine fringing coefficients as a function of the air-gap dimensions. Figure 5, however, shows clearly the great error which would result if the air-gap flux is calculated on the basis of no fringing, even for the case where the separation is only 33 per cent of the pole-face thickness.

Table III shows the ratio of the total measured flux in the core (the average of search coils *c* and *d*) to the total measured flux in the air gap. In these three magnets the core was about 25 per cent larger in cross section than the rest of the magnetic circuit, but the density due to leakage is seen to be very much greater than this increased section can accommodate without excessive magnetomotive-force drop.

That these large leakage factors are not peculiar to the type of magnet illustrated in Figure 5 is shown from data which have been taken on other types. Readings obtained on a magnet similar to Figure 1A, except with a long thin coil and smaller separation between the legs of the core, showed a ratio of flux measured at the center of the coil to flux measured at the pole face of 250 per cent to 333 per cent over the operating gaps of the magnet. In a solenoid-type magnet, such as Figure 1B, the ratio of maximum flux in the circuit to the useful flux crossing the air gap was found to be 150 per cent.

These leakage figures, of course, vary with the dimensions employed. No attempt here is being made to compare the leakages for various constructions. The only fact which is being presented is that leakage flux in d-c magnetic devices is of an order of magnitude which makes it necessary that their effect be considered in the design. The often seen assumption, "neglect leakage," made in order to effect an analytical solution cannot be justified in many cases.

Conclusions

In addition to data presented here, extensive tests have been conducted on many types of d-c magnets from which the following conclusions may be drawn.

1. Residual forces in magnets may be very large. In a circuit without an air gap, these forces are dependent on the residual induction rather than on the coercive force. Since magnetic steels do not vary greatly in residual induction, an air gap must be in the magnetic circuit of even a low coercive-force material.

2. There is a best pole-face area for maximum pull. This area varies with the air gap and applied ampere turns.

3. Conventional methods of calculating flux in a magnetic circuit are likely to lead to large errors because of the lack of leakage coefficients in calculating air-gap flux.

4. The flux density in a conventional magnet with a small air gap may vary by as much as 300 per cent between one part of the circuit and another.

5. A ballistic galvanometer is a very useful tool in d-c design because of its accuracy and ease of use.

Table III. Ratio of Core Flux to Air-Gap Flux for D-C Magnets

Air-Gap Length in Inches	Magnet A	Magnet B	Magnet C
	Impressed Ampere Turns		
	300	400	500
	Core Flux / Air-Gap Flux in Per Cent		
0.031	190	183	166
0.062	236	224	200
0.094		253	225
0.252			306

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Electrical Stability of Electrical Insulating Oils Under Limited Oxidation

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ELECTRICAL insulating oils find a wide application in the electrical industry in cables, transformers, capacitors, circuit-breakers, and other oil-insulated equipment. The present paper discusses the electrical stability as indicated by low power factor of an oil under limited-oxidation conditions, such as exist in certain service applications in oil-impregnated paper-insulated cables and sealed transformers. This test differs from the conventional "open-beaker" or unlimited-oxidation test. The limited amount of oxygen made available to the oil may be varied, from residual amounts due to contact materials or present in the oil, to the normal solubility of air in oil, and to larger amounts as desired. The limited-oxidation test indicates that, for a given duration of deterioration, oils may give power factors of 10 to 100 times those indicated in the conventional test. This fact is important in service applications for oil-paper cables (since these losses limit the rating of the cable and decrease its efficiency) and for oil-filled equipment (since increased dielectric losses may reduce directly or indirectly the useful life of the equipment). While the power factor of an oil is not of prime importance for many types of oil-filled electric equipment, it is one of the most sensitive known criteria for stability tests. Operating performance of oil-paper cables in service shows, in general, that the power factor of the insulation increases with time, even in certain cases to the extent of impairing the investment seriously. Hitherto, these changes have been unexplainable. It is believed that the limited-oxidation test, which to a certain extent simulates service conditions, will be useful in controlling this important problem.

Equipment and Procedure

The procedures used in making these tests have been described previously,[1] and the equipment used as a deterioration system, electrical measuring cells and electric measuring circuits, have also been described previously.[1,2] The equipment and procedure are indicated briefly here.

Deterioration System

The deterioration system used in certain of these tests is shown in Figure 1. This system may be used for both continuous and limited-oxidation tests. This system consists of:

(*a*). Chamber for containing the oil and contact material.

(*b*). An internal glass pump with glass valves, actuated by a sealed-in plunger for operation by a solenoid; this pump provides for dispersion of both the liquid and gas phases in the system.

(*c*). Electric measuring cells mounted on ground-glass male joints for making measurements on the oil during the deterioration. Provision is made for two cells, one of which may be used to measure the oil by itself, and the other, an oil-paper combination. These cells are of platinum and glass. The vacuum capacitance of the cell is approximately four micromicrofarads with a spacing of 50 mils.

Procedure

The procedure followed in the tests and in the preparation of the materials is given here.

Preparation of Copper. The copper used is in the form of number 36 five-mil-diameter copper wire wound into spirals. The copper is washed successively with benzene, methanol, and distilled water to extract any grease or oil and is then allowed to stand under 50 per cent hydrochloric acid for two hours. It is then successively washed with distilled water, methanol, and thiophene-free benzene. The copper is then dried in a vacuum desiccator for three to four hours.

Preparation of Paper. Paper may be used both in an electric measuring cell and as a contact material. The paper after being placed in the cell or system is effectively dried under a vacuum at an elevated temperature before the oil is degassed into the system.

Introduction of Oil to System. The oil is introduced into the system through a glass degassing equipment at a pressure of 50 to 100 microns. The oil is raised to a temperature of 100 degrees centigrade by a steam jacket after it is introduced into the vacuum chamber of the degassing equipment.

Limited-Oxidation Test. In the limited-oxidation test, the desired amount of oxygen is made available to the oil after the degassing of the oil into the system under a vacuum. The amount of oxygen made available to the oil is varied from none (blank test) up to amounts corresponding to a few thousand cubic centimeters per kilogram of oil.

Table I. Limited-Oxidation Characteristics of Shell 100-4, Transition—Limited to Continuous Oxidation

		Oil			Oil Paper			
Hours	Absorbed Oxygen Cubic Centimeters O_2 Per Kilogram	$\epsilon''f$ 85 C	tan δ % 60 Cycles 85 C	ϵ' 60 Cycles	$\epsilon''f$ 85 C 60 Cycles	$\epsilon''f$ 85 C 1,000 Cycles	tan δ % 60 Cycles 85 C	ϵ' 60 Cycles 85 C
0	↑	0.04	0.03	2.08	0.27	2.44	0.16	2.78
97		0.85	0.68	2.08	0.80	2.97	0.48	2.79
117	Limited	1.24	0.99	2.08	1.17	3.25	0.70	2.79
192	oxidation	3.37	2.69	2.09	3.63	5.54	2.17	2.79
285	500 cubic	4.37	3.49	2.09	5.40	7.50	3.22	2.79
357	centimeters	5.24	4.19	2.09	6.39	8.37	3.82	2.79
432	O_2 per kilogram	5.72	4.58	2.09	7.13	9.45	4.25	2.80
528		6.90	5.52	2.09	8.62	10.8	5.14	2.80
645		8.07	6.46	2.09	9.60	12.0	5.70	2.81
883	↓	9.13	7.23	2.09	11.3	13.0	6.92	2.83
1,315	*	10.2	8.17	2.09	12.3	14.0	7.53	2.83
1,323	1,700	13.1	11.5	2.10	12.6	15.5	9.10	2.84
1,341	2,900	3.50	2.80	2.14	5.62	9.03	3.27	2.87
1,365	6,500	1.31	1.05	2.20	2.40	6.86	1.36	2.94
1,389	10,000	1.65	1.32	2.26	3.67	10.9	2.03	3.02

* Continuous oxidation after 1,315 hours limited test.

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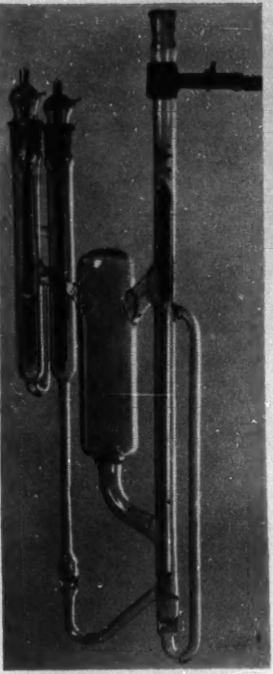


Figure 1. Deterioration cell

In many cases, tests are made using available oxygen in the range from 10 to 100 cubic centimeters per kilogram of oil. These amounts are in the range of oxygen due to the normal solubility of air in oil. Following the introduction of the oxygen, the system pressure is raised to atmospheric by nitrogen.

Continuous-Oxidation Test. In the continuous-oxidation test, the pressure in the system is maintained at 750 millimeters of mercury by the addition of oxygen as rapidly as it is absorbed by the oil. For this purpose, an automatic gas-feeding mechanism is utilized which permits determining on a burette both the rate and the amount of oxygen added to the system.

Figure 2. Electric measuring circuit

Test Temperatures. The limited and continuous-oxidation tests discussed in this paper have been made at a temperature of 85 degrees centigrade. This temperature has been used, as it represents a reasonably high operating temperature for oil-insulated equipment.

Electrical Measurements

The electric equipment used in making electrical measurements is shown in Figure 2. The electrical characteristics of a capacitor or dielectric are generally expressed in terms of an equivalent parallel circuit, Figure 3, and a vector diagram, shown in Figure 4. In Figures 3 and 4; G represents the equivalent parallel conductance, C represents the equivalent parallel capacitance, GE the energy component of current in phase with the voltage, and ωCE the quadrature or capacitance of component of current. The electrical characteristics of a dielectric are expressed in terms of the following:

Power Loss. The power loss P in watts in the dielectric is given by

$$P = E^2G = EI\cos\theta = EI\sin\delta \text{ (watts)} \qquad (1)$$

in which

E is the effective voltage in volts impressed on the dielectric.
I is the effective current in amperes flowing into the dielectric.
θ is the power-factor angle of the dielectric.
δ is the loss angle of the dielectric.
G is the equivalent parallel conductance in mhos of the dielectric.

Conductance. The conductance G in mhos of a dielectric is

$$G = \omega C\tan\delta = \omega C_0\epsilon'\tan\delta = (2\pi C_0)\epsilon''f \quad \text{(mhos)} \qquad (2)$$

in which

C is the capacitance in farads of electrode arrangement containing sample.
C_0 is the capacitance in farads of similar electrode arrangement in vacuum, that is, geometric capacitance.
$\tan\delta$ is the dissipation factor in per unit.
ω is equal to $2\pi f$, and f is the frequency in cycles per second.

Specific Conductance. The specific conductance G_s of a dielectric is

$$G_s = 0.556\frac{G}{2\pi C} = 0.556\epsilon''f(10^{-12}) \quad \text{(mho-centimeters)} \qquad (3)$$

Dielectric Constant. The dielectric constant ϵ' of a dielectric is

$$\epsilon' = \frac{C}{C_0} \qquad (4)$$

Loss Factor. The loss factor ϵ'' of a dielectric is given by

$$\epsilon'' = \epsilon'\tan\delta \qquad (5)$$

The loss factor is proportional to the loss per cycle. It is generally used when the loss is due to the orientation of dipoles

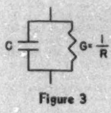


Figure 3

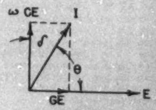


Figure 4

Table II

Size of Conductor (Circular Mils)	Diameter D (Inches)	Ratio Copper-Surface Area in Square Centimeters Per Gram of Oil Insulation Thickness (Inch)			
		0.005	0.0600	0.125	0.25
250,000	0.500	195	15	6	3
500,000	0.707	195	15	6	3
750,000	0.865	195	15	7	3
1,000,000	1.000	195	15	7	3

d_e assumed 0.8—k assumed 0.5.

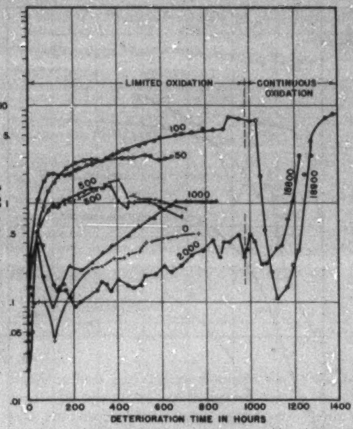


Figure 5 (left). Limited oxidation characteristics as a function of available oxygen

Gulf *D6E7F*, 85 degrees centigrade, 1.6 square centimeters copper per gram of oil, cubic centimeters of oxygen per kilogram of oil as indicated; transition from limited to continuous oxidation, cubic centimeters of oxygen per kilogram of oil at end of continuous test as indicated

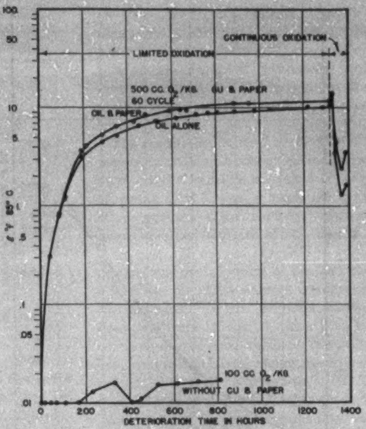


Figure 6 (right). Effect of paper and copper — limited oxidation

Shell 100-4, 85 degrees centigrade, 1.6 square centimeters copper per gram of oil, cubic centimeters of oxygen per kilogram of oil as indicated, in test with oil, copper, and paper; electrical measurements made on both oil and oil-paper combination; transition limited to continuous test at end of 1,300 hours

and other absorptive types of dielectric polarization.

Dissipation Factor. The dissipation factor of a dielectric is expressed in terms of tan δ, in which δ represents the loss angle as shown in Figure 4. Thus, the per-unit dissipation factor is equal to the per-unit ratio of the energy component of current *GE* to the quadrature capacitance component of current, ωCE.

Power Factor. The power factor of a dielectric is expressed in terms of cos θ, in which θ is the angle between the total current and voltage, as indicated in Figure 4. Thus, the power factor in per unit is the ratio of the energy component of current to the total current flowing into the dielectric.

Conductance Factor. The conductance factor of a dielectric $\epsilon''f$ is given by

$$\epsilon''f = \epsilon' \tan \delta f \qquad (6)$$

The conductance factor is directly proportional to the equivalent parallel conductance and the total energy loss of the dielectric. It is a convenient characterization when the energy dissipated is due to the motion of free ions—in this case; generally the conductance factor is independent of frequency. For mineral oils, the conductance factor $\epsilon''f$, is approximately equal to the power factor or dissipation factor expressed in per cent at a frequency of 60 cycles per second, since ϵ' is approximately two, and f is 60 in equation 6.

Comparison of Limited- and Continuous-Oxidation Characteristics

Comparison of limited- and continuous-oxidation characteristics of certain samples of mineral oils is shown in Figures 5, 6, 7, and 8. These comparisons are shown for different amounts of available oxygen in a limited-oxidation test in Figures 5 and 6 for the effect of paper and copper in Figure 6, and for continuous-oxidation tests on certain samples in Figures 7 and 8.

Limited-Oxidation Characteristics

The limited-oxidation tests in Figure 5 were made on Gulf Sample *D6E7F*. The physical properties and method of preparation of this sample have been described previously.[3] This sample is the raffinate from a solvent-refined series of samples with a viscosity of 82.9 Saybolt-Universal at 100 degrees Fahrenheit, a specific dispersion of 93, a sulphur content of 0.03 per cent, and by a Waterman analysis shows 0.0 per cent aromatics, 11.8 per cent naphthenes, 88.2 per cent paraffins. It is a highly refined, nearly water-white, aromatic-free oil with a relatively low sulphur content. As shown in Figure 5, this oil sample was tested under limited-oxidation conditions corresponding to available oxygen of 0, 50, 100, 500, 1,000, and 2,000 cubic centimeters per kilogram of oil. These tests were made at 85 degrees centigrade with a copper-oil ratio of 1.6 square centimeters of copper surface area per gram of oil. The electrical measurements are given in terms of the $\epsilon''f$ at a deterioration temperature of 85 degrees centigrade. The results show that the maximum conductance was obtained with 100 cubic centimeters of oxygen per kilogram of oil and gave a maximum value of $\epsilon''f$ at 85 degrees centigrade of approximately 8 per cent, and a corresponding 60-cycle

Table III

Size of Conductor (Circular Mils)	Diameter D (Inches)	Ratio Copper-Surface Area in Square Centimeters Per Gram of Oil
250,000	0.574	52 (19 strands)
500,000	0.814	51 (37 strands)
750,000	0.998	54 (61 strands)
1,000,000	1.152	47 (61 strands)

Table IV

Size of Conductor (Circular Mils)	Diameter D (Inches)	Ratio Copper-Surface Area in Square Centimeters Per Gram of Oil Insulation Thickness (Inch)			
		0.005	0.0600	0.125	0.25
250,000	0.574	49	27	17	9
500,000	0.814	49	31	21	12
750,000	0.998	52	36	25	15
1,000,000	1.152	45	32	24	15

d_s assumed 0.8— k assumed 0.5.

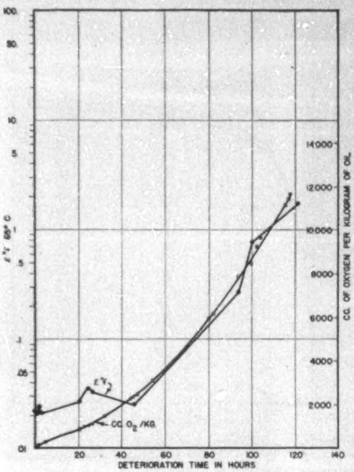


Figure 7. Continuous-oxidation characteristics

Shell 100-4, 85 degrees centigrade, 760 millimeters oxygen, 2.3 square centimeters copper per gram of oil; 1 gram paper per 19 grams oil

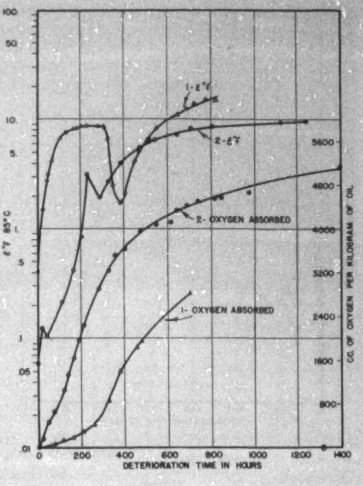


Figure 8. Continuous-oxidation characteristics

Oil 1, commercial light cable oil; oil 2, commercial transformer oil, 85 degrees centigrade; 760 millimeters oxygen, 1.6 square centimeters copper per gram of oil

power factor of approximately 6.5 per cent. The blank test with no available oxygen showed relatively good stability. At the end of approximately 950 hours, the 100 and 2,000 cubic centimeters of oxygen per kilogram of oil-limited tests were changed to the continuous-oxidation type. This was done by withdrawing the gas above the oil sample in the deterioration cell and then maintaining an oxygen pressure of 760 millimeters. In the case of the 100-cubic-centimeter test, the power factor or conductance of the oil rapidly decreased with continuous-oxygen absorption from an $\epsilon''f$ of approximately eight to a value approximating 0.1. Further absorption of oxygen beyond this point gave rapidly increasing losses. In the case of the 2,000-cubic-centimeter-limited test, the transition from the limited to the continuous type of test showed very little change in the electrical characteristics. At the conclusion of the continuous-oxidation tests, the oils had absorbed approximately 19,000 cubic centimeters of oxygen per kilogram of oil, with the losses of the same order of magnitude as for the 100-cubic-centimeter limited-oxidation test. It is possible that a limited test in the range between 100 and 500 cubic centimeters of oxygen per kilogram of oil would produce greater losses than is indicated for the 100-cubic-centimeter test. These results show that there may be a different mechanism responsible for the electric losses in the two types of tests. Presumably, the oxidation products formed in the limited type of test are unstable in the presence of further oxygen and give the results indicated in the 100-cubic-centimeter test at the transition point. In the case of the 2,000-cubic-centimeter limited test, apparently there is sufficient available oxygen, so that the oxidation products proceed to such end-oxidation products as water and acids. Satisfactory check results are indicated by the two tests made with 500 cubic centimeters of oxygen per kilogram of oil.

Effect of Copper and Paper

The test results in Figure 6 show the effect on oil sample Shell 100-4 of the presence of paper and copper during a limited-oxidation test. The effect of a transition from a limited to continuous type of oxidation is shown again. The physical properties and preparation of this sample have been described previously.[1] This is one of a group of solvent-refined samples with varying degrees of aromaticity. It has a viscosity of 86 Saybolt Universal at 100 degrees Fahrenheit, a specific dispersion of 96, and a total sulphur content of 0.18 per cent, and a mercaptan-plus-disulphide sulphur of less than 0.001 per cent. It is a practically water-white aromatic-free

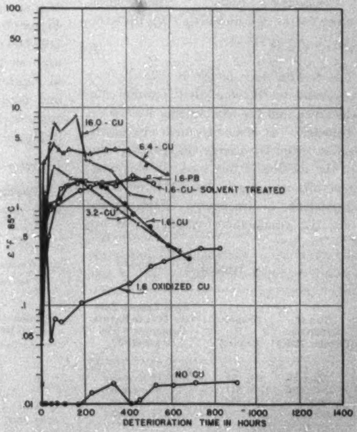


Figure 9. Effect of copper and lead area

Shell 100-4, limited oxidation, 100 cubic centimeters of oxygen per kilogram of oil, 85 degrees centigrade; surface area in square centimeters of copper per gram of oil as indicated

Table V. Physical Properities of Shell Series of Solvent-Refined Transformer Oils

	60	60-1	60-2	60-3	60-4
Gravity, degrees, American Petroleum Institute	25.7	28.9	32.0	34.6	36.0
Viscosity at 100 F (Saybolt Universal second)	59	61	58	58	57
Flash, open cup, F	300	300	300	300	300
Fire, open cup, F	335	335	335	335	335
Color	2+*	2+*	11**	19**	30+**
Specific dispersion	134	118	107	102	97
Per cent mercaptans and disulphide sulphur	0.001	0.001	0.001	0.001	0.001
Per cent total sulphur	1.16	0.72	0.33	0.18	0.04
Density 20/4 degrees centigrade	0.8936	0.8766	0.8592	0.8490	0.8420
Refractive index, n_D^{20}	1.4974	1.4853	1.4743	1.4680	1.4631
Aniline point, C	69.4	78.9	87.2	93.4	97.5
Molecular weight	274	286	290	302	300
Waterman analysis:					
Aromatics (per cent)	15.6	10.0	5.1	2.5	0
Naphthenes (per cent)	24.4	27.7	27.0	26.4	24.9
Paraffins (per cent)	60.0	62.3	67.9	71.1	75.1

* National Petroleum Association.   ** Saybolt.

highly refined oil. Figure 6 shows the results of two tests, one a limited test with 100 cubic centimeters of oxygen per kilogram of oil without copper and paper and the other a limited test with 500 cubic centimeters of oxygen per kilogram of oil with copper and paper. These tests were made at 85 degrees centigrade. In the case of the 500-cubic-centimeter limited test, electrical measurements were made utilizing two electric measuring cells, one measuring the oil alone, and the other an oil-paper cell giving electrical measurements on an impregnated-paper sample. At the end of approximately 1,300 hours of the 500-cubic-centimeter limited test, the test conditions were changed from the limited to the continuous type. The copper-oil ratio in these tests was 1.6. The results show, for the 100-cubic-centimeter limited test in the absence of copper and paper, that the conductance of the oil remains very low and approximately equal to the initial value throughout the test duration of approximately 800 hours. In the case of the 500-cubic-centimeter limited test with copper and paper, the 60-cycle power factor of the oil has increased to approximately ten per cent. Other work that has been done indicates that this increase in power factor is due principally to the presence of the copper, the paper introducing a negligible effect. It is interesting to note that the conductance of the oil itself is very nearly equal to that of the impregnated paper throughout the limited test. Further data on this test are given in Table I which also gives the 60-cycle power factor in per cent for both the oil and the oil-paper, and also gives the $\epsilon''f$ of the impregnated paper at both frequencies of 60 and 1,000 cycles per second.

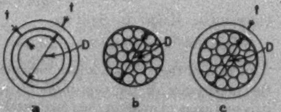


Figure 10

The impregnated paper during the limited portion of the test merely indicates the conductance of the oil itself. At the transition point where the test is changed from a limited to a continuous type, the losses initially increase for both the oil and the impregnated paper and then rapidly decrease to lower values. As shown in Table I, the relation between the 60- and 1,000-cycle values of $\epsilon''f$ are very much different during the limited as compared to the continuous type of test. During the limited test, the conductance of the impregnated paper is relatively independent of frequency, whereas it increases very rapidly with increasing frequency during the continuous type of test. For example, in Table I, at the end of 192 hours of deterioration of the limited tests, $\epsilon''f$ of the oil are 60 cycles, 3.63; 1,000 cycles, 5.54, whereas at the end of the continuous test, corresponding values are 60 cycles, 3.67; 1,000 cycles, 10.9. During the continuous type of oxidation test, acids and water are produced, which are adsorbed readily by the hydroxyl groups of the cellulose. It is believed that whatever oxidation products are produced during the limited test, they are not adsorbed by the paper. These results again indicate a fundamental difference in the type of oxidation mechanism between the limited and continuous types of oxidation.

CONTINUOUS-OXIDATION CHARACTERISTICS

The continuous-oxidation characteristics of samples of three different oils are shown in Figures 7 and 8. Figure 7 shows the results on Shell 100-4, the limited-oxidation characteristics of which have been discussed previously and shown in Figure 6. Figure 8 shows the results on two representative commercial oils.

The results in Figure 7 on Shell 100-4 were obtained under continuous oxidation at 85 degrees centigrade, with a copper-oil ratio of 2.3 and a paper-oil ratio of one gram of Whatman's 41-*H* ashless filter paper per 19 grams of oil. Figure 7 gives both the oxygen absorbed in cubic centimeters per kilogram of oil, and the $\epsilon''f$ of the oil at 85 degrees centigrade. As compared to the previous limited tests in Figures 5 and 6, the $\epsilon''f$ under continuous oxidation remains relatively low, even with relatively large amounts of oxygen absorbed. For example, at 85 degrees centigrade the $\epsilon''f$ with approximately 5,000 cubic centimeters of oxygen, is approximately 0.1, whereas under limited-oxidation conditions with 500 cubic centimeters, the $\epsilon''f$ of the oil is approximately 10. Thus, for ten times as much oxygen in the continuous test, the conductance is approximately 0.01 that for the limited test. The oxidation characteristics of this oil are strongly autocatalytic, which is somewhat characteristic of an overrefined oil corresponding to this particular sample. Such an oil generally produces, under oxidation, water and low-molecular-weight acids which would rapidly be absorbed by the paper. This probably has helped to maintain relatively low values of conductance during this test. Other tests on the same type of oil in the absence of paper have indicated that the losses would remain relatively low during the initial stages of the oxidation but would increase to relatively large values for materially lower values of total oxygen absorption. Furthermore, an impregnated-paper sample would reveal the characteristics shown in Figure 6 and in Table I after the transition to continuous-oxidation conditions.

The results in Figure 8 show continuous-oxidation characteristics and corresponding conductance factors on two samples of oil—oil 1, a representative

Table VI. Physical Properties of Shell Special 121 Oil

Viscosity at 100 F (Saybolt Universal second)	55.6
Viscosity at 210 F (Saybolt Universal second)	33.7
Density d_4 20 C	0.8756
Refractive index n_D^{20}	1.4871
Specific dispersion	124.
Per cent sulphur	0.02

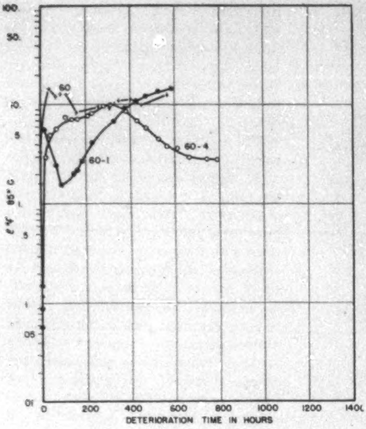
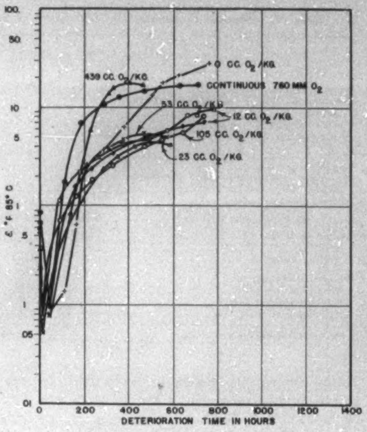


Figure 11 (left). Limited - oxidation characteristics

Shell series 60, 60-1, 60-4, 100 cubic centimeters of oxygen per kilogram of oil, 85 degrees centigrade; 1.6 square centimeters copper per gram oil

Figure 12 (right). Limited - oxidation characteristics—variable available oxygen

Shell 60-2, 85 degrees centigrade, cubic centimeters oxygen per kilogram oil as indicated; 1.6 square centimeters copper per gram oil, continuous test, 760 millimeters oxygen



commercial oil of the light-cable-oil class, and oil 2, a representative commercial oil of the transformer-oil class. These tests were made at 85 degrees centigrade with a copper-oil ratio of 1.6. The viscosity of oil 1 is approximately 100 Saybolt Universal at 100 degrees Fahrenheit, and that of oil 2 is approximately 55 Saybolt Universal at 100 degrees Fahrenheit. It may be noted that these oils are relatively more stable to oxidation than the over-refined Shell 100-4 previously discussed. The oxidation characteristics of oil 1 are representative of the inhibited type. It is interesting to note that, during the induction period of oil 1, the conductance $\epsilon''f$ rapidly increases to a value approximating 9. However, at the end of the induction period corresponding to approximately 300 hours, the conductance rapidly decreases to a much lower value and then increases with further oxygen absorption. Thus, this characteristic is somewhat analogous to the transition from limited to continuous oxidation, as shown in Figures 5 and 6. However, in this case limited-oxidation characteristics of the oil are inherent in the continuous type of test, by reason of the inhibited type of oxidation reaction during the early stages of the test. Oil 2 shows the normal electrical characteristics for an oxidation reaction in which the oxidation reaction proceeds to the normal end products. As a comparison it is interesting to observe that for the deterioration time of 200 hours, oil 2 has absorbed, roughly, ten times as much oxygen as oil 1, but the conductance or power factor of oil 2 is approximately 0.1 that of oil 1. Limited-oxidation studies have also been made on these two oils. Under limited-oxidation conditions, at 85 degrees centigrade a copper-oil ratio of 1.6, and available oxygen of 100 cubic centimeters per kilogram, both oils will give power factors at 60 cycles approximating ten per cent. Thus, there is relatively little difference between the oils on a limited-oxidation basis, whereas there are relatively large differences in the initial stages on the continuous basis.

Effect of Metal Surface Area

As shown previously, the presence of copper in a limited-oxidation test gives increased electrical conductivity or power factor of the oil. This is illustrated further in Figure 9, which gives the results of Shell 100-4 with limited oxidation of 105 cubic centimeters of oxygen per kilogram of oil at 85 degrees centigrade with variable amounts of copper surface per gram of oil, and with different methods of treatment of the copper surface.

VARIABLE COPPER- AND LEAD-OIL RATIOS

The amount of copper surface is varied from a blank test in which no copper is present in increasing amounts up to 16 square centimeters of copper surface area per gram of oil. The normal preparation of the copper has been given previously under *Equipment and Procedure.* The solvent-treated copper surface is similar to the normally treated, except that the copper is not immersed in a 50 per cent hydrochloric acid solution. The oxidized copper surface is similar to the normally treated, except that before the copper is placed in the deterioration cell, it is heated to 100 degrees centigrade in an air oven for approximately an hour.

The results indicate that in the absence of copper the power factor of the oil remains relatively low—of the order of 0.01 per cent at 85 degrees centigrade. With copper present, the maximum power factors of the oil are somewhat proportional to the amount of copper surface. In one test, lead was used in place of copper, and the results were approximately the same as for copper of corresponding area. Additional tests were made with lead, but it was difficult to obtain reproducible lead surfaces and, hence, reproducible results. It was not practicable to use relatively large lead areas corresponding to those for copper. The solvent-treated copper gave approximately the same increase in power factor as the normal copper surface. The oxidized copper surface gave materially lower losses than the normal copper surface. Similar tests with other oils have given corresponding results. This particular oil sample, Shell 100-4, would give somewhat higher losses with larger amounts of available oxygen under a limited-oxidation test, as shown in Figure 6. Thus, the results show that the copper-oil ratio is important in determining power factor under limited oxidation.

COPPER-OIL RATIO IN SERVICE APPLICATIONS

In view of these results which indicate the importance of the copper-oil ratio, it is pertinent to analyze the corresponding ratios for oil-impregnated paper-

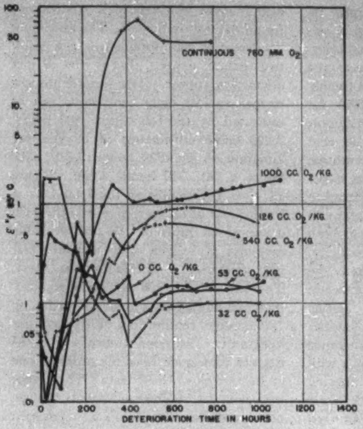
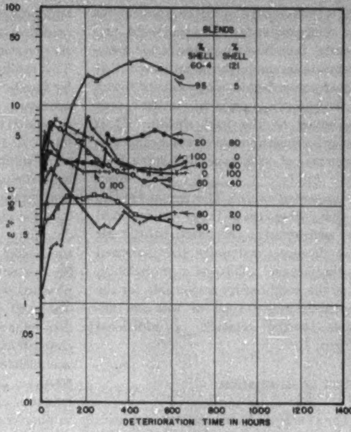


Figure 13 (left). Limited - oxidation characteristics—variable available oxygen

Shell 60-3, 85 degrees centigrade, cubic centimeters oxygen per kilogram oil as indicated; 1.6 square centimeters copper per gram oil, continuous test, 760 millimeters oxygen

Figure 14 (right). Effect of blending an aromatic-free and an aromatic oil

Blends Shell 60-4 and Shell 121, limited oxidation, 85 degrees centigrade, 100 cubic centimeters oxygen per kilogram oil; proportion of blends as indicated; 16 square centimeters copper per gram oil



insulated cables and other oil-insulated equipment. Previous work[4] of tape-by-tape power-factor measurements on cables has indicated that the power factor is not uniform throughout the thickness of the insulation. Generally, high power factors are indicated for those tapes adjacent to the conductor and the sheath. Thus, in obtaining copper-oil ratios for cables, it is necessary to consider, not only the over-all ratio corresponding to total surface areas and total oil in the insulation, but also the ratios that apply to the strand space and for a relatively thin wall of insulation immediately adjacent to either the conductor or the sheath. Furthermore, these ratios will depend on whether the surface area is relatively smooth as for the sheath and compact-conductor construction or as for the standard concentric-strand-conductor construction. An estimate of the different ratios that apply in cables may be obtained from the following:

1. Copper-oil ratio for a layer of insulation on or within a cylindrical surface. This case is shown in Figure 10, and is given by

$$\text{copper-oil ratio} = \frac{\text{area (square centimeters)}}{\text{gram oil}} = \frac{1}{kt\left(1 \pm \frac{t}{D}\right)d_e} \qquad (7)$$

in which

d_e = specific gravity of oil
D = diameter of conductor in centimeters
t = thickness of oil film or insulation in centimeters
k = per-unit ratio of volume of oil in thickness t to total volume included in thickness t
+ = exterior insulation
− = interior insulation

Representative copper-oil ratios for this case are given in Table II for different conductor sizes and different insulation thicknesses. These values would apply to insulation on a cylindrical conductor surface, as would be represented by compact construction or for the insulation within a lead sheath. It may be noted that the values vary between very wide limits. For the case of the oil contained in the first tape of insulation immediately adjacent to such a conductor or sheath surface, the ratio is 195, independent of the diameter of the conductor or sheath.

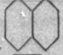
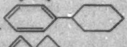
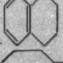
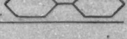
2. Copper-oil ratio applying to the strand space of a stranded conductor. This case is shown in Figure 10b and is given by

$$\text{copper-oil ratio} = \frac{\text{area (square centimeters)}}{\text{gram oil}} = \frac{14\sqrt{n}}{d_e D} \qquad (8)$$

in which

n = number of strands

Table VII

Decane	$CH_3(CH_2)_8CH_3$
Hexadecane	$CH_3(CH_2)_{14}CH_3$
Hexadecene	$CH_3(CH_2)_{13}CH{=}CH_2$
Cis-decalin	
Phenylcyclohexane	
Tetralin	
Dicyclohexyl	

Representative values of these ratios are given in Table III for various conductor sizes. It may be noted that these ratios approximate 50 and are somewhat independent of the conductor size.

3. Copper-oil ratio applying to insulation on a stranded conductor, including the oil in the strand space as well as the oil in the insulation. This case is represented by Figure 10c and is given by

$$\text{copper-oil ratio} = \frac{\text{area (square centimeters)}}{\text{gram oil}} = \frac{14\sqrt{n}}{D\left[1+16k\frac{t}{D}\left(1+\frac{t}{D}\right)\right]d_e} \qquad (9)$$

Representative values for this case are given in Table IV for various conductor sizes and insulation thicknesses. The ratio depends upon the thickness of the insulation but is equal to approximately 50 for the first insulation tape adjacent to the conductor.

SIGNIFICANCE OF COPPER-OIL RATIO

The maximum copper-oil ratio that has been used in the limited test has been 16. This maximum value has been limited by available space for the copper and also by the desire to maintain adequate circulation. As shown in Tables II, III, and IV, copper-oil ratios in oil-paper cables may be very much higher than the values used in the limited tests.

The work that has been done has indicated that, at least up to the maximum value used, the power factor is approximately proportional to the amount of copper surface. Whether this proportionality would continue for higher ratios is not known. It is possible to make appropriate changes in the deterioration systems to permit higher copper-oil ratios. It is believed that these high ratios such as exist in service applications are of importance. As mentioned previously, tape-by-tape power-factor measurements on cables have indicated that higher power factors may exist for the oil in the immediate proximity of the copper conductor or the lead sheath. It is somewhat questionable whether the measurements of this type that have been made indicate the values that obtain in the sealed cable. The reason for this is that, when these tapes are removed for measurement, exposure to the air may decrease materially the measured power factors. This is a probability, since the components responsible for the limited-oxidation type of loss are unstable in the presence of additional oxygen.

Effect of Aromatics

The effect of the amount of aromatics of an oil under limited oxidation with different amounts of available oxygen is shown in Figures 11, 12, 13, and 14.

PHYSICAL PROPERTIES OF OIL SAMPLES

These tests were made on a series of oils, specially prepared by the Shell Petroleum Corporation, the physical properties of which are given in Tables V and VI. The Shell-60 series was prepared from a plant cut of a special West Texas grade of low pour point. The cut had a viscosity of around 60 seconds. The first sample, 60, was not solvent-extracted; the other samples were extracted to give decreasing degrees of aromaticity, 60-4 being completely freed of aromatics by treatment with fuming sulphuric acid. All of the samples received a finishing treatment with sulphuric acid followed by clay contacting. This resulted in aromaticity, as measured by specific dispersion, of from 134 (highly aromatic, benzene equal to 189) for sample 60 to 97 (aromatic-free) for sample 60-4. As shown in Table V, the per cent total sulphur decreases with decreasing aromaticity; thus sample 60 has a per cent total sulphur of 1.16 as compared to 0.04 for sample 60-4. In an attempt to determine the relative effects of both total sulphur content and aromaticity, Shell special 121 oil was prepared, the physical properties of which are given in Table VI. This sample has a specific dispersion of 124, and a total sulphur content of 0.02 per cent. This sample was blended in varying proportions with 60-4 to give samples having varying amounts of aromaticity, but with lower sulphur content than would be obtained from the 60- series of samples for corresponding dispersions.

RESULTS OF TESTS

Figure 11 gives the results of limited tests at 85 degrees centigrade, copper-oil ratio of 1.6, and available oxygen of 100 cubic centimeters per kilogram of oil on samples 60, 60-1, and 60-4. All of these gave 60-cycle power factors approximating ten per cent, so that further tests using different amounts of available oxygen were not made on these samples. Figure 12 gives the results on sample 60-2 at 85 degrees centigrade with a copper-oil ratio of 1.6 and available amounts of oxygen ranging from a blank test with no oxygen up to a continuous test. This sample gives relatively high conductance for all the tests indicated. The maximum conductance was obtained in the blank test without oxygen, that is, the oil just contacting copper. This sample absorbs oxygen rapidly on a continuous test as indicated by the following: 102 hours, 2,290 cubic centimeters of oxygen per kilogram of oil; 209 hours, 3,710; 313 hours, 4,180; 720 hours, 4,970. Figure 13 gives the results on Shell 60-3 for a limited test at 85 degrees centigrade with a copper-oil ratio of 1.6 and available amounts of oxygen from a blank test with none up to a continuous type of test. This oil shows relatively good stability under all limited tests up to and including 1,000 cubic centimeters of oxygen per kilogram. The conductance $\epsilon''f$ at 85 degrees centigrade is of the order of one or less for all of these tests. There does not seem to be any consistent relation between the loss and the amount of oxygen made available, except that, on the average, it is somewhat greater for the larger amounts of available oxygen. In the continuous test, the power factor reaches relatively high values. This sample also absorbs oxygen quite rapidly under a continuous test, as indicated by the following: 96 hours, 2,080 cubic centimeters of oxygen per kilogram of oil; 184 hours, 6,670; 304 hours, 15,060; 784 hours, 20,900. Figure 14 gives the results of various blends of Shell 60-4 with Shell 121. These tests were of the limited type using 105 cubic centimeters of oxygen per kilogram of oil, a copper-oil ratio of 16 at 85 degrees centigrade. It is important to note that these particular tests have a copper-oil ratio of ten times

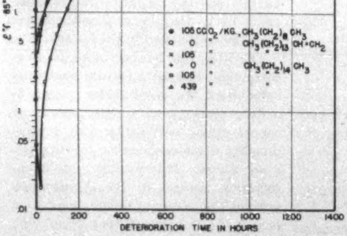


Figure 15 (left). Limited-oxidation characteristics of decane, hexadecane, hexadecene

Available oxygen as indicated, 85 degrees centigrade, 1.6 square centimeters copper per gram oil

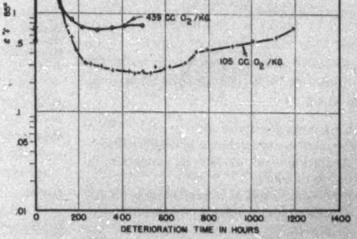


Figure 16 (right). Limited-oxidation characteristics of cis-decalin

Available oxygen as indicated, 85 degrees centigrade, 1.6 square centimeters copper per gram oil that for the preceding results in Figures 9, 10, and 11. These tests show a maximum electrical stability for the samples containing 10 and 20 per cent of Shell 121. These blends indicate greater stability than that for either the *60-4* or the *121* individually.

Effect of Per Cent Aromatics

The results of the tests discussed in the foregoing show quite clearly the greater electrical stability under limited tests for those samples containing a small percentage of aromatics. The relation between electrical stability and aromatics is not too definite, since it is probably a function of copper-oil ratio, amounts of available oxygen, and deterioration time. More complete information would require further tests for each sample as a function of each of these variables. Apparently, sulphur content is not the major determining factor for the samples with electrical instability, since certain of the samples relatively free from sulphur also produce relatively high losses.

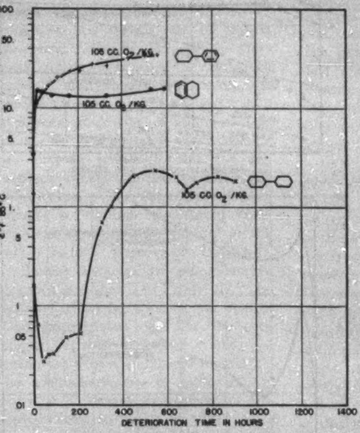


Figure 17 (left). Limited - oxidation characteristics of tetralin, phenylcyclohexane, and dicyclohexyl

Available oxygen as indicated, 85 degrees centigrade; 1.6 square centimeters copper per gram oil

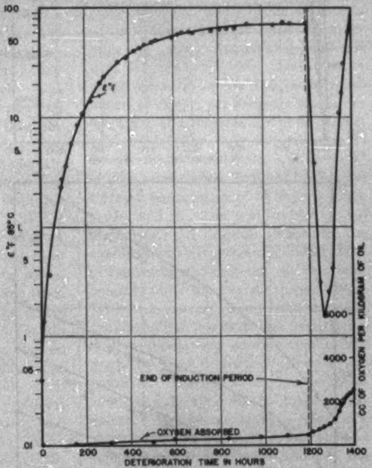


Figure 18 (right). Continuous - oxidation characteristics of hexadecane

760 millimeters oxygen, 85 degrees centigrade; 1.6 square centimeters copper per gram oil

Effect of Type of Hydrocarbon on Electrical Stability

Studies have been made of representative types of hydrocarbons to interpret the characteristics of mineral oils of varying compositions. Such studies have been made on the basis of both the limited and continuous type of tests. The hydrocarbons studied are given in Table VII. The group includes representative normal paraffins, an olefin, aromatics, and naphthenes. The group represented is a relatively narrow one. The molecular weight of these hydrocarbons is in most cases, considerably less than that for light cable or transformer oils. However, it is believed that the results are useful in interpreting the results obtained on hydrocarbon oils.

Preparation of Hydrocarbons

All of the samples were dried over sodium wire before fractionation in a column of 40 theoretical plates. The preparations of decane, hexadecane, hexadecene, cis-decalin, and tetralin have been described previously.[5] Phenylcyclohexane and dicyclohexyl were obtained from the Eastman Kodak Company. Refractive indexes of the fractions were measured throughout the distillation, and the proper samples were selected.

Limited-Oxidation Tests on Hydrocarbons

Limited-oxidation tests on the foregoing hydrocarbons are shown in Figures 15, 16, and 17. These tests were made at 85 degrees centigrade with a copper-oil ratio of 1.6 and available amounts of oxygen as indicated. Figure 15 shows the results on decane, hexadecane, and hexadecene. Figure 16 gives the results for cis-decalin, and Figure 17 the results for tetralin, phenylcyclohexane, and dicyclohexyl. The results indicate that the normal paraffins produce relatively high losses under limited-oxidation tests. It is somewhat surprising that both decane and hexadecane produce relatively larger losses than those indicated for hexadecene which is an unsaturated paraffin. The conductance of hexadecene increases much more rapidly with deterioration time than for the normal paraffins. This is probably due to the fact that the oxidation rate or the absorption of any available oxygen is much more rapid for the unsaturated paraffin than for the normal paraffins. In the case of cis-decalin which is a naphthenic type, the nature of the limited-oxidation loss is relatively different from that for the paraffins. In this case, the losses immediately increased to relatively high values and then decreased to substantially lower values with increasing deterioration time. Furthermore, the limited test in which 105 and 439 cubic centimeters of oxygen per kilogram of oil are made available to the oil gives substantially lower losses than is indicated for the blank test. This is probably because cis-decalin oxidizes very rapidly, and, accordingly, even with relatively small amounts of oxygen, the oxidation proceeds to normal end products which are not highly conducting. The relatively high initial values are probably due to contact with copper, even though only a short time elapsed between contact and initial measurement. Thus, the initial values of conductance are not indicative of the hydrocarbon oil itself. The results for tetralin, phenylcyclohexane, and dicyclohexyl are very much different from the characteristics of cis-decalin.

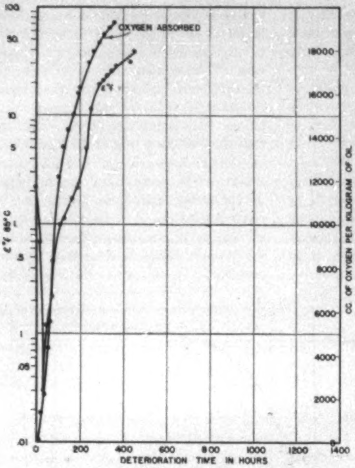


Figure 19 (left). Continuous-oxidation characteristics of cis-decalin

760 millimeters oxygen, 85 degrees centigrade; 1.6 square centimeters copper per gram oil

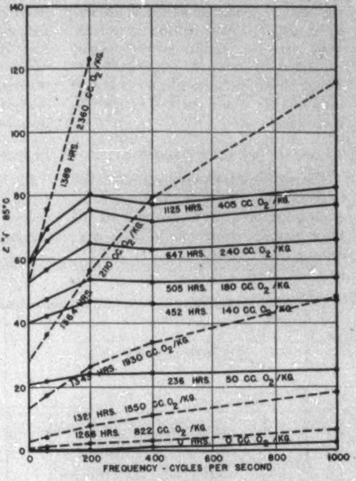


Figure 20 (right). Conductance as a function of frequency — hexadecane-paper cell—continuous oxidation at different stages of the oxidation

760 millimeters oxygen, 85 degrees centigrade; 1.6 square centimeters copper per gram oil; deterioration time in hours and cubic centimeters oxygen per kilogram oil absorbed indicated

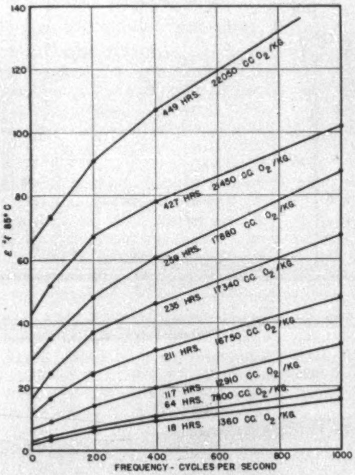


Figure 21 (left). Conductance as a function of frequency—cis-decalin-paper cell—continuous oxidation at different stages of the oxidation

760 millimeters oxygen, 85 degrees centigrade; 1.6 square centimeters copper per gram oil; deterioration time in hours and cubic centimeters oxygen per kilogram oil absorbed indicated

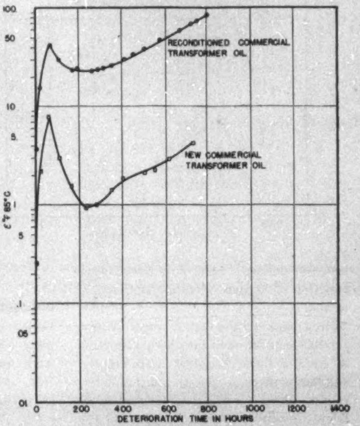


Figure 22 (right). Comparison of limited-oxidation characteristics of a new and a reconditioned commercial transformer oil

Limited oxidation, 100 cubic centimeters oxygen per kilogram oil, 85 degrees centigrade; 1.6 square centimeters copper per gram oil

Furthermore the losses of dicyclohexyl, a naphthene, are relatively low, compared to the previously discussed results of the normal paraffins.

Continuous-Oxidation Tests on Hydrocarbons

Continuous-oxidation tests have been made on both hexadecane and cis-decalin. The results of these tests are shown in Figures 18, 19, 20, and 21. These tests were made at 85 degrees centigrade and a copper-oil ratio of 1.6. Electrical measurements were made on both the hydrocarbon itself and on an impregnated-paper cell, using Whatman's 41-*H* ashless filter paper. The continuous-oxidation characteristics of hexadecane as indicated by the $\epsilon''f$ of the hydrocarbon and the oxygen absorbed as a function of deterioration time are shown in Figure 18. This hydrocarbon is very stable, with an oxygen absorption of approximately 600 cubic centimeters per kilogram at the end of 1,200 hours. At the end of this induction period, the rate of oxygen absorption increases very greatly. During the induction period of the hexadecane, the conductance or power factor increased to relatively large values, approximating 60 per cent. At the end of the induction period of approximately 1,200 hours, the conductance very rapidly decreased to a value approximating 0.1. Thus, even though this is a continuous-oxidation test, the hexadecane exhibited limited-oxidation characteristics in view of its inherent oxidation characteristics. During this induction period, the electrical characteristics correspond to the limited test previously discussed and shown in Figure 15. These results are comparable to those for the commercial oil shown in Figure 8 which also had an initial induction period. Following the transition from the limited to the continuous type of test, the conductance of the hexadecane again increased to relatively large values with continued oxygen absorption. Figure 19 shows the corresponding continuous-oxidation characteristics for cis-decalin. As compared to the hexadecane, cis-decalin has a high rate of oxygen absorption. During an initial short period, the conductance of the cis-decalin decreases with time, giving a very low minimum value, approximating 0.2 with an oxygen absorption of approximately 2,000 cubic centimeters per kilogram. Following this minimum value, the conductance rapidly increases to materially higher values. Thus the transition from limited- to continuous-oxidation characteristics proceeds very rapidly at the start of the test. These results check with the limited characteristics, as previously described and shown in Figure 16. The electrical characteristics of the hydrocarbon-impregnated cells for the continuous tests of hexadecane and cis-decalin are shown in Figures 20 and 21. The conductance of the paper cell is shown at 85 degrees centigrade for frequencies from 60 cycles to 1,000 cycles per second. It is important that the conductance of the paper cell is relatively independent of frequency during the induction period for the hexadecane. Following the induction period, the conductance shows a gradually increasing characteristic with increasing frequency. In the case of cis-decalin, the conductance of the paper cell is analogous to that of the hexadecane after the induction period. These results show that, during the limited oxidation of hexadecane, the conductance of the hydrocarbon-paper combination is due principally to the conductance of the hexadecane contained in this cell. A conductance increasing with frequency is generally indicative of oxidation products which are adsorbed by the hydroxyl groups of the cellulose. These results indicate that the reaction products formed during the limited period are fundamentally different from those formed during the continuous period. Apparently the oxidation products or other compounds formed during the limited period are not of the type to be adsorbed selectively by the paper. In analyzing the results of the hydrocarbon-paper cell, note that Whatman's 41-*H* ashless filter paper has been used, instead of cable paper. The filter paper is of much lower density than cable paper, and so the results shown for the conductance as a function of frequency are more pronounced than would be the case with the higher-density paper.

Comparison of New and Reconditioned Transformer Oils

Limited-oxidation tests on a new and on a reconditioned commercial transformer oil are shown in Figure 22. These tests were made at approximately 100 cubic centimeters of oxygen per kilogram of oil, a copper-oil ratio of 1.6, and a temperature of 85 degrees centigrade. The results show that the reconditioned oil gives very much higher losses than the new oil. Separate measurements on the two oils in the absence of copper indicate approximately equal power factors for the oils. The high initial power factor for the reconditioned oil in Figure 22 is due to the time that the oil has contacted copper, after introducing oil to the system before an electrical test can be made. There are a number of factors that might be considered to give the differences between the two samples. Certain stabilizers that are present in the oil might have been used up in the initial service for the reconditioned oil. It is also possible that oxidation products produced under service conditions and not completely removed in the reconditioning treatment might make the reconditioned oil sensitive to copper in such a limited test. Both oils have essentially the same characteristics, and these tests were made at about the same time. Later tests made after the oils had been standing for some time indicate some change in the characteristics. This change with time has also been noted for other samples. However, at a given time, satisfactory check runs generally can be made.

Electrical Measurements at Low Temperatures

In analyzing the characteristics of mineral oils, it is difficult to determine the relative effects of the hydrocarbon components and of the nonhydrocarbon components, such as sulphur compounds. Normally refined oils generally have relatively low power factors or conductivities; since these oils do contain nonhydrocarbons, these components apparently do not contribute to the conduction type of loss at elevated temperatures. However, at relatively low temperatures it may be possible to determine the presence of these materials, since the conduction loss would be eliminated by the increased viscosity of the oil. Accordingly, tests have been made on a number of samples over a frequency range of 100 cycles to 15 kilocycles at a temperature range of 85 to −70 degrees centigrade. These results are shown in Figures 23 and 24. Figure 23 gives the results for an experimental transformer oil after a limited-oxidation test with lead-oil ratio of 1.6, a temperature of 85 degrees centigrade, after 385 hours in the absence of oxygen. Figure 24 gives the results on the same oil under limited-oxidation test with 0.1 per cent cetyl mercaptan, a copper-oil ratio of 16, 105 cubic centimeters of oxygen per kilogram of oil after 648 hours at a temperature of 85 degrees centigrade. The electrical measurements are given in terms of the loss factor ϵ'' at the temperature indicated. At the elevated temperature of 85 degrees centigrade, and at room temperature of approximately 25 degrees centigrade, the conductance $\epsilon'' f$ is practically independent of frequency, showing that in this temperature range the loss is of the ionic type. With successively lower temperatures, the conduction loss decreases to relatively small amounts, and another type of loss gradually gives increasing values of loss factor ϵ''. The loss at the low temperatures is of the type due to dipole polarization. Measurements have also been made on the oil by itself, and at the low temperatures it gives approximately the same results. Certain types of hydrocarbons do have structures such as to give dipole moments. However, for the usual values it would be necessary for practically all of the hydrocarbons to have a dipole moment of approximately 0.5 Debye unit to contribute the loss factors indicated at the lower temperatures. This seems to be a rather unreasonable assumption. Accordingly, it is believed that other nonhydrocarbon components are at least partly responsible for the observed loss. Tests have also been made at low temperatures and over a wide frequency range for the same oil before and after contacting copper. Even though there was a very large difference between the power factor at 85 degrees centigrade, there was very little difference in the loss factor at the very low temperatures. These results indicate at least qualitatively that the electrical instability with copper under limited tests is due to an ionic type of

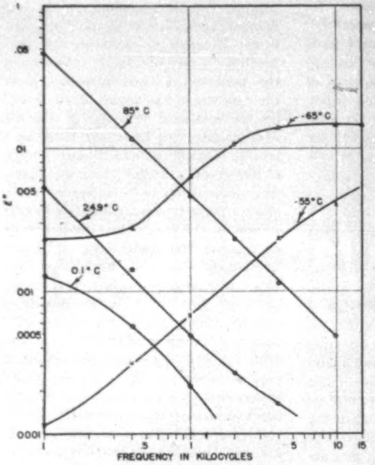


Figure 23 (left). Loss factor as a function of frequency and temperature

Experimental commercial transformer oil, limited oxidation, 85 degrees centigrade, 0 cubic centimeters oxygen per kilogram oil, 1.6 square centimeters lead per gram oil, measurements made at the end of 385 hours of test

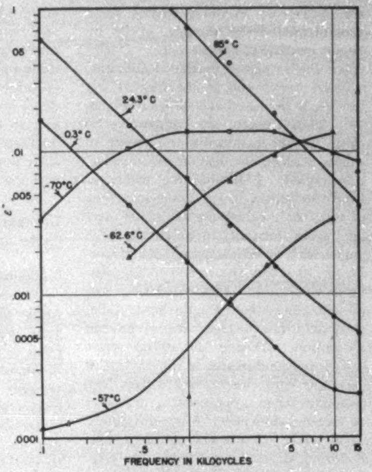


Figure 24 (right). Loss factor as a function of frequency and temperature

Experimental commercial transformer oil plus 0.1 per cent cetyl mercaptan, limited oxidation, 85 degrees centigrade, 100 cubic centimeters oxygen per kilogram oil, 16 square centimeters copper per gram oil, measurements made at the end of 648 hours of test

loss and that contacting copper does not change appreciably the loss characteristics of the nonhydrocarbon components of the oil.

Summary of Results

1. The presence of copper produces the electrical instability in limited-oxidation tests. Tests on several oils have indicated that the power factor is nearly proportional to the amount of copper area present, at least over a limited range.

2. The presence of lead in a limited-oxidation test produces results analogous to those for copper. Lead is somewhat more difficult to work with, because of the difficulty in cleaning the lead surfaces and also in obtaining convenient lead-oil ratios.

3. The presence of paper in limited-oxidation tests generally has a negligible effect. The products that are responsible for the limited-oxidation type of loss are not adsorbed by the paper. In the continuous-oxidation test, the presence of paper may play an important part. This is due to the fact that, under continuous oxidation, acids and water are produced which are readily adsorbed by the paper.

4. A study of the copper-oil ratios in oil-paper cables indicates that relatively high values may be obtained for certain limiting conditions, such as for the oil in the strand space or for the oil in the layer of insulation next to the sheath or conductor. It is rather difficult to reproduce these ratios in a satisfactory test cell. It is believed that these ratios are quite important in the operating performance of oil-paper cables.

5. An oil with a small percentage of aromatics generally gives a more stable oil under limited oxidation in the presence of copper from an electrical point of view. The amount of the aromatics as indicated by specific dispersion is not too definite, but is believed to be somewhere between the 110 and 120 range. It may be that the amount of aromatics for electrical stability is not only a function of the amount of aromatics themselves, but is also a function of the copper-oil ratio and the amount of available oxygen in a limited test.

6. Oils with different physical characteristics show wide differences in the electrical characteristics under the limited-oxidation tests. For example, certain oils will give maximum electrical instability, as indicated by high power factor, with the oil contacting copper in the absence of oxygen. Generally these oils will produce lower power factors with increasing amounts of available oxygen. Other oils give a maximum electrical instability as indicated by high power factor, with amounts of available oxygen in the range of a few hundred cubic centimeters of oxygen per kilogram of oil.

7. Overrefined oils generally give good electrical stability in the initial period of a continuous-oxidation test. Such oils generally produce large amounts of water and low-molecular-weight acids.

8. Representative straight hydrocarbons exhibit somewhat the same characteristics under limited-oxidation tests as apply to commercial electrical insulating oils. Accordingly, it is believed that the type of hydrocarbon plays at least some part in the characteristics of a given mineral oil.

9. The nonhydrocarbon components of a mineral oil determine to a large extent the characteristics of that oil under limited oxidation. In many cases, the addition of a few tenths of a per cent of added compounds may change very greatly the characteristics of an oil.

10. Oils under limited-oxidation test in the presence of copper may produce very high electric losses without any significant change in color. This would indicate that a copper compound formed is in the cuprous state.

11. It has been observed that mineral oils may change their limited-oxidation characteristics with time of standing in a container.

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An Improved Axial Air-Blast Interrupter for Severe Operating Duty

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HERETOFORE it has not been feasible to build power circuit breakers capable of many repetitive operations at high currents without maintenance and contact renewal. The evident need for breakers capable of scores of high-current interruptions between maintenance periods and, at the same time, easy and economical to maintain has stimulated the development of an improved circuit interrupter of oilless design.

The accomplishment of the design has been made possible by a further development of the axial air-blast principle, which is of truly American origin, having been conceived by Read about the turn of the century.

Analysis indicated that, because of the combination of repetitive duty requirements and the restricted space limitations imposed, the lower-interrupting-capacity breakers in the range from 100,000 to 250,000 kva would present the more difficult problem. The 150,000-kva 5-kv rating was, therefore, selected as a vehicle for the development of the new interrupter.

The axial air-blast interrupting principle was considered to offer the better possibilities, in view of the improvements over European designs,[1] reported by Edsall and Stubbs,[2] which permitted interruption of currents of the order of 60,000 rms amperes in the 500,000-kva 15-kv size. It was appreciated that the adaptation of the axial air-blast interrupting principle to the repetitive interruption of the 37,500-ampere (rms) maximum interrupting duty of the proposed 150,000-kva design would impose difficulties, because of the necessity of accommodating this breaker within the restricted space available in standard metal-clad switchgear.

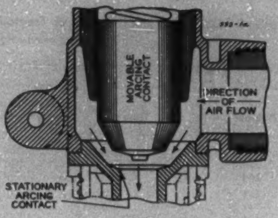
The basic theory of interruption by the axial air-blast interrupting principle and the sequence of operations for an axial air-blast circuit breaker were discussed by Edsall and Stubbs.[2] The physical arrangement of parts for the 150,000-kva air-blast breaker, to which the new interrupter has been applied, differs somewhat from that of the 500,000-kva breaker described in the Edsall–Stubbs paper. The arrangement and sequence of operation for the new 150,000-kva breaker are described in Appendix A.

Development of the Interrupter

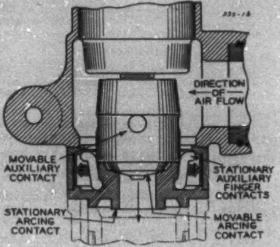
In the interest of an appreciation of the problems involved and their solution, it is desirable and interesting to review briefly the various designs of interrupters tested, intermediate between the interrupting chamber described by Edsall and Stubbs and the new interrupter.

Figure 1A shows the initial adaptation of the American axial air-blast interrupting chamber[2] to the space limitations imposed by the 150,000-kva 5,000-volt air-blast circuit breaker within a standard vertical-lift metal-clad switchgear unit having an over-all width of 26 inches. The contacts are shown in the vertical arrangement used in the adaptation to the limiting dimensions of the equivalent three-pole-in-one-tank oil circuit breaker. The arrangement of terminals corresponds to that of the oil circuit breaker. In the 600-ampere size shown, only a single plunger-type contact was used. The arrangement shown in Figure 1A was capable of performing up to about 25,000 rms amperes but developed occasional trouble on high peak inrush currents because of contact freezing.

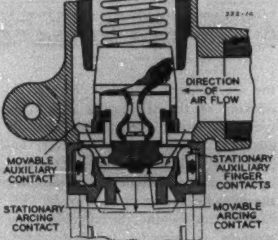
In order to overcome the freezing tendencies, auxiliary stationary finger contacts co-operating with a bell-shaped movable auxiliary contact were added as shown in Figure 1B. The movable auxiliary contact was arranged in a telescoping relationship with the movable arcing contact to insure the proper sequence of operation. This arrangement eliminated the freezing tendencies experienced with



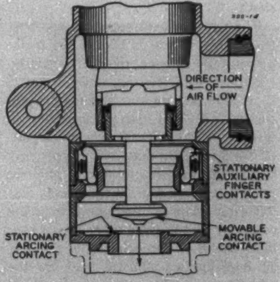
A. Contacts in interrupting position


DIRECTION OF AIR FLOW
MOVABLE AUXILIARY CONTACT
STATIONARY AUXILIARY FINGER CONTACTS
STATIONARY ARCING CONTACT
MOVABLE ARCING CONTACT

B. All contacts in normal position


DIRECTION OF AIR FLOW
MOVABLE AUXILIARY CONTACT
STATIONARY AUXILIARY FINGER CONTACTS
STATIONARY ARCING CONTACT
MOVABLE ARCING CONTACT

C. All contacts in interrupting position


DIRECTION OF AIR FLOW
STATIONARY AUXILIARY FINGER CONTACTS
STATIONARY ARCING CONTACT
MOVABLE ARCING CONTACT

D. All contacts in interrupting position

Figure 1. Experimental interrupting chambers

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The authors acknowledge contributions by the staff of the circuit-breaker testing laboratories and engineering departments of the Allis-Chalmers Manufacturing Company.

Table I. Data on Typical Series of Single-Phase Interrupting Tests Using Experimental 150,000-Kva Interrupter Shown in Figure 1C

Complete Series Made With Single Set of Contacts Shown in Figure 3

Test Number	Duty†	Test Voltage (Single-Phase)	Rms Amperes Interrupted	Arcing Time (Cycles)**	Breaker Time (Cycles)
109–140.1	CO—30 seconds	2,330	22,000	0.35	2.8
–140.2	—CO—15 seconds	1,970	20,000	0.05	2.5
–140.3	—CO	1,750	20,100	0.15	2.55
–141.1*	CO—30 seconds	2,330	22,100	0.0*	2.4
–141.2*	—CO—15 seconds	1,970	22,100	0.15	2.85
–141.3*	—CO	1,750	20,800	0.2	2.9
–142.1	CO—30 seconds	2,330	21,400	0.15	2.9
–142.2	—CO—15 seconds	1,970	19,850	0.5	2.95
–142.3	—CO	1,750	16,450	0.6	3.25
–143.1	CO—30 seconds	2,330	19,200	0.45	3.0
–143.2	—CO—15 seconds	1,970	20,800	0.15	2.8
–143.3	—CO	1,820	19,860	0.05	2.5
–144	Calibration				
–145.1	CO—30 seconds	2,330	21,650	0.1	2.9
–145.2	—CO—15 seconds	1,970	21,650	0.1	2.9
–145.3	—CO	1,750	20,800	0.3	2.8
–146.1	CO—30 seconds	2,330	21,200	0.25	3.05
–146.2	—CO—15 seconds	2,330	21,900	0.2	3.05
–146.3	—CO	1,820	20,750	0.2	2.6
–147.1	CO—30 seconds	2,330	21,900	0.4	3.0
–147.2	—CO—15 seconds	1,970	21,900	0.25	2.65
–147.3	—CO	1,750	20,100	0.1	2.5
–148.1*	CO—30 seconds	2,330	22,900	0.1	3.0
–148.2*	—CO—15 seconds	1,970	20,900	0.1	2.8
–148.3	—CO	1,840	21,300	0.2	3.0
–149.1	CO—30 seconds	2,330	21,900	0.1	3.0
–149.2	—CO—15 seconds	1,980	22,400	0.2	2.9
–149.3	—CO	1,840	21,650	0.1	2.9
–150.1	CO—30 seconds	2,260	21,900	0.1	2.8
–150.2	—CO—15 seconds	1,910	22,400	0.2	3.1
–150.3	—CO	1,650	21,100	0.1	2.6

* Oscillograms of these tests are shown in Figure 4.

† CO—close–open.

** Arcing time is measured in time units of one cycle or $^1/_{60}$ second. All arcing times are one current loop or less after adequate parting of the arcing contacts.

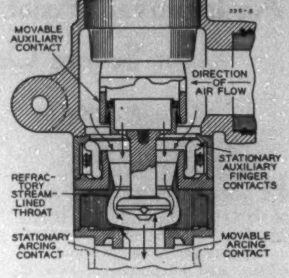

MOVABLE AUXILIARY CONTACT
DIRECTION OF AIR FLOW
STATIONARY AUXILIARY FINGER CONTACTS
REFRACTORY STREAMLINED THROAT
STATIONARY ARCING CONTACT
MOVABLE ARCING CONTACT

Figure 2. Final experimental interrupting chamber with refractory throat

All contacts in interrupting position

the arrangement shown in Figure 1A. Sticking of the telescoping action between the movable arcing and movable auxiliary contacts was, however, experienced at high interrupting currents because of the blowback of metal along the movable arcing contact body. Because of this sticking, the movable arcing contacts occasionally failed to reset, resulting in the arc being drawn between the auxiliary contacts and consequent inefficient interruption on subsequent shots.

The movable arcing contact was next revised, as shown in Figure 1C, to incorporate an annular groove or metal trap equipped with cooling jets. Table I shows the interrupting performance obtained with this arrangement when tested at 2,300 volts across a single pole. Of the 30 interruptions at approximately 25,000 rms amperes tabulated, all were interrupted at the first current zero following adequate parting of the arcing contacts; and a single set of arcing contacts was used throughout the series of 30 interruptions. Figure 3 shows the movable arcing contact used during these tests placed beside an unused contact of the same design for purpose of comparison. Figure 4 shows a series of typical test oscillograms from Table I.

Table II shows an additional series of single-pole interrupting tests made at currents ranging from 3 to 19,000 amperes with as high as 4,500 volts across the single pole. The majority of the tests for which data are given in Table II was made on a close–open—30 second—close – open — 15 second — close – open basis. A series of typical oscillograms from Table II is shown in Figure 6.

When an attempt was made to extend the device shown in Figure 1C to operation at currents up to 37,500 rms amperes, inconsistencies in performance developed, apparently, because of chance disposition of the metal vapor thrown from the contact surfaces and driven back up the air stream. Chance disposition occasionally placed this vapor between the auxiliary current-carrying contacts in such position and in such quantities as to cause reignition of the arc at this point. This difficulty appears to have been similar to that encountered by Prince, Henley, and Rankin[1] which they ascribed to the plugging of the orifice by a large diameter arc.

Figure 1D shows the next major step, in which the arcing contacts were longitudinally displaced below the auxiliary contacts to provide isolation between the arcing contacts and the auxiliary contacts. A metal subchamber or trap was inserted between the auxiliary contacts and the arcing contacts, into which the major proportion of the metal vapor would be driven and trapped. This arrangement showed a marked improvement in interrupting performance over the arrangement shown in Figure 1C, but difficulty was encountered because of an occasional tendency of the arc terminal on the stationary arcing contact to move outward away from the orifice along the floor of the metal subchamber, thereby escaping from the effective zone of the air blast.

In all of these arrangements (Figures 1A to 1D) plain copper or brass arcing-contact surfaces were used. Thus, even with the base metal contacts, this device is capable of large numbers of interruptions without contact maintenance at currents up to the breaker maximum rating of 37,500 amperes.

The final experimental arrangement, incorporating a streamlined refractory throat in place of the metal subchamber, is shown in Figure 2. The throat was

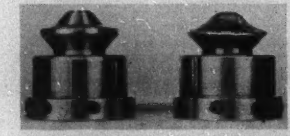


Figure 3. Arcing contacts as used in experimental interrupting chamber, figure 1C

Left—Unused contacts
Right—Contact after 30 shots at 25,000 rms amperes

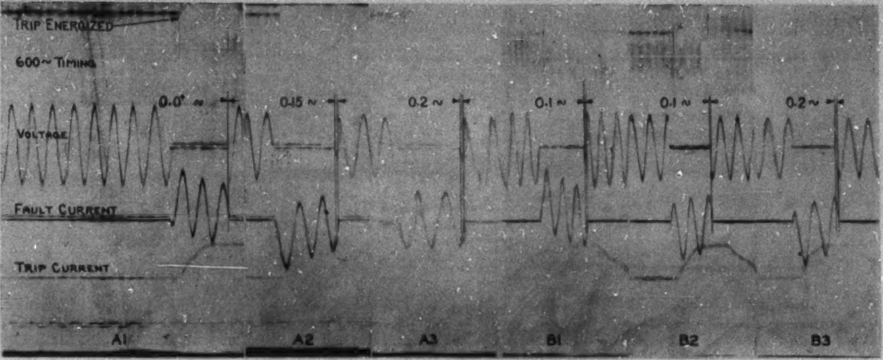


Figure 4 (above). Typical test oscillograms from Table I

Tests on interrupter shown in Figure 1C at 2,300 volts across a single pole

Oscillogram Number	Current Interrupted (Amperes)	Restored Voltage Across Pole Unit (Volts)	Duty Cycle*
A1	22,100	2,330	CO—30 seconds
A2	22,100	1,970	—CO—15 seconds
A3	20,800	1,750	—CO
B1	22,900	2,260	CO—30 seconds
B2	20,900	1,910	—CO—15 seconds
B3	21,300	1,770	—CO

*CO—close–open.

designed to collect and cool metallic vapor and particles and to shield the auxiliary contacts. Further, the inner surface of the throat was so arranged as to streamline the air flow behind the orifice so as to result in a highly effective sweeping of the zone of arc inception. The air flow approaching the orifice was then such, in combination with the sudden expansion of the air immediately beyond the orifice, as to result in a highly effective diffusion and cooling of the arc products which has been shown[3] to be essential to efficient arc interruption. Tungsten–silver arcing-contact facings were used to limit further the emission of metallic vapor and to increase contact life.

Table III indicates the performance of the final experimental interrupting chamber during a series of 16 close–open interruptions, at 35,000 to 40,000 rms amperes, made with reduced air pressures of from 120 to 130 pounds per-square-inch gauge. Figure 5 shows a set of stationary and movable arcing contacts, taken from one pole of the three-pole breaker tested, beside a set of new contacts of the same design. The condition of the used contacts clearly indicates that they were capable of considerable

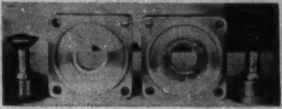


Figure 5. Movable and stationary arcing contacts as used in final experimental interrupting chamber shown in Figure 2

Left—Unused contacts

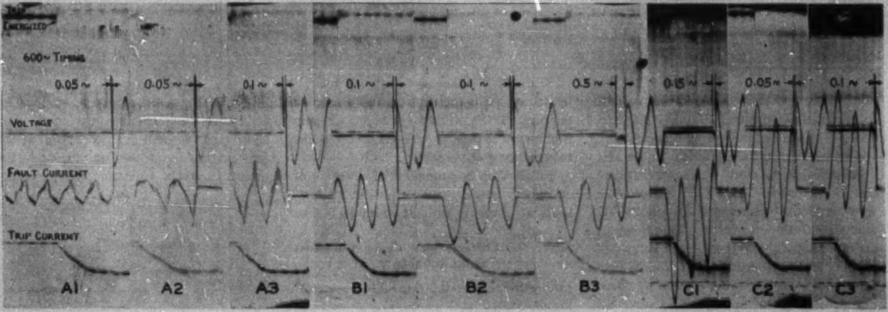
Right—Contacts after 16 shots at approximately 37,500 rms amperes

Figure 6 (below). Typical test oscillograms from Table II

Tests on interrupter shown in Figure 1C at voltages up to 4,500 volts across a single pole

Oscillogram Number	Current Interrupted (Amperes)	Restored Voltage Across Pole Unit (Volts)	Duty Cycle*
A1	5.1	2,320	O—15 seconds
A2	9.3	2,040	O
A3	16.7	2,180	O
B1	22,500.0	2,330	CO—15 seconds
B2	22,900.0	2,180	—CO—15 seconds
B3	24,800.0	2,040	—CO
C1	19,400.0	4,370	CO—15 seconds
C2	17,000.0	4,170	—CO—15 seconds
C3	18,350.0	4,020	—CO

* CO—close–open.



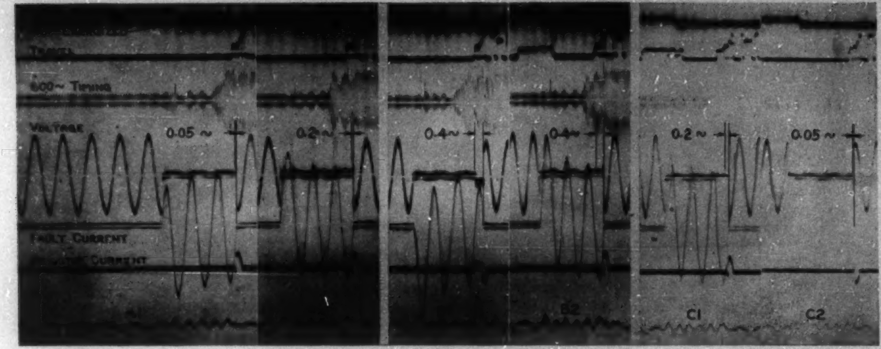


Figure 7 (above). Typical test oscillograms from Table III

Interrupter as shown in Figure 2 at 2,300 volts across a single pole

Oscillogram Number	Current Interrupted (Amperes)	Restored Voltage Across Pole Unit (Volts)	Air Pressure (Pounds)	Duty Cycle*
A1	39,700	2,070	130	CO—15 seconds —CO
A2	38,000	1,880	122	
B1	39,800	2,070	130	CO—15 seconds —CO
B2	34,800	1,880	122	
C1	39,700	2,070	130	CO—15 seconds —CO
C2	38,000	1,880	122	

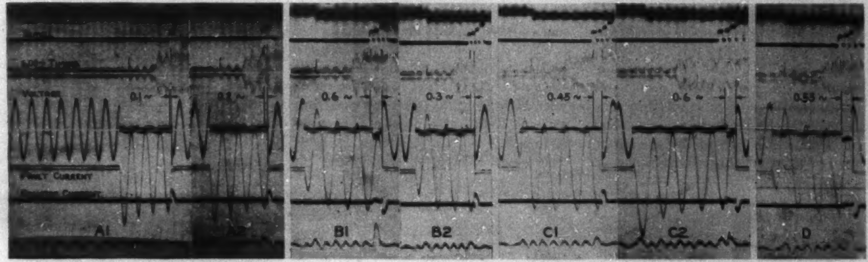
* CO—close–open.

Figure 8 (below). Typical test oscillograms from Table IV

Interrupter as shown in Figure 2 at 2,300 volts across a single pole and with reduced air pressures of 110 to 80 pounds per square inch

Oscillogram Number	Current Interrupted (Amperes)	Restored Voltage Across Pole Unit (Volts)	Air Pressure (Pounds)	Duty Cycle*
A1	39,700	2,070	110	CO—15 seconds —CO
A2	38,000	1,880	103	
B1	38,000	2,070	100	CO—15 seconds —CO
B2	38,000	1,880	94	
C1	39,000	2,070	90	CO—15 seconds —CO
C2	38,000	1,810	84	
D	38,000	2,070	80	CO

* CO—close–open.



additional performance at the same high interrupting currents. Figure 7 shows a series of typical oscillograms taken during the series of tests tabulated in Table III.

A series of 25 close–open interrupting tests is shown in Table IV. These tests were made at currents between 35,000 and 40,000 amperes with a single set of arcing contacts and at low pressures ranging from 110 down to 80 pounds per-square-inch air pressure. Even at these low pressures the arcing times were all less than one current loop after adequate parting of the arcing contacts. Figure 8 shows a series of typical oscillograms taken during these tests.

Table V is a summary of a series of 100 shots made on a single set of contacts at currents ranging from 10,000 to 40,000 amperes, all at or below rated minimum operating pressure. The peak inrush currents closed on some of these tests were as high as 115,000 amperes. The contacts used during this series of tests are shown in Figure 9.

The final design of the new interrupting device is shown in Figure 10. Figure 11 shows a typical set of arcing contacts after 26 close–open operations at 37,500 amperes with this arrangement.

Table VI shows a series of three-phase tests made at currents of the order of 40,000 to 50,000 amperes. The arcing time on these tests, most of which were far in excess of the maximum current rating, was in all cases less than one-half cycle.

It will be noted from the various interrupting-test oscillograms and data shown that consistent interruption at the first current zero following parting of the arcing contacts (maximum one current

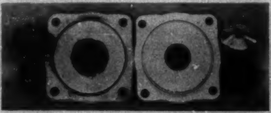


Figure 9. Contacts used during test series shown in Table V

Left—After tests at 10,000 to 41,000 amperes
Right—Unused

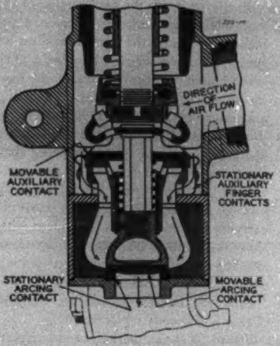

DIRECTION OF AIR FLOW
MOVABLE AUXILIARY CONTACT
STATIONARY AUXILIARY FINGER CONTACTS
STATIONARY ARCING CONTACT
MOVABLE ARCING CONTACT

Figure 10. Final design of interrupting device with refractory streamlined throat and tungsten–silver-surfaced arcing contacts

All contacts in normal position

loop* arcing time) was obtained throughout the range from zero to far beyond maximum rated interrupting amperes.

A large proportion of these tests were close–open shots, which fully demonstrated the ability of the high-speed isolating contacts to close in on high inrush currents. These isolating contacts have repeatedly closed against peak inrush currents up to 115,000 amperes without appreciable burning. Figure 12 is an oscillogram of a close–open shot on which the breaker closed against 115,000 peak amperes. Literally hundreds of high-current closures without maintenance are to be expected. Thus, the inherent closing as well as opening abilities of this breaker are far superior to that of any oil or magnetic breaker with which we are familiar.

The Breaker

The new axial air-blast breaker is designed to recognize the requirements of modern metal-clad switchgear. The breaker is comparable in size and shape to the common-frame indoor oil circuit breaker of the same rating. The arrangement of terminals is the same as for the equivalent oil circuit breaker, and the supporting framework is so arranged as to enable complete enclosure if required. Figure 13 shows the general arrangement of parts. A 2,000-ampere 5,000-volt 150,000-kva breaker is shown in Figure 14.

Table II. Data on Typical Series of Single-Phase Interrupting Tests Using Experimental 150,000-Kva Interrupter Shown in Figure 1C

Test Number	Duty†	Test Voltage (Single-Phase)	Rms Amperes Interrupted	Arcing Time (Cycles)**	Breaker Time (Cycles)
109–117.1*	O—15 seconds	2,320	5.1	0.05	2.2
-117.2*	—O	2,040	9.3	0.05	2.1
-118.1	O—15 seconds	2,180	14.2	0.1	2.4
-118.2	—O	2,180	14.8	0.1	2.25
-119.1*	O—15 seconds	2,180	16.7	0.1	2.4
-119.2	—O	2,110	16.	0.1	2.15
-120.1	CO—15 seconds	2,330	12,600	0.05	2.65
-120.2	—CO	2,180	10,700	0.2	2.9
-121.1	CO—15 seconds	2,260	22,500	0.1	2.6
-121.2	—CO—15 seconds	2,260	22,900	0.1	2.55
-121.3	—CO	2,110	24,800	0.5	2.85
-122.1*	CO—15 seconds	2,400	22,500	0.1	2.7
-122.2*	—CO—15 seconds	2,180	26,100	0.4	2.85
-122.3*	—CO	2,040	22,500	0.4	3.3
-123	CO	4,360	15,200	0.2	3.05
-124.1	CO—15 seconds	4,820	16,400	0.45	3.2
-124.2	—CO—15 seconds	4,450	16,400	0.45	2.75
-124.3	—CO	4,230	16,750	0.0†	2.4
-125.1	CO—15 seconds	4,670	18,350	0.35	2.85
-125.2	—CO—15 seconds	4,370	15,900	0.3	2.75
-125.3	—CO	4,230	15,800	0.3	2.7
-126.1*	CO—15 seconds	4,600	19,400	0.15	2.9
-126.2*	—CO—15 seconds	4,370	17,000	0.05	2.35
-126.3*	—CO	4,170	18,350	0.1	2.4
-127.1	CO—15 seconds	4,370	17,450	0.3	2.75
-127.2	—CO—15 seconds	4,170	18,000	0.1	2.55
-127.3	—CO	4,020	19,200	0.1	2.5
-128.1	CO—15 seconds	4,600	17,100	0.45	2.8
-128.2	—CO—15 seconds	4,370	16,500	0.2	2.75
-128.3	—CO	4,230	16,300	0.0*	2.35
-129.1	CO—15 seconds	4,520	16,800	0.1	2.6
-129.2	—CO—15 seconds	4,370	19,100	0.1	2.65
-129.3	—CO	4,230	16,300	0.1	2.7

* Oscillograms of these tests are shown in Figure 6.

† O—open; CO—close–open.

** Arcing time is measured in time units of one cycle or 1/60 second. All arcing times are one current loop or less after adequate parting of the arcing contacts.

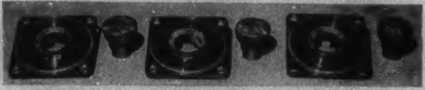


Figure 11. Contacts after 26 close–open operations at 37,500 amperes

Table III. Data on Typical Series of Single-Phase Interrupting Tests at Reduced Air Pressure [redacted] Using Final Experimental 150,000-Kva Interrupter Shown in Figure 2

Test Number	Duty†	Air Pressure (Lb Per Sq In.)	Test Voltage (Single-Phase)	Rms Amperes Interrupted	Arcing Time (Cycles)**	Breaker Time (Cycles)
109–315.1	CO—15 seconds	130	2,070	38,000	0.4	3.1
-315.2	—CO	122	1,940	36,300	0.6	3.3
-316.1	CO—15 seconds	130	2,230	39,200	0.3	3.1
-316.2	—CO	122	1,940	38,200	0.3	2.75
-317.1	CO—15 seconds	130	2,070	38,800	0.05	2.75
-317.2	—CO	122	1,880	38,800	0.2	2.9
-318.1*	CO—15 seconds	130	2,070	39,700	0.05	2.55
-318.2*	—CO	122	1,940	38,000	0.2	2.8
-319.1	CO—15 seconds	130	2,230	39,700	0.05	2.55
-319.2	—CO	122	1,880	38,000	0.2	2.7
-320.1*	CO—15 seconds	130	2,230	39,800	0.4	2.8
-320.2*	—CO	122	1,880	34,800	0.4	3.0
-321.1	CO—15 seconds	130	2,230	38,900	0.25	2.7
-321.2	—CO	122	1,940	39,700	0.2	2.8
-322.1*	CO—15 seconds	130	2,230	39,700	0.2	2.5
-322.2*	—CO	122	1,940	38,000	0.05	2.7

* Oscillograms of these tests are shown in Figure 7.

† CO—close–open.

** Arcing time is measured in units of one cycle or 1/60 second. All arcing times are one current loop or less after adequate parting of the arcing contacts.

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* It is to be expected that the arcing time will occasionally exceed one current loop by perhaps as much as one-tenth cycle, since the theoretical maximum arcing time is equal to one current loop plus the time necessary for the arcing contacts to reach the separation necessary for interruption.

Table IV. Extended Series of Single-Phase Interrupting Tests at Low Air Pressure Using Final Experimental 150,000-Kva Interrupter Shown in Figure 2

Test Number	Duty†	Air Pressure (Lb Per Sq In.)	Test Voltage (Single-Phase)	Rms Amperes Interrupted	Arcing Time (Cycles)**	Breaker Time (Cycles)
109-327.1*	CO—15 seconds	110	2,230	39,700	0.1	3.0
-327.2*	—CO	103	1,940	38,000	0.2	3.2
-328.1	CO—15 seconds	110	2,230	36,300	0.5	3.4
-328.2	—CO	103	1,940	39,700	0.4	3.2
-329.1	CO—15 seconds	110	2,230	38,800	0.2	3.1
-329.2	—CO	103	1,940	37,200	0.3	3.3
-330.1	CO—15 seconds	110	2,230	38,800	0.1	2.9
-330.2	—CO	103	1,940	38,800	0.35	3.45
-331.1	CO—15 seconds	110	2,230	38,800	0.5	3.3
-331.2	—CO	103	1,940	38,000	0.5	4.7
-332.1	CO—15 seconds	110	2,230	37,300	0.6	3.2
-332.2	—CO	103	1,940	38,000	0.15	3.15
-333.1	CO—15 seconds	110	2,230	39,700	0.3	3.3
-333.2	—CO	103	1,880	37,500	0.2	3.2
-334.1	CO—15 seconds	110	2,230	39,800	0.2	2.8
-334.2	—CO	103	1,940	38,800	0.25	3.25
-335	Calibration					
-336.1*	CO—15 seconds	100	2,230	38,000	0.6	3.6
-336.2*	—CO	94	1,940	38,000	0.3	3.2
-337.1	CO—15 seconds	100	2,230	38,800	0.25	5.25
-337.2	—CO	94	1,940	36,300	0.6	3.8
-338.1	CO—15 seconds	90	2,230	38,900	0.6	3.7
-338.2	—CO	84	1,940	37,400	0.55	3.75
-339.1*	CO—15 seconds	90	2,230	39,000	0.45	3.55
-339.2*	—CO	84	1,880	38,000	0.6	5.4
-340	Calibration					
-341*	CO	80	2,230	38,000	0.55	3.55

* Oscillograms of these tests are shown in Figure 8.

† CO—close–open.

** Arcing time is measured in units of one cycle or 1/60 second. All arcing times are one current loop or less after adequate parting of the arcing contacts.

Table V. Summary of Series of 100 Single-Phase Interrupting Tests Made at or Below Minimum Operating Pressure at Currents Ranging From 10,000 to 41,000 Amperes on a Single Set of Contacts, Shown in Figure 9, as Used in Final Experimental Arrangement Shown in Figure 2

Number of Shots	Rms Amperes Interrupted	Duty*	Arcing Time (Cycles)**
30	10,000–15,000	CO	0.05 to 0.4
21	15,000–20,000	CO	0.04 to 0.4
15	20,000–25,000	CO	0.05 to 0.6
21	25,000–30,000	CO	0.05 to 0.7
4	30,000–35,000	CO	0.1 to 0.45
9	35,000–41,000	CO	0.1 to 0.55
100			

* CO—close–open.

** Arcing time is measured in time units of one cycle or 1/60 second. All arcing times are one current loop or less after adequate parting of the arcing contacts.

Table VI. Data on Series of Three-Phase Interrupting Tests With Final Design of 150,000-Kva Interrupter Shown in Figure 10 at High Currents up to 70,000 Amperes on a Single Set of Contacts

Test Number	Duty*	Rms Amperes Interrupted			Arcing Time		
		A	B	C	A	B	C
118-301	CO	6,000	6,010	5,540	0.1	0.05	0.1
-302	CO	34,200	36,000	33,200	0.3	0.2	0.15
-303	CO	40,400	43,900	41,700	0.2	0.3	0.05
-304	CO	44,600	48,100	46,900	0.2	0.25	0.05
-305	CO	44,200	47,700	46,900	0.25	0.3	0.05
-307	CO	27,800	28,600	28,400	0.1	0.1	0.25
-311	CO	No film—approximately 45,000 amperes					
-312	CO	45,000	49,100	46,900	0.2	0.3	0.05
-314	O	44,600	48,800	46,500	0.25	0.25	0.1
-315	O	44,600	No film		0.3	No film	
-316	O	45,500	49,500	47,200	0.25	0.05	0.2
-317	O	45,000	48,800	47,200	0.25	0.1	0.25
-319	O	47,100	50,200	47,600	0.25	0.05	0.2
-320	O	47,100	51,000	47,600	0.2	0.05	0.25
-321	O	47,100	51,300	48,200	0.05	0.25	0.25

* O—open; CO—close–open.

Figure 10 shows the final version of the new interrupting device. The arrangement represents the consummation of the development herein described. The arrangement of the contacts, in conjunction with the pneumatic operating and interrupting systems, is such as to promote reliable effective high-speed operation. In the 2,000-ampere size, the contacts shown in Figure 10 are short-circuited, except during the interrupting process, by solid-copper main current-carrying contact blocks provided with silver contacts. The arcing contacts are faced with tungsten–silver arcing material. All contacts are readily accessible for inspection and are capable of easy and speedy replacement.

Figure 15 shows metal-clad switchgear installation using these breakers.

Conclusion

Comprehensive test data indicate that means have been found to augment the rate of ion diffusion, and to control metallic emission and its effects, so that axial air-blast circuit breakers, even in small physical sizes where space limitations impose severe penalties, not only are capable of interrupting the highest short-circuit currents to which they may be subjected with a maximum arcing time of one current loop, but also offer exceptional performance with extremely low maintenance even at those currents.

This is contrary to the findings published by other investigators[1,14] who found the current-interrupting "ceiling" of the axial (radial- or nozzle-type) air-blast circuit breaker to be too low for use on American systems. The considerable penetration beyond this "ceiling" obtained with the interrupter described, again confirms the adequacy of the axial air-blast circuit breaker for American use. The evidence indicates that no such fixed ceiling or point of breakdown of relative values exists, and that still further development and extension of the potentialities of the device may be expected.

The exhaustive test data submitted demonstrate that this small axial air-blast breaker, equipped with the improved interrupter, will interrupt successfully scores of faults, without maintenance, at currents up to 50,000 amperes (over 130 per cent maximum interrupting current rating) in one-half cycle or less. The burden of evidence implies that a single set of contacts will last throughout the life of the breaker on the average application. We submit, therefore, that this appears to be truly a severe duty

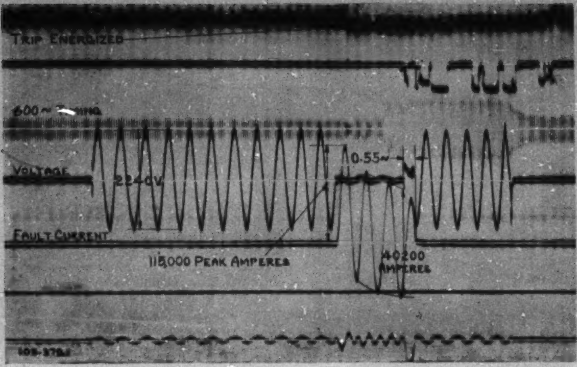

Figure 12. Oscillogram showing breaker closing against 115,000 peak amperes

interrupter, capable of meeting the requirements of the most exacting applications, without drastic derating even on severe repetitive duty cycles.

Appendix A. Circuit-Breaker Operating Sequence

The arrangement shown in Figure 13 is used for 2,000-ampere circuit breakers and consists essentially of a fabricated rectangular steel framework in which are mounted the interrupting members, isolating contacts, and an operating air tank, in which compressed air is stored for operation of the breaker. The breaker air tank 1 is mounted at the top front of the structure. It is connected to the interrupting assembly through a main air-blast valve 2 and hollow insulator 3. The interrupting assembly, consisting of a contact chamber 4 and interrupting chamber 5, is rigidly mounted between the terminal stud 8, and cooling chamber 9, which is connected to muffler 10.

When closed, the circuit through the breaker is from terminal stud 8, through bridging contacts 11 to terminal stud 12, and the operation is so arranged as to insure that the bridging contacts 11 will break first during the breaker opening operation and make last during closing. These bridging contacts are not used on 600- and 1,200-ampere breakers.

When the main bridging contacts 11 are open, the circuit through the breaker is from terminal stud 8 through the movable and stationary arcing contacts to bronze contact chamber 4, and thence through movable isolating contacts 15 and stationary isolating contacts 16 to terminal 12. Operation of bridging contact 11 and movable isolating contact 15 is obtained pneumatically through actuation of double-acting pistons which transmit their closing and opening impulses through connecting rods so arranged that they assume an overtoggling relationship when the contacts are in the closed position, thereby locking the contacts closed and preventing inadvertent opening of contacts 11 and 15 caused by magnetic reaction during the passage of high currents. Solenoid control valves 19 control the admission of air to the operating parts of the breaker during both the closing and opening functions.

Opening

Electrical operation of the opening control valve admits compressed air to the air-blast valve 2, causing immediate opening of the air-blast valve and admitting compressed air through insulator column 3 to contact chamber 4. The compressed air at once acts upon a piston attached to the upper end of the movable arcing contact (which is normally held closed by heavy springs). First the auxiliary current-carrying contacts (see Figure 10) are separated and are followed by withdrawal of the movable arcing contact from the stationary arcing contact (which is also the air outlet), drawing an arc between the arcing contacts; at the same time compressed air is allowed to pass through the initial arcing zone and through the orifice in the center of the stationary arcing contact into interrupting chamber 5 and through cooling chamber 9 and muffler 10 to atmosphere. On 2,000-ampere breakers pneumatic interlocks insure opening of the main bridging contacts before blast-valve operation is indicated.

Tests have shown that a short arcing-contact separation is essential in order to obtain maximum air-blast efficiency and maximum arc control.[2] In order to provide circuit isolation in the open position, therefore, isolating contacts 15 and 16 are connected in series with the arcing contacts.

As the arc is drawn between the arcing contacts, the swiftly flowing compressed air centralizes and extends the arc, forcing it into contact with an electrode situated below the orifice in the center of the interrupting chamber 5. The outer end of the electrode is connected to one end of a resistor noninductively wound around the outside of the cooling chamber 9 and protected by an outer insulation tube. The other end of the resistor is connected through insulated con-

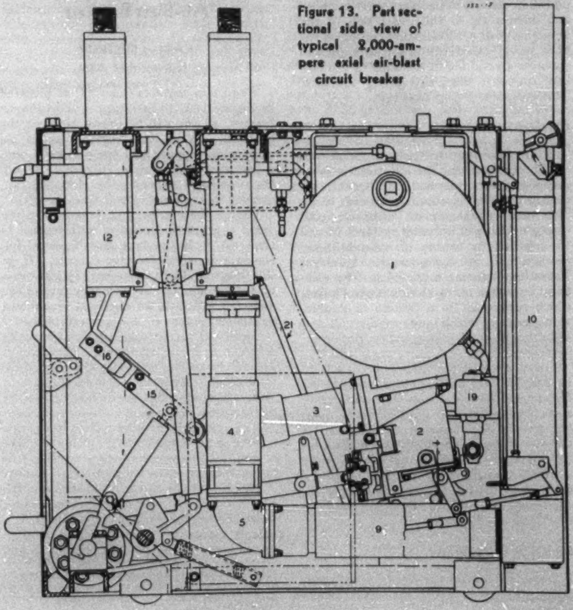

Figure 13. Part sectional side view of typical 2,000-ampere axial air-blast circuit breaker

nection 21 to terminal stud 8, so that, when the arc is forced into contact with the electrode, the resistor is automatically connected in parallel with the section of the arc between the movable arcing contact and the electrode.

As the current approaches zero, the highly effective diffusion and cooling produced in the arcing zone by the sudden expansion of the compressed air as it leaves the orifice, heightened by the streamlining of the air flow in the refractory chamber behind the orifice, causes rapid deionization of the arc path, preventing reignition of the arc following current zero, achieving circuit interruption.

Connection of the resistor into the circuit before current zero controls the amplitude and rate of rise of the recovery voltage appearing between the arcing contacts (see Appendix B) which, coupled with the high dielectric value of the compressed air passing between the contacts, assists in preventing reignition of the arc. Continued flow of air through the cooling chamber and muffler scavenges the interrupting chamber and projects the cooled arc products into the atmosphere.

Immediately following arc interruption, compressed air is permitted to act upon a double-acting piston; causing opening of isolating contacts 15 and 16 under no-load conditions; providing circuit isolation in series with the arcing contacts.

As the isolating contact 15 reaches the full open position, an auxiliary switch opens and a slide valve attached to the air-blast valve 2 is actuated. Opening of the auxiliary switch de-energizes the opening control valve; shutting off air supply to the blast-valve cylinder. The slide valve bleeds off the air already in the blast-valve cylinder and admits air to the other side of the actuating piston, accelerating closing of the blast valve. This cuts off the air supply to chamber 4 resulting in reclosing of the arcing contacts due to spring pressure. This completes the opening cycle.

Closing

As the interrupting contacts are always closed, except during the interrupting process, closing of the circuit is effected through closing of isolating contacts 15 and 16 on 600- and 1,200-ampere breakers, and through closing of isolating contacts 15 and 16, followed by closing of main bridging contacts 11 on 2,000-ampere breakers. These functions are accomplished by electrical actuation of the closing control valve, which admits air to a system of double-acting pistons, which move contacts 11 and 15 in the closing direction and in the correct sequences.

Coincident with the closing of contacts 11 on 2,000-ampere breakers, and with closing of contacts 15 and 16 on 600- and 1,200-ampere breakers, two limit switches close, one of which acts through the closing control relay to break the circuit to the closing control valve, cutting off the air supply to the isolating and bridging contact operating cylinders. Closing the other limit switch completes the tripping circuit in readiness for an opening operation.

Close–Open Operation

Dump valves are provided on the operating cylinders to permit high-speed close–open operation. Figure 16 shows the contact operating sequence for a close–open operation on a 2,000-ampere breaker. On the 600- and 1,200-ampere breakers the main bridging contact is omitted.

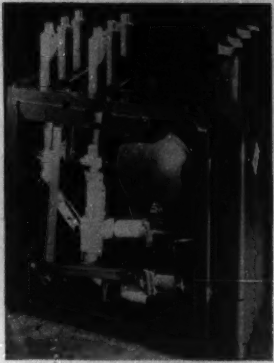

Figure 14. Typical axial air-blast circuit breaker, 2,000 amperes, 5,000 volts, 150,000-kva interrupting rating

Appendix B. Transient-Recovery-Voltage Control With Axial Air-Blast Breakers

KARL LERSTRUP
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When two contacts separate, as pictured in Figure 17A, to interrupt a short-circuit current, an arc will be drawn between the contacts. When the current passes its zero value, the arc will cease to exist, and a potential difference, e_0 (Figure 17B) will be established between the electrodes according to some function of time, $e_0=f(t)$.

We are most interested, however, in the value of the first derivative with respect to time, de_0/dt, in the very short interval following a zero value of the current. If, in this interval, the increase in electrical stress on the gap exceeds the recovery rate of the dielectric strength, an electrical breakdown will follow, and the arc will be re-established.[13]

Successful current interruption is usually accomplished by designing the circuit breaker in such a way that the rate of dielectric recovery is higher than the rate at which the electrical stress appears across the interrupting gap.

It is evident, however, that the same result can be obtained by reducing the rate of rise of the electrical stress, so that it is below the limit set by the dielectric recovery.

The rate of rise of electrical stress on the gap, immediately after a current-zero value, characterized by the derivative de_0/dt, is a system constant. It is mainly dependent upon sizes and locations of reactances and capacitances in the system. The valuable help derived from the use of reactors to reduce the short-circuit current and thereby help circuit interruption has been long recognized. It is less appreciated that any addition of series reactance to the circuit will tend to reduce the magnitude of the voltage rise de_0/dt as will the addition of shunt capacitance.

The extent to which such measures are helpful will depend largely upon the relative location. Possible sizes and locations of reactors are limited definitely by other considerations, but a wise application of capacitors undoubtedly will go a long way towards the relief of circuit breakers working close to their limits.

Another method is to accomplish the interruption by a double break with one gap bridged by a resistor, such as pictured in Figure 18A.

To understand the action of this resistor, we shall consider the situation immediately after a zero value of the current, such as pictured in Figure 18B. The two gaps are equivalent to two small capacitors in series, and since the resistance R is very small compared with the impedance of the capacitors c_1 and c_2 we can consider gap 1 as short-circuited, and we have a single-break proposition as pictured in Figure 17B. The voltage over gap 2, therefore, will increase with the full value de_0/dt as determined by the system constants.

Under this heavy stress, gap 2 will break down as pictured in Figure 18C. (If not, the interruption would be accomplished without benefit of the resistor, but this easy case is of no special interest.)

We shall now consider the conditions of gap 1 as pictured in Figure 18C during a short interval immediately after the breakdown of gap 2.

We have a circuit (Figure 19) containing the short-circuit impedance, Z, where $Z=r+jx$, and the resistor, R. To that circuit is applied an electromotive force, e, which we can assume to be constant in the very short interval considered. For this circuit we have the differential equation

$$e=L\frac{di}{dt}+(R+r)i$$

with the solution

$$i=\frac{e}{R+r}\left(1-\epsilon^{-\frac{t}{T}}\right)$$

where the time constant

$$T=\frac{L}{R+r} \quad \text{or} \quad T=\frac{x}{(R+r)\cdot 2\pi f}$$

where x is the reactance at power frequency f.

The voltage over gap 1, Figure 18C, therefore, is

$$e_1=i\cdot R=e\frac{R}{R+r}\left(1-\epsilon^{-\frac{t}{T}}\right)$$

Differentiating with respect to time, we get

$$de_1=e\frac{R}{x}\cdot 2\pi f\cdot \epsilon^{-\frac{t}{T}}\cdot dt$$

The maximum value of this voltage variation is for $t=0$, and, since r is usually

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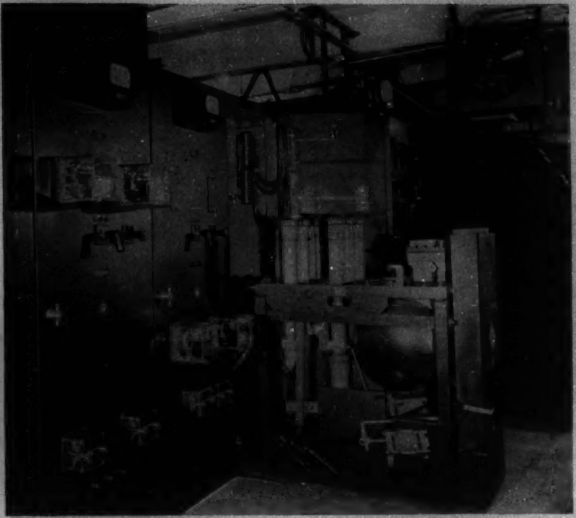


Figure 15. Metal-clad switchgear installation using breaker shown in Figure 14

small compared to x, e will be almost the crest value of the system voltage, and so we can write

$$\frac{de_1}{dt}\max = E_{\max}\frac{R}{x}2\pi f$$

This voltage variation, therefore, is only R/x times as severe as that of the basic system voltage. Hence, if R is small enough, gap 1 will not break down, and the resistor remains connected into the circuit until the next time the current reaches zero value.

Figure 20 shows what takes place when the resistor R is introduced into the circuit as previously described. Curve 1 shows the system electromotive force, curve 2 is the quasi-stationary short-circuit current and curve 3 indicates the quasi-stationary condition with the resistor R in the circuit. At the point 4 we introduce the resistor, and through a transient condition we shall accomplish the change from curve 2 to curve 3. The difference between curves 2 and 3 at point 4 gives the amplitude of the transient current at that moment, and we have already found the time constant for the circuit to be

$$T=\frac{x}{(R+r)2\pi f}$$

The curve 5 indicates this transient current, and the heavy line curve gives a picture of the current actually flowing in the circuit during the switching period.

It is obvious that even for a moderate value R, the transient current is negligible before the first current zero after the introduction of the resistor. We, therefore, can base our investigation on quasi-stationary considerations, and we see that the value of the system electromotive force at this second current-zero point is reduced to about x/R times the value at the first current-zero point, and we can express the voltage conditions on gap 2 (Figure 18D) as

$$\frac{de_2}{dt}=\frac{x}{R}\cdot\frac{de_0}{dt}$$

This is a considerable reduction of the stress. The resistor thus enables the breaker to interrupt successfully short-circuit currents on systems where the rate of rise of the reappearing voltage is R/x times larger than the usual limit for a breaker not equipped with a resistor and resistor gap.

We have seen, however, that we cannot increase R at will, lest the resistor gap fail to clear and thereby fail to introduce the resistor into the circuit. It is evident, then, that the resistor is of little value in raising the current limit of the breaker, but that it is extremely valuable when the problem is to interrupt systems with very high natural frequencies, such as those found at generating stations and large substations.

Figure 17. Simple break

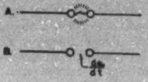


Figure 18. Double break with resistor shunted over one gap

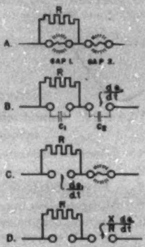
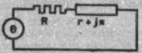


Figure 19. Equivalent circuit for conditions in Figure 18C



The method by which the resistor is introduced into the circuit by the air-blast breaker described in the accompanying paper is superficially somewhat different from what we have considered so far.

Figure 21 is a schematic diagram of the setup. Figure 21A shows the arc being drawn between the contacts A and C. The air stream blows the arc up against the contact B (Figure 21B), and after the first current zero only that part of the arc which lies between electrodes B and C will reignite, following the already ionized path as previously described (Figure 21C). The gap between the electrodes A and C, however, is physically small and is subject to a voltage rise of full severity, but since no arc existed directly between these two electrodes immediately prior to current zero, it is not very difficult to maintain at this place a dielectric of sufficient strength to withstand the maximum stresses, and since this

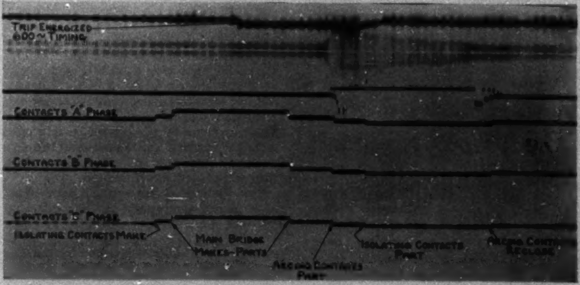


Figure 16. Timing oscillogram showing contact operating sequence of the 2,000-ampere breaker on close–open operation

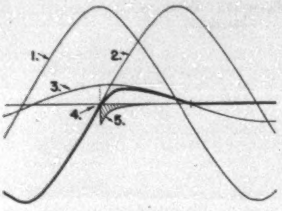


Figure 20. Current relations when resistor is introduced

1. System electromotive force
2. Short-circuit current, uninterrupted
3. Current according to circuit in Figure 19
4. Moment when the resistor is introduced
5. Transient current created by the introduction of the resistor

For clarity diagram is drawn under assumptions: $r=0$, $x=1$, $R=5$; in actual performance R is in the order of 20–30 times minimum short-circuit reactance at rated voltage

dielectric strength is already built up to full value before the current reaches its zero point, the time relation de_0/dt is without any significance whatsoever. The setup, therefore, is equivalent to that previously described with two separate breaks, and the advantages gained are the same.

Because of the light weight of the contacts, their very short travel, and the lightness of the air dielectric, the axial air-blast breaker is mechanically very quick in action. Therefore, the conditions necessary for the successful insertion of the resistor into the circuit will be present at the first current zero. The residual current through the resistor is so small that fault interruption can be considered as accomplished as soon as the resistor is introduced, that is one-half cycle or less after contact separation. The only reignition, therefore, is the one that initiates the residual current through the resistor, and the resistor is in series with the arc. Thus, this great danger of switching surges, which usually accompanies all multiple half-cycle breakers, is entirely eliminated.

It can be seen from Figure 20 that the flow of residual current through the resistor ceases at a moment very close to the voltage zero passage. This is sometimes referred to by saying that the breaker corrects the power factor before final interruption. Strictly speaking, the term power factor can be defined only for quasi-stationary conditions and has little or no meaning for transient phenomena. We have seen, however, that the transient which brings us from one quasi-stationary condition to another is practically completed before final interruption introduces a new transient. It may be justifiable, then, to speak of the power factor (meaning phase angle) of this intermediate hypothetical quasi-stationary current, although this current has never been allowed to develop.

The axial air-blast interrupter described in the main paper is peculiarly well suited to the use of resistors, automatically inserted during the interrupting process, for the purpose of controlling the amplitude and rate of rise of the recovery voltage.

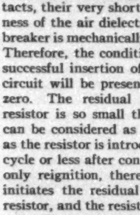


Figure 21. Arcing sequence showing automatic insertion of resistor during interruption in an axial air-blast breaker

Appendix C. Early Development of the Air-Blast Circuit Breaker

ERWIN SALZER
NONMEMBER AIEE

In order better to evaluate the progress made to date in the design of air-blast circuit breakers, a brief historical survey of their origin and early development is of value.

It has been the general belief that air-blast circuit breakers originated in Europe, but, in fact, a number of patents seeking to deal with the problem of arc extinction by the use of cross blasts of compressed air had been granted to American engineers prior to 1895, and there is evidence tending to show that such means actually were used at that time for extinction of arcs.

Similarly, pneumatically actuated circuit breakers were developed and installed in the United States during the last decade of the past century. About that time Walter Rathenau, a German engineer, philosopher, and statesman (later to become German foreign minister), came to this country to study the progress made in electrical engineering. Upon his return, he reported upon the new pneumatically operated American circuit breakers. That report induced the designers of the circuit breakers for the new Oberspree power plant in Germany to change their original designs by adding pneumatic actuating means and a pneumatic blowout feature for the purpose of extinguishing the arc drawn upon separation of the contacts. These circuit breakers, installed at Oberspree in 1897, seem to have been the first air-blast circuit breakers which were in actual service for an appreciable period of time.

Figure 22 is an illustration of the switching arrangement in the Oberspree power plant. The circuit breakers, which were of the cross-blast type, were provided with a nozzle for producing a blast of compressed air across the contacts. This arrangement did not operate satisfactorily. The arc was elongated to such an extent that it reached adjacent metal parts, thereby causing short circuits. For this reason the circuit breakers were removed, and the principle on which they were based was discarded.

The problem of arc extinction by blasts of prestored compressed gas was taken up again in the United States by R. H. Read about 1900. Figure 23 shows one of the Read designs, disclosed in United States patent 716,475, issued December 23, 1902. Read went ways of his own, and his designs were fundamentally different from earlier structures.

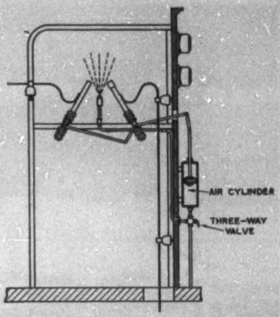


Figure 22. Early cross air-blast circuit breaker as installed at Oberspree, Germany, in 1897

An important feature which appears, among others, in the designs of Read is that particular arrangement which has become known as the axial-blast type. This term has been coined to designate circuit breakers wherein arc extinction is effected by means of blasts of gas flowing in the direction of the common axis of two coaxially arranged co-operating contacts, one being annular and the other plug-shaped. The axial-blast type of circuit breaker also has been termed sometimes the European (radial or nozzle) type. This probably may be attributed to the fact that European engineers have shown particular appreciation for the merits inherent in that essentially American type

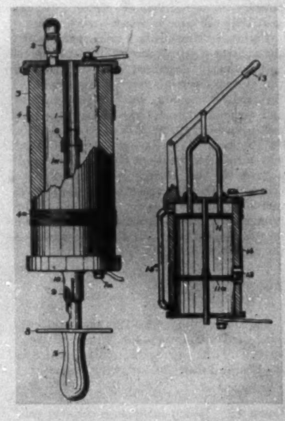


Figure 23. Early axial air-blast circuit breaker patented by R. H. Read in 1902

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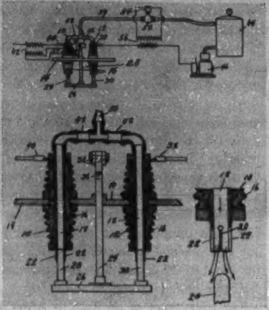

Figure 24. Double-break axial-air-blast circuit-breaker arrangement developed by T. T. Greenwood in 1926

of breaker. M. Puppikofer, Swiss designer of electric switchgear, acknowledging that the axial-blast type which has been adopted so widely in Europe was but a reintroduction of the old Read structure, wrote: "In 1901, Robert H. Read, in Schenectady, proposed a design of breaker which already embodied all the features of the present-day air-blast breaker."

At approximately the same time that Read pioneered the development of air-blast circuit breakers, the first oil circuit breakers made their appearance and dominated the field of power circuit interruption for many years to follow. The absolute domination of oil circuit breakers greatly retarded further progress in air-blast circuit breakers. During the time the oil circuit breaker dominated the field of circuit interruption, the design and manufacture of air-blast circuit breakers was never entirely discontinued. However, the efforts made in the development of air-blast circuit breakers were insignificant, as compared with the efforts made in the development of oil circuit breakers. Air-blast circuit breakers were considered to be peculiar and strange devices, applicable only where high rupturing capacity was not required.

In those early days, pneumatic actuation was one among other possible modes of circuit-breaker actuation, but not a necessity, as certain unique characteristics of pneumatic actuation were not needed. Later that mode of actuation was abandoned, and weights, springs, and electromagnetic devices, such as solenoids and electric motors, were substituted for pneumatic means. Because of the close relation between actuation of circuit breakers by compressed gas and extinction of arcs by blasts of compressed gas, the abandonment of pneumatic actuation lessened the incentive to develop further gas-blast circuit breakers.

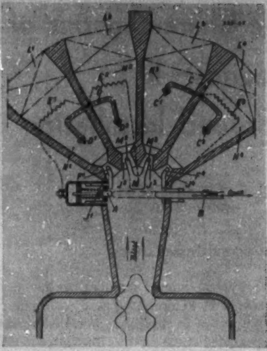

Figure 25. French cross air-blast circuit breaker patented by A. Clerc in 1937

Up to the beginning of the '20s, it was generally believed that gas-blast circuit breakers having a sufficiently high rupturing capacity for breaking power circuits could not be built, but by the middle of the '20s it was shown by different investigators, both in continental Europe and in England, that high power arcs could be extinguished within one-half cycle by powerful blasts of compressed gas. As an almost immediate result of this and related discoveries, a number of European organizations began to develop air-blast power circuit breakers. At about the same time, further development of the axial air-blast type of circuit breaker was started in the United States. Figure 24 shows a double-break design by T. T. Greenwood of Boston, developed in 1926.

The air-blast circuit breakers developed in Europe were of both the axial-blast and cross-blast types. The former was preferred in Germany and Switzerland and the latter in France. Figure 25 shows a design by A. Clerc, a French designer of circuit-interrupting devices, which was made not later than 1933. The design comprises, in combination, certain typical features, such as a cross-blast arrangement, a fishtail arc chute made up of insulating barriers, and probe electrodes for connecting resistors automatically into the circuit for control of recovery voltage.

During the last few years, the demand for oil elimination and high-speed interruption has caused reawakened interest in air-blast circuit breakers. As a result, both the axial-blast and cross-blast principles, originally conceived in this country before or about the turn of the century, have been revived; and various improved devices of each type have been successfully developed to meet the high current-interrupting requirements of American systems.

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Impulse and 60-Cycle Characteristics of Driven Grounds—III

Effect of Lead in Ground Installation

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GROUND installations comprise physically the ground proper and, in addition, a lead which connects the grounded part (protective device, insulator, overhead ground wire, lightning rod, and so forth) to the ground. The lead (grounding wire or metal structure) is essentially an inductance, while the ground acts as a resistance in the manner discussed previously.[1] These two elements determine in a large measure the total voltage across the ground installation and therefore its performance under lightning discharges.

This paper considers the effect of the lead inductance in combination with the ground proper and sets forth the practical importance of the lead or tower-structure drop, particularly for the rapid current discharges associated with direct strokes. Calculations are made for typical installations of down-leads and tower structures combined with the more common grounds such as deep and shallow driven grounds, parallel grounds, and counterpoise grounds. The circuit elements (inductance, resistance, and capacitance) of both leads and grounds are determined, and their significance is discussed. Comparative data are presented which show the impulse voltage developed in the ground installations for currents of given amplitudes and wave form representative of lightning. The other influencing factors, terminal conditions in the earth and stroke, are surveyed.

The voltages developed are discussed in terms of the impulse characteristics of insulation. In this manner, the problem is assessed in relation to the protection and co-ordination of line and station insulation, as well as to other applications of protection.

Ground Installations

Typical ground installations are presented in Figure 1. The more common consist of a lead or metal structure grounded at the earth as illustrated in (a). In the protection of electric lines, buildings, storage tanks, and so forth, the protective system usually provides several paths to ground for the lightning discharge as shown in (b), (c), (f), and (g). The protection of station apparatus requires, in addition, consideration of the arrester characteristics and the equivalent circuit of the apparatus,[2] represented schematically in (d) and (e).

In general, ground installations are essentially a combination of leads and grounds, and for analysis even the more complex installations can often be reduced to simpler forms such as (a). For these reasons ground installation (a) is analyzed quite completely here. In the calculation ten representative arrangements of installation (a) have been selected and are tabulated in Figure 2. These consist of a lead or tower in combination with the common types of ground such as single driven rods, rods or similar grounds in parallel, and counterpoise grounds buried in the earth usually not more than two feet deep.

A necessary step in the analysis is to consider the factors which determine the impulse characteristics of the lead and of the ground proper and their relative importance. The circuit elements involved are shown in (a) and (c) of Figure 3, which represent respectively shallow grounds (10-to 20-foot depth), and deep grounds.

In the ground proper, a factor of primary importance is the resistance to earth, referred to as the ground resistance. A second factor of importance, particularly for extensive grounds such as deep-driven and counterpoise grounds, is the inductance of the rod or conductor. A third factor is the resistance of the ground rod or conductor. Calculations, including the skin effect on impulse, show that this resistance practically is negligible. The capacitance of the rod or conductor to earth theoretically is also a factor. For instance, the capacitance (C) of a 100-foot ground rod is in the order of 1,500 micromicrofarads (dielectric constant of earth = 9). This capacitance is effectively in parallel with the ground resistance (R). For soil of low or medium resistivity, the time constant RC is in the order of 0.01 to 0.1 microsecond, indicating that capacitance is a factor of secondary importance. For grounds buried in rocks, the resistivity of which is very high, this time constant may increase to one microsecond or more, in which case the effect of the capacitance becomes considerable and relatively beneficial.

The inductance of the lead or tower is a factor of primary importance while the resistance of these can be disregarded. The capacitance to earth of a 100-foot tower is in the order of 1,000 micromicrofarads and much less for a grounding wire. Combining the capacitance and the inductance gives an effective time constant of 0.2 microsecond. For impulse currents rising to crest in one microsecond or longer the capacitance of either tower or lead is a secondary factor.

From these considerations, it is apparent that the important factors are the ground resistance, the lead or tower inductance, and the ground inductance. For the impulse conditions and ground installations considered in this paper these factors determine the impulse voltages. The development of the simplified circuits is given in Figure 3.

The ground resistance varies with the impulse current in the manner discussed in the preceding paper.[1] The inductance of single ground rods is calculated from the formula:[3]

$$L = 0.002l \ln 0.735 \frac{l}{r} \text{ microhenrys}$$

where l is rod length and r is effective rod radius (both in centimeters). In determining the inductance of parallel grounds, proper allowance is made for the mutual inductance between individual rods. As shown in the equivalent simplified circuits (b) and (e) in Figure 3, two thirds of the rod inductance is combined with the lead inductance for shallow grounds, while for the deeper and more extensive grounds the calculations are based on uniformly distributed ground resistance and inductance. Lead inductance is also calculated from the formula above. The inductance of tower structures is determined by substituting for the tower four parallel cylindrical conductors arranged in a square approximating the dimensions of the tower. The tower inductances calculated in this manner agree reason-

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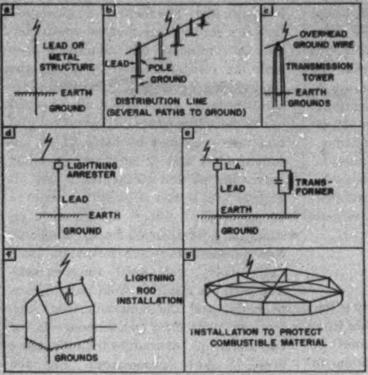

a
LEAD OR METAL STRUCTURE
EARTH
GROUND
b
LEAD
POLE GROUND
DISTRIBUTION LINE (SEVERAL PATHS TO GROUND)
c
OVERHEAD GROUND WIRE
TRANSMISSION TOWER
EARTH GROUNDS
d
LIGHTNING ARRESTER
LEAD
EARTH
GROUND
e
L.A.
LEAD
EARTH
GROUND
TRANS-FORMER
f
LIGHTNING ROD INSTALLATION
GROUNDS
g
INSTALLATION TO PROTECT COMBUSTIBLE MATERIAL

Figure 1 (left). Typical ground installations

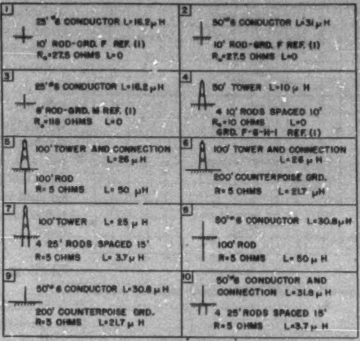

1
25' #6 CONDUCTOR L=16.2 µH
10' ROD-GRD. F REF. (1)
R_0=27.5 OHMS L=0
2
50' #6 CONDUCTOR L=31 µH
10' ROD-GRD. F REF. (1)
R_0=27.5 OHMS L=0
3
25' #6 CONDUCTOR L=16.2 µH
8' ROD-GRD. M REF. (1)
R_0=118 OHMS L=0
4
50' TOWER L=10 µH
4 10' RODS SPACED 10'
R_0=10 OHMS L=0
GRD. F-G-H-I REF. (1)
5
100' TOWER AND CONNECTION L=26 µH
100' ROD
R=5 OHMS L=50 µH
6
100' TOWER AND CONNECTION L=26 µH
200' COUNTERPOISE GRD.
R=5 OHMS L=21.7 µH
7
100' TOWER L=25 µH
4 25' RODS SPACED 15'
R=5 OHMS L=3.7 µH
8
50' #6 CONDUCTOR L=30.8 µH
100' ROD
R=5 OHMS L=50 µH
9
50' #6 CONDUCTOR L=30.8 µH
200' COUNTERPOISE GRD.
R=5 OHMS L=21.7 µH
10
50' #6 CONDUCTOR AND CONNECTION L=31.8 µH
4 25' RODS SPACED 15'
R=5 OHMS L=3.7 µH

Figure 2 (right). Combinations of lead and ground

ably well with the values reported elsewhere.[4–6]

Figure 4 presents the inductance of typical leads and towers plotted against height. In addition, the figure gives the effective inductance of leads and towers in combination with shallow grounds.

Method of Analysis

The method of analysis in this paper consists of determining the voltage developed for a current applied to the ground installation. The wave form and amplitude of the currents considered conform to present knowledge of lightning and to the practical experience available.

Field investigations have established the amplitude and frequency of occurrence of currents in lightning strokes, through tower structures, in lightning-arrester grounds, and in other circuit parts.[7–16] On transmission lines, tower currents have been recorded as high as 160,000 amperes, and about a third exceed 20,000 amperes. In distribution and station-type arresters, the maximum currents recorded do not exceed this value. As lightning-stroke discharges to electric circuits or protective systems usually reach earth through several paths or grounds, currents in single grounds seldom would exceed 50,000 amperes. Therefore, currents of 10,000, 20,000, and 40,000 amperes are used as typical values in this analysis.

The wave form of lightning currents is not so well established as the amplitude, but investigations in recent years have contributed considerably to present knowledge.[17–25] In general, the current rises to crest in a time from a fraction to several microseconds and recedes to half value in 40 to 50 microseconds. The highest rate of rise of current in the stroke occurs on the front and seldom, if ever, exceeds 50,000 amperes per microsecond. The average is about 10,000 amperes per microsecond. Because of the division of current, the rate of rise in single grounds probably does not exceed 20,000 amperes per microsecond. A front rising to crest in one microsecond can be considered a steep front, and in two to four microseconds an average front. The fronts of the currents selected are accordingly 1, 2, 4, and 8 microseconds from start to crest, and the duration is 40 microseconds to half crest on the tail.

The wave form of lightning currents varies. The exponential wave has been considered by some investigators as fairly representative, and this choice, perhaps, has been favored by the fact that this form can be expressed by a single mathematical expression. It should be noted, however, that the exponential front rises abruptly from the very start, while some of the field records show a more gradual upturn at the beginning, developing into a rapid rise as the current builds up. In view of these considerations, waves of both sinusoidal and exponential fronts have been studied and applied (see Figures 5 and 6). In Table I are tabulated the constants for these and several other wave forms.

The sinusoidal front starts with a gradual upswing, which is followed by maximum rate of rise at half value, taper-

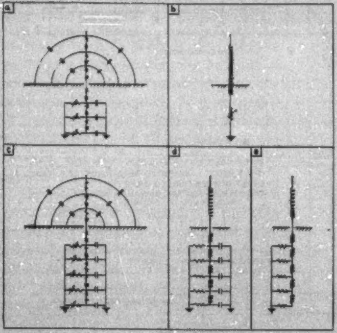


Figure 3 (left). Circuit diagrams of (a) shallow and (c) deep grounds and their equivalent forms

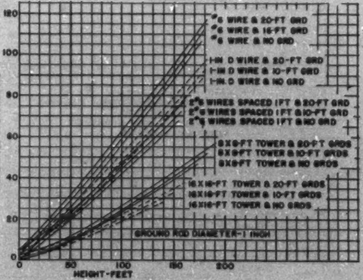


Figure 4 (right). Inductance of vertical lead or tower and ground

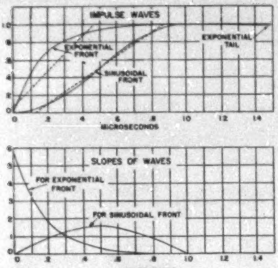


Figure 5. 1x40-microsecond impulse waves

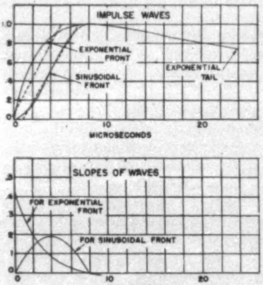


Figure 6. 8x40-microsecond impulse waves

ing off as crest is reached. The sinusoidal front approaches the average rise more closely than the exponential, and in this connection it should be noted that the field data on current rate of rise are so measured or recorded that, essentially, these give the average or effective (10–90 method) rise. Because of the fundamental and practical merits, a sinusoidal front has been used throughout the analysis, and in several instances the exponential is presented for purposes of comparison.

Shallow Grounds

In localities where soil resistivity is low it is possible to secure low resistance from a shallow driven ground, 10 or at most 20 feet deep. The first four installations in Figure 2 are representative shallow grounds. In installations 1 and 2, the ground proper is ground *F* in clay, studied in the preceding paper.[1] The high-resistance ground in installation 3 is ground *M* in sand, and the tower footing in installation 4 is the parallel ground *F-G-H-I* in clay. In installations 1 and 3, the down-lead is a 25-foot conductor; in installation 2, a 50-foot conductor; and in installation 4, a 50-foot tower.

For shallow grounds the time constant of the ground proper is sufficiently short so that the ground inductance can be combined with the lead inductance as shown in Figure 3b. The method of calculation used is a step-by-step method which consists of combining the inductance drop with the resistance drop to obtain the total voltage developed. The ground resistance varies with current as explained in the preceding paper. Typical calculations are presented in Table II for installations 1 and 3, and corresponding curves are plotted in Figures 7 and 12. Figures 7, 8, 9, and 10 present a series of curves of the voltages developed in installation 1 for a 10,000-ampere discharge corresponding to wave forms of 1x40, 2x40, 4x40, and 8x40 microseconds. The voltages resulting from both the exponential and the sinusoidal-front currents are plotted for comparison. In the two more rapid discharges the inductance drop is quite pronounced. For the slower fronts this effect is not so noticeable.

In the curves which follow we shall consider the more severe waves: the 1x40- and 2x40-microsecond waves. Figures 11 and 12 show the voltage developed by a 1x40-microsecond, 10,000-ampere discharge on installations 2 and 3. The long lead in installation 2 results in a higher inductance voltage on the front than in installation 1 (compare Figures 11 and 7). The voltage developed in installation 3 has the same inductance drop as installation 1, but, because of the higher resistance of the ground, the voltage

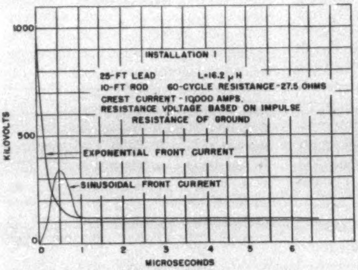
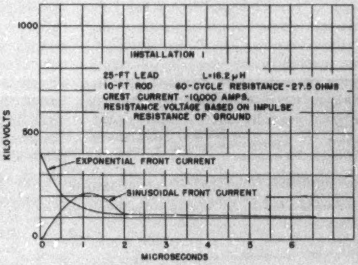


Figure 7 (left). Voltage across lead and ground

Installation 1
1x40 - microsecond wave

Figure 8 (right). Voltage across lead and ground

Installation 1
2x40 - microsecond wave



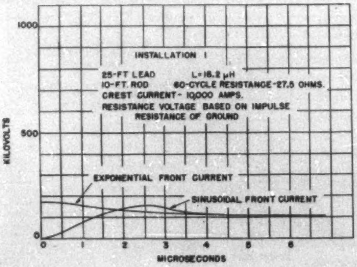
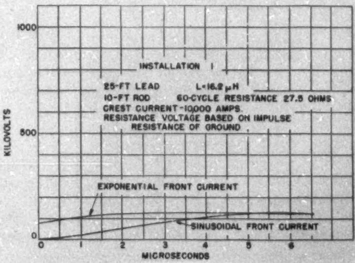


Figure 9 (left). Voltage across lead and ground

Installation 1
4x40 - microsecond wave

Figure 10 (right). Voltage across lead and ground

Installation 1
8x40 - microsecond wave



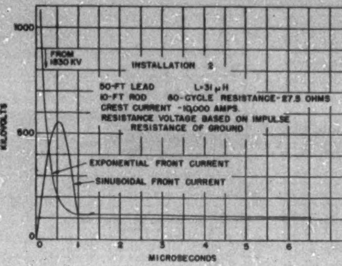


Figure 11 (left). Voltage across lead and ground

Installation 2
1x40 - microsecond wave

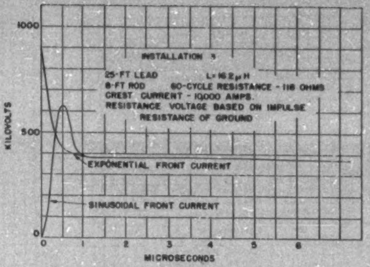


Figure 12 (right). Voltage across lead and ground

Installation 3
1x40 - microsecond wave

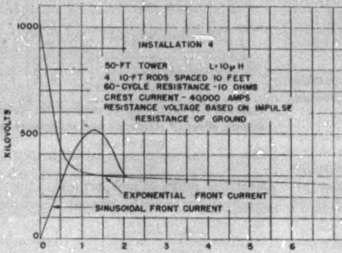


Figure 13 (left). Voltage across tower and ground

Installation 4
2x40 - microsecond wave

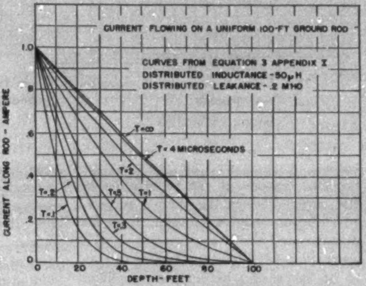


Figure 14 (right). Current distribution on 100-foot ground rod

Unit current applied

throughout the entire discharge is much higher. That is, the *ir* values in Table II for ground *M* are nearly three times as great as those for ground *F*. The voltage developed in installation 4 by a 40,000-ampere, 2x40-microsecond current is given in Figure 13. For the sinusoidal front this discharge gives an effective rate of rise of 26,000 amperes per microsecond which is considered quite severe. These typical examples demonstrate that for high-current steep-front discharges the lead effect is a factor of considerable importance.

Deep or Extensive Ground Installations

Where soil resistivity is high, it is often necessary to drive grounds deep into the earth, or to lay counterpoise grounds of considerable length in order to secure low resistance. Installations 5, 6, 8, and 9 of Figure 2 represent extensive grounds. Installations 7 and 10 are intermediate between the shallow and the extensive grounds.

For shallow grounds the lumping of the ground circuit is justified. With deep grounds or counterpoise grounds the distributed ground should be taken into account (Figure 3c). However, for practical purposes simplifications can be effected as shown in Figures 3d and 3e. In Figure 14 are plotted curves of current distribution for a 100-foot rod driven in soil of medium resistivity. The curves are calculated for unit current, based on the simplified circuit of uniformly distributed inductance and leakage resistance. It is apparent that the current does not approach substantially uniform distribution for about two microseconds.

The simplification of the equivalent circuit in Figure 3e for deep grounds and counterpoise grounds of the lengths considered here is justified for grounds in medium resistivity soils ($\rho=10{,}000$ ohm-centimeters) and for current wave fronts one microsecond and greater. Figures 15a and 16a show that the effective impedance with and without capacitance essentially are the same, and capacitance can be omitted. Figures 15b and 16b show that capacitance is an important factor for grounds in high resistivity soil ($\rho=100{,}000$ ohm-centimeters). While these curves are based for convenience on infinite length of ground, they indicate with adequate approximation the extent to which capacitance is a factor. A formal solution and calculations of the finite circuit in Figure 3d are beset with diffi-

Table I. Tabulation of Constants for Impulse-Wave Equations

Exponential Form Wave

$$i=i_{max}A\left[\epsilon^{-\alpha t}-\epsilon^{-\beta t}\right]$$

$$\frac{di}{dt}=i_{max}\left[b\epsilon^{-\beta t}-a\epsilon^{-\alpha t}\right]$$

Sinusoidal Front

$$i=\frac{i_{max}}{2}[1-\cos\omega t)]$$

$$\frac{di}{dt}=\frac{i_{max}\,\omega\sin\omega t}{2}$$

	Constants for Exponential Form Wave						Sinusoidal Front	
Wave Microseconds	A	α	β	b $(A\beta)$	a $(A\alpha)$	$\left(\frac{di}{dt}\right)_{max}$	ω	$\left(\frac{di}{dt}\right)_{max}$
½x40	1.010	0.01759	13.28	13.40	0.01777	$13.38i_{max}$	6.283	$3.142i_{max}$
1 x40	1.021	0.01788	5.80	5.92	0.01817	$5.90i_{max}$	3.142	$1.571i_{max}$
1½x40	1.032	0.01812	3.54	3.654	0.01871	$3.635i_{max}$	2.094	$1.047i_{max}$
2 x40	1.045	0.01842	2.47	2.581	0.01927	$2.562i_{max}$	1.571	$0.7855i_{max}$
4 x40	1.104	0.0198	1.001	1.105	0.02185	$1.083i_{max}$	0.7855	$0.3928i_{max}$
8 x40	1.296	0.0238	0.3655	0.473	0.03083	$0.442i_{max}$	0.3928	$0.1964i_{max}$
½x20	1.021	0.03576	11.60	11.84	0.03634	$11.80i_{max}$	6.283	$3.142i_{max}$
1 x20	1.045	0.03684	4.94	5.162	0.03854	$5.123i_{max}$	3.142	$1.571i_{max}$
½x10	1.045	0.0737	9.88	10.32	0.0771	$10.24i_{max}$	6.283	$3.142i_{max}$
1 x10	1.104	0.0792	4.004	4.42	0.0874	$4.33i_{max}$	3.142	$1.571i_{max}$
½x 5	1.104	0.1584	8.01	8.84	0.1748	$8.67i_{max}$	6.283	$3.142i_{max}$
1 x 5	1.296	0.1904	2.924	3.784	0.2466	$3.537i_{max}$	3.142	$1.571i_{max}$

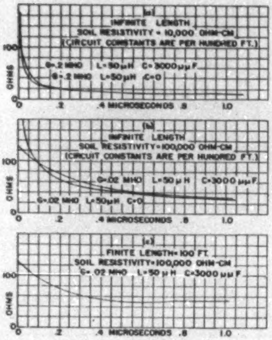


Figure 15. Surge impedance of extensive grounds

Unit current applied

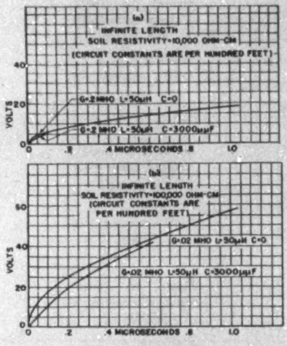


Figure 16. Voltage developed on extensive grounds for uniformly rising current (one ampere per microsecond)

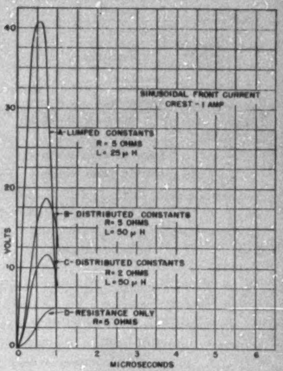


Figure 17. Illustration of method for determining effect of resistance and inductance on front of wave

culties, particularly for counterpoise grounds, because of the penetration of the current below the earth's surface, which brings about a variation of the inductance and also of the capacitance to a lesser extent.[26,27] Where capacitance is to be taken into account, a simplified method which has practical merits and is adequate for engineering purposes consists of determining the effective impedance of the ground,[28,29] as illustrated in Figure 15c.

The variation of leakage resistance with heavy currents is another factor which presents difficulties, and the complete theoretical analysis becomes too involved. A practical simplification consists of considering the distribution of the current in the ground, as illustrated in Figure 14, and assigning a resistance value accounting for the lowering of the resistance during the penetration of the current. Upon complete penetration of the current in the ground the total leakage resistance becomes effective, its value depending on the current as previously discussed.[1] To illustrate the effect produced by the variation of the ground resistance, curves *B* and *C* in Figure 17 have been plotted. The curves are compared for a one-ampere current rising to crest in one microsecond. Curve *B* is the voltage across the 100-foot five-ohm ground developed by a medium or low-current surge. A current attaining the unusual value of 100,000 amperes would lower the resistance to 40 per cent of the value in curve *B* and would result in curve *C*, which again is the voltage per-unit current. It is apparent that the lowering of the resistance of grounds with current is beneficial on the front, but not to the same extent as it is on the crest and tail of the current where the inductance effect is not present. For a moderate or average current discharge, the voltage developed evidently will lie between the two curves, close to curve *B*. In comparing deep grounds and counterpoise grounds on the front of wave, the 60-cycle resistance is used, for it gives adequately close results and is conservative. The purpose of curve *A* is to demonstrate that a deep ground driven in homogeneous soil cannot be represented arbitrarily by a simple inductance (such as one-half the total inductance), in series with the leakage resistance. It also shows that for this particular ground about a fourth of the total inductance is effective for a one-microsecond front. Curves *B* and *D* illustrate that the inductance contributes largely to the voltage developed across a deep ground when the

Table II. Typical Calculations

Current Impulse Applied			For Curves of Figure 7					For Curves of Figure 12				
			25-Foot Down-Lead Inductance = 16.2 Microhenrys Ground F—10-Foot Rod in Clay 60-Cycle Resistance = 27.5 Ohms Crest Current = 10,000 Amperes 1x40-Microsecond Wave					25-Foot Down-Lead Inductance = 16.2 Microhenrys Ground M—8-Foot Rod in Sand 60-Cycle Resistance = 118 Ohms Crest Current = 10,000 Amperes 1x40-Microsecond Wave				
Form	t Microseconds	i Kiloamperes	r Ohms	ir Kv	$\frac{di}{dt}$ Kiloamperes Per Microsecond	$L\frac{di}{dt}$ Kv	e Kv	r Ohms	ir Kv	$\frac{di}{dt}$ Kiloamperes Per Microsecond	$L\frac{di}{dt}$ Kv	e Kv
Exponential	0	0	27.5	0	59	960	960	118.	0	59	960	960
	0.1	4.5	15.4	69	33	538	607	46.0	207	33	538	745
	0.2	7.0	13.6	95	18	293	388	37.1	260	18	293	553
	0.5	9.5	12.0	114	3	49	163	28.4	270	3	49	319
	1.0	10.0	11.7	117	0	0	117	27.4	274	0	0	274
	3.0	9.4	12.0	113	0	0	113	28.6	269	0	0	269
	7.0	9.0	12.3	111	0	0	111	29.7	267	0	0	267
Sine	0	0	27.5	0	0	0	0	118.	0	0	0	0
	0.2	1.0	22.0	22	5	82	104	82.	82	5	82	164
	0.4	3.4	17.3	59	15	242	301	55.	187	15	242	429
	0.6	6.6	13.8	91	15	242	333	37.2	246	15	242	488
	0.8	9.0	12.1	109	5	82	191	29.7	267	5	82	349
	1.0	10.0	11.6	116	0	0	116	27.4	274	0	0	274
	t>1.0	same as exponential form wave										

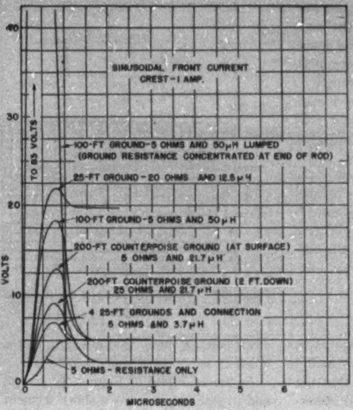


Figure 18 (left). Voltage characteristics of various grounds

1x40 - microsecond current wave

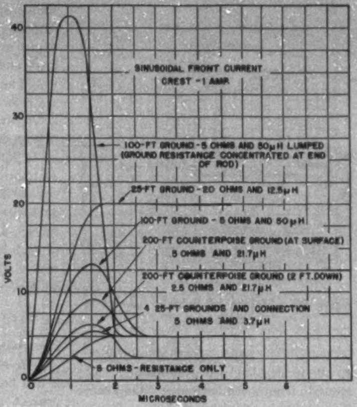


Figure 19 (right). Voltage characteristics of various grounds

2x40 - microsecond current wave

steep front of a current wave is being discharged.

In Figures 18 and 19 are compared the voltage characteristics of the ground proper for five grounds in uniform soil of medium resistivity ($\rho = 10{,}000$ ohm-centimeters). The calculations are based on distributed constants. A sixth ground represents a 100-foot rod driven in a very high-resistivity soil until it reaches a conducting layer 100 feet below the surface. The curve for a five-ohm pure resistance is plotted for reference. The parallel ground (four 25-foot rods), the 200-foot counterpoise at the surface (connection at center), the 100-foot rod in uniform soil, and the 100-foot rod reaching a low-resistivity layer 100 feet in the earth, all have five-ohm resistance and can be compared directly for their relative inductive effect on the front. The parallel ground (four 25-foot rods) gives the lowest voltage, for it has the lowest inductance. The 100-foot rod tapping low resistance deep down would naturally be the worst, as it entails the greatest inductance. The 100-foot rod and the 200-foot counterpoise (connection at center) in uniform soil are intermediate. The advantage of driving a 25-foot single rod deeper into the ground is apparent. Insofar as limiting the voltages on the front for currents rising to crest in less than two microseconds, there is little additional gain in driving a ground more than 100 feet or in extending a counterpoise much beyond this amount. However, a greater length in either case is effective for slower fronts and for reducing the voltage beyond the first few microseconds.

The difference between the characteristics of the six grounds is not so large when the grounds are combined in the complete installation with a lead or a tower. The curves of Figure 20 give the voltage for a 2x40-microsecond 40,000-ampere discharge to the tower and ground installations 5, 6, and 7 (Figure 2) and for installation 5 with the resistance concentrated at the lower end of a 100-foot rod. Figure 21 presents curves for similar lead and ground installations. While the installation with lumped resistance at the end of the 100-foot ground gives a high voltage, the difference between the other three is relatively small.

It is not the purpose here to attempt to draw up specific recommendations as to which type or arrangement of ground to recommend. The factors that determine the type of installation in practice are many. To cite a few: these involve the degree of protection, the nature of the terrain, the question of right of way, the cost of the installation, and other considerations, each of which requires evaluating on its respective merits. The primary

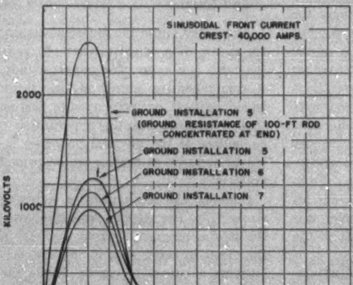


Figure 20 (left). Voltages across tower and ground for installations 5, 6, and 7

2x40 - microsecond current wave

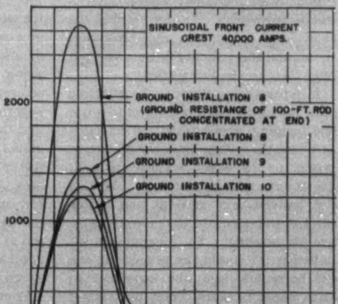


Figure 21 (right). Voltages across lead and ground for installations 8, 9, and 10

2x40 - microsecond current wave

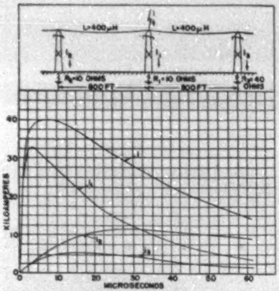


Figure 22. Division of lightning current in multiple paths

purpose of this paper is rather to develop an understanding of the problem of grounds from a fundamental standpoint. Figures 20 and 21 show that the voltages developed from steep-front high currents in extensive grounds are substantially greater than where conditions permit shallow grounds. Therefore, in laying out grounds for protection against lightning, the inductance of the installation and its relation to the effective resistance are factors of importance.

Multiple Paths to Earth

For practical calculations most installations with two or more paths to earth can be reduced to one of a few simplified forms. For instance, in installation (f) of Figure 1, the current of the stroke at the middle air terminal will divide equally between the four down-leads, provided the grounds are alike. The same holds for a stroke to the center part of installation (g). In these cases of symmetry and also in many others, where departure from symmetry is not too great, simplification to an equivalent simple *L-R* circuit is permissible. In determining the equivalent inductance and resistance, the mutual effects should be examined but often these can be disregarded.

A protective system to be effective must shield the protected object (electric line, building, storage tank, and so forth), and limit the voltage from the protective system to the object. These are well-known principles in protection. To this end low-resistance grounds and a low effective inductance of the protective system are essential. To illustrate: in installation (f), should the grounds at one end be unduly high, the impedance to earth would be nearly twice the original amount and the voltage practically doubled. Again, consider the arrangement in (f) applied to a building of the same height and side dimensions, but considerably longer. To maintain the same degree of protection, the number of down-leads and grounds as well as air terminals, would have to be increased accordingly.

Protective systems of an extensive character often comprise several paths each of increasingly greater time constant, that is, $L_1/R_1 \ll L_2/R_2 \ll L_3/R_3$, and so forth. As the duration of the high current of the stroke is limited, the effective parallel paths usually can be reduced to two or three. Let the operational admittance of each be denoted as $Y(p)$ and the current of the stroke $i(t)$. The voltage developed is

$$e(t) = [Y_1(p) + Y_2(p) + Y_3(p)]i(t)$$

The complete solution of this expression is more elaborate than difficult. Usually, it can be simplified by the fact that the nearest path to ground (L_1/R_1) determines largely the voltage across the installation for the first few microseconds, and thereafter this part of the circuit in effect is simply the resistance R_1.

A typical example is installation (b) of Figure 1, considered in the numerical example of Figure 22. A stroke occurs at tower 1 which has an inductance of 10 microhenrys and a ten-ohm footing resistance. The other two paths to ground each are over an 800-foot span of wire (400 microhenrys) to adjacent towers 2 and 3, having footing resistances of 10 and 40 ohms. The current in tower 1 is i_1, and the currents i_2 and i_3 in the adjacent towers build up as determined by their respective time constants, L_2/R_2 and L_3/R_3.

The division of currents shows that the inductance and resistance of tower 1 practically establish the voltage developed for the first two or three microseconds in the same manner as in Figure 13. The drain on the stroke current from the adjacent towers becomes pronounced in a few microseconds and reduces the duration of the current in tower 1. For slower fronts the beneficial effect of the adjacent towers is increased. Another important observation in Figure 22 is that the crest currents in the three towers cannot be added directly to determine the crest current of the stroke, as has been the practice in analyzing magnetic-link measurements of currents. From the crest measurements at the tower hit and at the adjacent towers, however, it is possible to estimate the duration of the stroke current.[30]

From the method of lumped inductance, we find from Figure 23 that strokes at mid-span of 40 kiloamperes per microsecond average rate-of-rise, develop a crest voltage close to $(40)(\pi/2)(100) = 6{,}300$ kv.

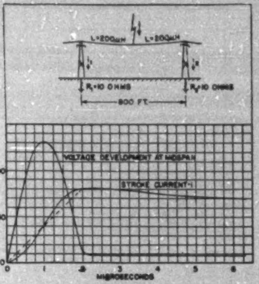


Figure 23. Voltage developed by stroke to mid-span

In the examples of Figures 22 and 23 and in similar problems a short segment of line is represented by its lumped inductance. The justification of this simplification is illustrated in Figure 24, where currents in the 800-foot line segment terminating in ten ohms are compared on the basis of both the lumped-inductance method and the traveling-wave theory. In the presence of very high voltage corona becomes a factor which reduces both the surge impedance and the velocity of propagation and increases the attenuation. The surge impedance and the velocity of propagation are reduced proportionally, giving practically the same results as in Figure 24. In these problems attenuation is not a primary factor, as the energy supply is considerably greater than the drain effected from this cause. It should not be construed from the above example, however, that the same simplification applies invariably to other problems which fundamentally require the application of traveling wave methods.

Terminal Conditions in Stroke and Earth

As pointed out in the preceding paper,[1] the distribution of the electric charge in the earth is a factor that requires study. A comprehensive survey of this factor would, in itself, comprise a paper, and we shall of necessity limit this presentation to an outline of the physical processes involved and to their bearing on the ground problem.

Consider the mechanism of lightning stroke.[21,17] The stepped leader initiates the stroke, progressing from the base of the cloud to earth and, in this manner, lowering a charge to earth. When the leader contacts earth, the high current of the discharge (main stroke) blazes its way up the channel. For the purpose of this

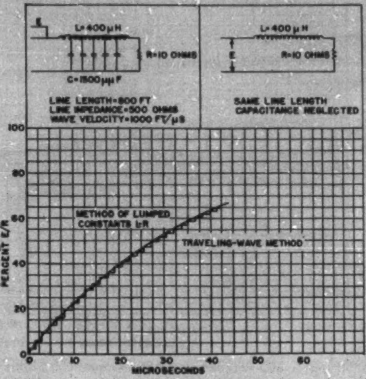
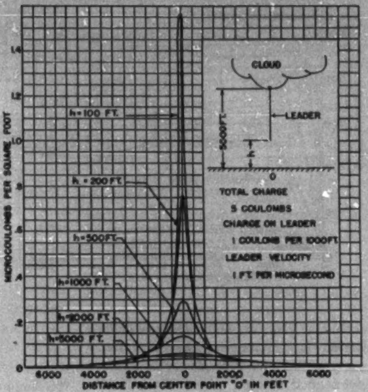


Figure 24 (left). Comparison of traveling-wave and lumped-constant methods

Figure 25 (right). Charge density on earth's surface preceding lightning discharge



analysis the charge in the leader progresses uniformly to earth as shown schematically in Figure 25. The distribution curves of the induced-charge density in the earth for various positions of the leader are plotted in the figure. Representative values of total charge, cloud height, and leader velocity are assigned, and the surface at the earth is assumed to be flat and the soil of high or medium conductivity.

In Figure 26 are plotted curves of the charge per foot of radial distance from the center point "O" on the earth. A second family of curves gives the corresponding current flow in the earth. As the leader progresses, the charge moves up closer and closer to point "O" on the earth. As it approaches the earth, its velocity is increased, apparently gapping the last hundred feet in a few microseconds. It will be noted that the crest current approaches more and more the average current of the leader. Of even greater significance is the distribution of the charge as the leader reaches ground. When the leader contacts earth, the rush of the charge from earth into the leader channel determines the high-current component of the discharge. At this point, it is well to examine the significance of charge distribution in the earth. From the curves in Figure 26 as the leader contacts earth, consider that the charge is converted into the high-current discharge. Fundamental considerations show that free current flow in the earth attains a velocity in the order of one-third the velocity of light. On this basis curves for $h=100$ and 200 feet in Figure 26 are plotted in Figure 27 as current waves. In these curves we recognize current waves of form and magnitude of lightning discharges.

Actually, the conversion of the equal and opposite charges on leader and earth into the current discharge is a more complex process than we have indicated, for the two charges are not symmetrically distributed with respect to the ground (point "O"), nor are their mediums nor mode of propagation physically matched. For instance, in the example the two opposite charges in the first 1,000 feet up the leader and on the earth are respectively 1 and 0.7 coulombs. However, the charge on the leader 2,500 feet up has its counterpart on the earth 5,000 feet from the ground point "O," and the ratio of the two per-unit length is 1 to 0.3. The dissymmetry increases with the distance. Thus, attenuation considered, the current wave progresses up the channel with a decreasing velocity. This deduction on the current propagation in the stroke is confirmed by the experimental evidence[31,33] which shows that the main stroke moves up the leader channel initially at about one-third the velocity of light, but, as the wave progresses higher up the channel, the velocity tapers off, and it may dwindle to one-tenth the velocity of light or less before reaching the base of the cloud.

Other influencing factors are present. The state of ionization as well as the presence of branches associated with the leader no doubt have some effect on the current discharge. The charge lowered by the leader varies with the intensity of the stroke and governs the current. In multiple strokes the leader of the second and subsequent discharges progresses more rapidly than in the first discharge. However, the charge distributions are of the form given in Figure 26, except possibly for very high-resistivity soils. The topography, geology, and resistivity of the earth are also factors. Obviously, a complete survey of the variations of the current and wave form in lightning discharges requires consideration of all these factors. Figures 26 and 27 set forth the fundamental process and relationship as the charge in the earth is converted into the lightning-stroke discharge.

Next consider the terminal conditions in the earth in relation to the ground problem. As curves of Figures 26 and 27 show, the charge in the first few hundred feet determines the front of the current discharge. The ground should be of a type and arrangement to minimize the combined inductance and resistance drops in this initial stage. After the current has reached crest, the charge flows in from greater distances, and the ground then performs essentially as a resistance. In a low- or medium-resistivity soil the common driven ground provides low impedance during the entire current discharge. A counterpoise ground fanning out in multiple paths also is effective, although it does not appear to be the economical type for low-resistivity soils. In soils of high resistivity, a counterpoise ground is more effective for it "picks up" the charge on the surface. To limit effectively the voltage developed on the front, the counterpoise need not extend more than a few hundred feet, but, where low resistance throughout the entire discharge is required, the advantages of a continuous counterpoise are apparent. The effectiveness of one type of ground in preference to another depends also on the structure of the earth's surface, geology, and so forth. For instance, one type of structure consists of a layer of some 20 to 50 feet of sand, gravel, or other high-resistivity soil, followed under-

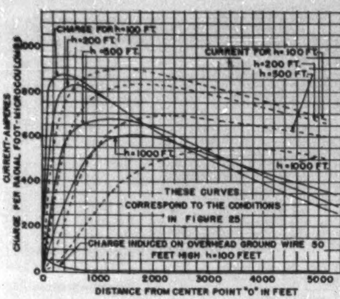


Figure 26 (left). Charge and current distribution preceding lightning discharge

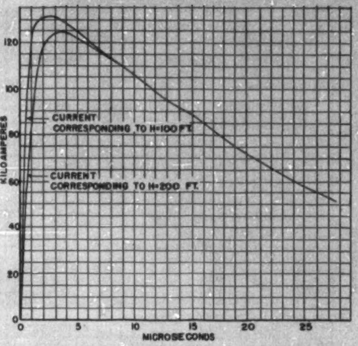


Figure 27 (right). Lightning current waves from charge distribution (Figure 26)

neath by clay or other low-resistivity strata. In this case, deep-driven grounds are practical and economical, and have given good results. Here the surface charge flows into the low-resistivity strata and then to the driven ground; thus for this condition, the deep ground provides a low-impedance path. Should the low-resistivity strata lie too deep, the condition approaches in effect a high-resistivity soil for which a counterpoise ground has advantages. These examples illustrate the many aspects of the ground problem and the importance of the structure and resistivity characteristics of the earth's surface.

The charge distribution on the earth in the curves of Figure 26 is for a flat surface. A ground installation in the field of the leader assumes part of the charge. For instance, an overhead ground wire 50 feet high will have an induced charge as shown in the bottom curve of Figure 26, and the charge on the surface of the earth is reduced by this amount. In like manner, the presence of the down-lead or tower reduces the charge on the earth near the installation. It should be noted also that, as the leader approaches earth, a streamer formation from the installation results in a current flow which will drain part of the charge from the installation and, in turn, from the earth nearby. These factors cause the current which flows into the ground installation to rise more slowly on the front than is indicated in Figure 27, and, in effect, the inductance drop is somewhat reduced.

A number of field observations become more understandable when examined in the light of terminal conditions in the earth. For instance, long-duration currents seem to prevail in regions of high-resistivity earth. Consider an installation of limited extent in very high-resistivity soil such as granite rock (ρ=1,000,000 ohm-centimeters). The effective resistance in the earth, through which the bulk of the charge must flow during the discharge, is in the order of 200 ohms. If it were possible for the charge to flow in as short a time (T=50 microseconds) as in low-resistivity earth, the voltage developed in the earth would be in the order of 20,000,000 volts (QR/T=5x200/50). Estimates of the potential of the lightning stroke vary, but figures assigned in recent years average well below 100,000,000 volts. All this means that the flow of the charge in high-resistivity earth is of necessity prolonged into a long-duration discharge.

Terminal conditions in the stroke proper also affect the voltage developed in the ground installation. That is, the heavy current flow up the leader channel has a mutual inductive effect on the down-lead or structure. The voltage induced in the installation is the product of the mutual inductance and the rate of change of current in the channel. This voltage is not more than about 20 per cent of the total inductive drop in the lead proper. For practical purposes the inductance of the lead may be increased by this amount, as the examples of Figure 28 indicate.

Judged in the light of the terminal conditions and their effect, the simple method of analysis for determining the voltage across the ground installation, in which a current is applied to an equivalent circuit, is reasonably correct and practically satisfactory. The justification of a gradual rise to crest as represented by a sinusoidal front is also borne out. However, the terminal conditions in the earth are of such character and can vary to such an extent with different regions and localities that a survey of these conditions in the more important installations of grounds should supplement the simple method of analysis. It is clear, too, that to advance further the art of grounding on a quantitative basis more fundamental and detailed study of terminal conditions is both desirable and necessary.

The Insulation Problem

The insulation problem consists in proportioning the insulation of the installation to withstand the voltages appearing across the various parts. The amount of insulation may be governed by economics, as for transformer and similar substation apparatus, in which case protective devices are applied and co-ordinated with the insulation to limit the voltages to permissible values.

Present impulse data for air, oil, wood, and so forth, and for the apparatus proper, are based largely on the volt-time curves from the $1^1/_2$x40-microsecond wave, extending from full wave to breakdown on the front in a fraction of a microsecond.[7,32–39] Curve *a* and curves *b* and *c* in Figure 29 illustrate the volt-time characteristics for two of the more common types of insulation: oil-insulated apparatus (transformers, and so forth), and air (rod gaps, bushings, and so forth). These and the corresponding data for other types of insulation permit determining to a fairly satisfactory degree the insulation requirements in most practical applications.

The volt-time method for determining and applying insulation data has well-known practical and theoretical advantages. It has however certain limitations which have been partly recognized. In ground-installation problems and other protection problems, a general form of the wave encountered is shown in curve *d* of Figure 29. This is more complex than either the $1^1/_2$x40 wave or the chopped impulses which the volt-time characteristic of insulation for the $1^1/_2$x40 wave represent. Because of a lack of data on this wave form, some tests on the strength of

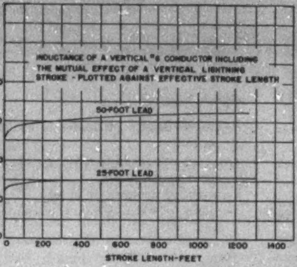


Figure 28. Mutual inductive effect of stroke on lead

air were obtained and are reported in curve *b* of Figure 30. The following illustrates the usefulness of such data in assessing the insulation requirements in problems of ground installations and protection.

Where the ground-resistance drop or the arrester voltage alone is prevalent, the air-insulation requirements are estimated satisfactorily from curves *d* and *e*. All considered, a minimum air clearance of about one foot per 150 kv will suffice. Should the clearance be from a downlead to a separate object connecting to the same ground, 300 kv per foot is permissible as indicated in curve *a*. For instance, the voltage across a 50-foot lead discharging a current rising at the unusual rate of 40,000 amperes per microsecond would not exceed 1,250 kv, and a four- or even a three-foot clearance to a separate lead connecting to the same driven ground would be suitable. Consider now the case in which the combination of the two components is effective. In curve *b* the wave of the tests made is based on a ratio of the two voltages, $V_1/V_2=2$, having durations of 1.3 and 15 microseconds. The air clearance required to withstand this type of wave is 30 per cent greater than for either the full wave or the short impulse component alone.

From the examples of the paper (see Figures 7 to 13, 20, and 21), the lead drop or short impulse component bears a ratio to the resistance drop or long component which varies over wide limits ($V_1/V_2=$ 1.5 to 15). Obviously, more impulse data on the strength of insulation for this type of wave are desirable, but, pending the future availability of such data, the curves in Figure 30 and other available data should serve reasonably well in determining the insulation requirements in problems of ground installations and in similar problems.

Insulation designed entirely for the resistance drop of the ground may not be adequate under the more severe conditions of rapid high-current discharges. There has been field evidence to this effect.[10] The example in Figure 29 clearly shows that, while the air insulation would be satisfactory for either a full wave or a short impulse, the combination of the two in the wave form *d* will flash over the air insulation. The combined effect of this type of impulse is particularly noticeable on air insulation. The effect of this impulse on oil-immersed insulation is probably much less than on air insulation.

Summary

In laying out grounds for protection against lightning, the inductance of the installation and its relation to the effective resistance of the ground are factors of importance. The lead and ground inductance becomes particularly significant for rapid current discharges associated with direct strokes when the inductance drop may exceed the resistance drop. Examples show that the voltages developed from steep-front high currents in extensive grounds are considerably greater than where conditions permit shallow grounds.

The simple method of analysis for determining the voltage across the ground installation, in which a current is applied to an equivalent circuit, is reasonably correct and practically satisfactory. Ground installations comprising multiple paths to earth are also amenable to simplified calculations. A survey of the terminal conditions in the earth should supplement the simple method of analysis in the more important installations of grounds.

In assessing the insulation requirements, these should be considered in relation to the impulse voltages developed, which for ground installations are more complex than the ordinary standard waves.

Appendix I. Method of Solution for Extensive Grounds

While the calculation for shallow grounds is effected simply from the addition of inductance and resistance voltages, the calculations for deep grounds are more involved and require explanation.

The equation for the curves of current distribution (Figure 14) is obtained from the transmission formula[40] which, expressed operationally, is

$$i(x)=\frac{\sinh\left[\sqrt{(R+Lp)(G+Cp)}\left(\frac{x}{l}\right)\right]}{\sinh\left[\sqrt{(R+Lp)(G+Cp)}\right]}i\,1 \qquad (1)$$

where

x = distance from lower end of rod in feet
$i(x)$ = current at x in amperes
i = input current in amperes

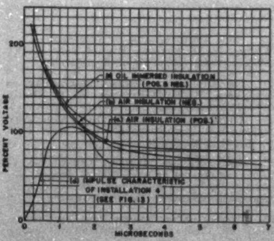


Figure 29 (left). Typical volt-time curves for oil and air insulation

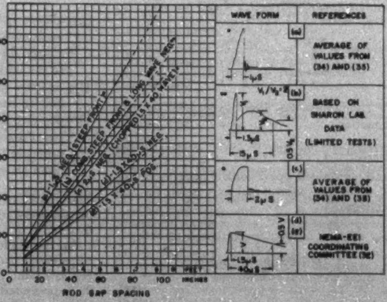


Figure 30 (right). Impulse strength of air for various wave forms

* Positive wave values are near negative wave values

** Curve for positive wave is about five per cent lower

R = total rod resistance in ohms
L = total rod inductance in henrys
G = total ground leakance in mhos
C = total capacitance in farads
l = rod length in feet
p = Heaviside's operator d/dt

Since R and C are considered negligible, the operational expression becomes

$$i(x)=\frac{\sinh\left[\sqrt{GLp}\left(\frac{x}{l}\right)\right]}{\sinh\sqrt{GLp}}\, i\, 1 \tag{2}$$

From the Heaviside expansion theorem the current at any point on the rod is

$$i(x,t)=I\left[\frac{x}{l}+\sum_{n=1}^{\infty}\frac{2(-1)^n}{n\pi}\sin\frac{n\pi x}{l}\cdot\epsilon^{-\frac{n^2\pi^2}{LG}t}\right] \tag{3}$$

The operational expression for the impulse voltage on the ground proper is

$$e=\frac{i}{\sqrt{\frac{G}{Lp}}\tanh\sqrt{GLp}} \tag{4}$$

and the solution for unit current becomes

$$e=\frac{I}{G}\left[1+2\sum_{n=1}^{\infty}\epsilon^{-\frac{n^2\pi^2}{GL}t}\right] \tag{5}$$

The response to currents of other forms than unit function can be derived by superposition or operationally. The two forms of lightning currents considered in this paper are the exponential and the sinusoidal front, in each case with exponential tail. The exponential wave form is expressed by the equation

$$i(t)=I[\epsilon^{-\alpha t}-\epsilon^{-\beta t}] \tag{6}$$

This expression in operational form and combined with equation 4 gives

$$e=I(\beta-\alpha)\times\left[\frac{1}{(p+\alpha)(p+\beta)\sqrt{\frac{G}{Lp^2}}\tanh\sqrt{GLp}}\right]1 \tag{7}$$

From the Heaviside expansion theorem, the solution for the voltage is

$$e=I\left[\frac{\sqrt{\frac{L\alpha}{G}}\epsilon^{-\alpha t}}{\tan\sqrt{GL\alpha}}-\frac{\sqrt{\frac{L\beta}{G}}\epsilon^{-\beta t}}{\tan\sqrt{GL\beta}}+\frac{\alpha-\beta}{G^2L}\sum_{n=1}^{\infty}\frac{2n^2\pi^2\epsilon^{-\frac{n^2\pi^2t}{GL}}}{\left(\alpha-\frac{n^2\pi^2}{GL}\right)\left(\beta-\frac{n^2\pi^2}{GL}\right)}\right] \tag{8}$$

The sinusoidal front expressed by the equation

$$i(t)=A(1-\cos Bt) \tag{9}$$

and, combined operationally with equation 4, gives the operational expression for the voltage

$$e=AB^2\left[\frac{1}{(p^2+B^2)\sqrt{\frac{G}{Lp}}\tanh\sqrt{GLp}}\right]1 \tag{10}$$

Solving the Heaviside expansion theorem, the voltage is

$$e=\frac{A}{G}-A\sqrt{\frac{LB}{G}}\times\frac{\cos\left[Bt+0.783-\tan^{-1}\left(\frac{\sin\sqrt{2GLB}}{\sinh\sqrt{2GLB}}\right)\right]}{\frac{\sqrt{\sinh^2\sqrt{2GLB}+\sin^2\sqrt{2GLB}}}{\cosh\sqrt{2GLB}+\cos\sqrt{2GLB}}}-\frac{2AGB^2L^2}{\pi^4}\sum_{n=1}^{\infty}\frac{\epsilon^{-\frac{n^2\pi^2}{GL}t}}{n^4+\frac{B^2G^2L^2}{\pi^4}} \tag{11}$$

This solution applies to the front of the wave only. From the crest point, the current decreases exponentially.

In the calculations for curves a and b of Figure 15, the impedance (voltage for unit current applied) of a ground of infinite length in which capacitance is considered is derived following a method of analysis in Carson's book.[41]

$$e=\sqrt{\frac{L}{C}}\,\epsilon^{-\frac{Gt}{2C}}I_0\left(\frac{Gt}{2C}\right) \tag{12}$$

where $I_0(Gt/2C)$ is the modified Bessel function of the first kind and zero order. The voltage for a current rising uniformly, as plotted in Figure 16, is obtained from the principle of superposition applied to equation 12.

Appendix II. Charge Distribution in the Earth

The curves in Figures 25 and 26 for charge distribution in the earth as the leader progresses toward earth are determined by the method of images on the basis that the earth is an equipotential surface. The field intensity at the earth's surface is calculated. From this the charge density, in coulombs per square foot, is determined:

$$\sigma=\frac{Q_1H}{2\pi(H^2+r^2)^{3/2}}+\frac{Q_2}{2\pi(H-h)}\times\left[\frac{1}{\sqrt{h^2+r^2}}-\frac{1}{\sqrt{H^2+r^2}}\right] \tag{13}$$

where

Q_1 = charge concentrated at upper end of leader in coulombs
Q_2 = charge distributed uniformly on leader in coulombs
H = distance between Q_1 and earth (cloud height) in feet
h = distance from leader tip to earth in feet (see Figure 25)
r = radius in feet from center point "O" to point at which σ is calculated

The charge per radial foot in Figure 26 is obtained by multiplying σ in Equation 13 by $2\pi r$.

As the leader progresses, the charge on the earth's surface moves in toward the center point "O." The current in Figure 26, represented by this flow of charge, is the total charge within a given radius differentiated with respect to time, that is,

$$i=qv\left[\frac{H}{\sqrt{H^2+r^2}}-\frac{h}{\sqrt{h^2+r^2}}\right] \tag{14}$$

where

q = charge density on leader in coulombs per foot
v = leader velocity in feet per second
H = cloud height in feet
h = height of streamer tip in feet
r = radius for which current is calculated in feet

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Field Disturbances Produced by Lightning

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THE present knowledge of the mechanism of natural lightning permits more accurate calculation of its electric and magnetic fields than has hitherto been possible. In a previous paper,[1] the electric ground gradients for nearby strokes to earth were treated principally for the determination of the voltages induced on transmission lines by indirect strokes. In this paper the study is extended to the fields produced at greater distances. Knowledge of the character of such fields is important because of their relationship to stroke mechanism and electrical cloud conditions in general. They also are sources of interference to radio and telephony communication and can cause sparking which may be serious in structures constituting a fire or explosion hazard.

Measurements have been made by numerous investigators of the electric gradients during both fair and disturbed weather conditions, and extensive studies have been made of the high-frequency fields produced by lightning discharges. However, the information gained has been limited by several factors, such as limitations in recording speed, field distortion with propagation, the large number of influencing characteristics of the stroke discharge, and the lack of sufficient knowledge of their exact effects. It was, therefore, felt that a critical analysis of these field disturbances would be of value at the present time.

Electric Ground Gradient During Various Weather Conditions

The relation between the fields produced during the brief period of a lightning stroke and those existing at all other times is important for a comprehensive analysis of this subject.

Fair-Weather Field

There exists at all times an electric field in the atmosphere surrounding the earth.[2] During fair weather it is always directed vertically downward, being produced by negative charges on the earth's surface and positive charges in the upper layers of the earth's atmosphere. Fields of this direction have been adopted by convention as positive. The average magnitude of the fair-weather field ranges from about 15 to 100 volts per foot in different parts of the world. Over oceans it is less variable and averages about 40 volts per foot with a daily variation from a minimum of about 25 volts per foot occurring around 4 a.m. local mean time to a maximum of 50 volts per foot at 6 or 7 p.m.[3–6] This variation progresses according to universal solar time across the oceans. Over land there is also a diurnal variation which is greater in magnitude and more variable in its nature.[6–11] These variations are due principally to fluctuations in atmospheric pollution, the earth's gradient increasing with the increase in pollution. The maximum range of this variation is approximately 15 to 200 volts per foot.

Disturbed-Weather Fields

The increase of ground gradient with atmospheric pollution is caused by the relation existing between the atmospheric gradient, conductivity, and conduction current.[2] The conduction current varies only slightly despite large variations in the other two quantities. The presence of dust particles or fine water droplets in the air decreases its conductivity by providing condensation nuclei for raising the proportion of slow ions. Decrease in the air conductivity, however, produces an increase in the gradient and little change in the conduction current. During fog the ground gradient increases to as high as 300 to 500 volts per foot.

There are also several types of atmos-

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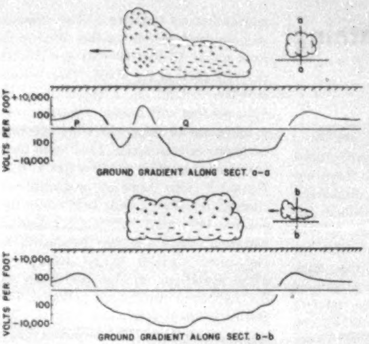


Figure 1. Typical variation of ground gradient beneath shower and thunderclouds (Simpson[20,21])

pheric disturbances producing sufficient charge separation in the air to change the earth's field materially. Under violent dust storms common to arid regions, the ground gradient usually becomes negative and may reach magnitudes as high as 3,000 to 4,000 volts per foot.[2] There is no sharp distinction between the conditions of charge separation in rain and thunderclouds or the magnitude of the ground gradients they produce, except during the brief period of a lightning stroke. Light or steady rain is usually accompanied by negative gradients. Positive charge is carried to earth by the raindrops reversing the sign of the earth's charge, and negative charge is left in the cloud base.[18] As the turbulence and violence of the storm increases to heavy shower or thunderstorms, the magnitude of the gradient increases, and a greater number of cases of positive-charge centers at the base of the cloud, resulting in positive gradient beneath, are observed. Gradients of either polarity may be as much as 5,000 to 8,000 volts per foot, with the negative usually somewhat higher.[20]

The measurements by Simpson and his associates[20,21] at Kew, England, of the electric gradients both at ground and in the cloud have provided data on the relation between the charge distribution in both thunder and shower clouds and the ground gradient. Figure 1 represents the major typical relations which he has found to exist. The top of the cloud having positive charge affects the gradient at the surface of the earth first as a cloud approaches, so that the fair-weather field is first increased positively. It then is usually reversed by negative charges near the base and front of the cloud and is predominantly negative under the base of the cloud unless a positive charge center is overhead.

Table I. Types of Lightning Discharges and the Field Changes They Produce

Type of Discharge	Sign of Field Change	
	Less Than Reversal Distance	Greater Than Reversal Distance
Q_1 to ground	Negative	Negative
Q_2 to ground	Positive	Positive
Q_3 to ground	Negative	Negative
Q_1 to Q_2	Positive	Negative
Q_2 to Q_3	Uncertain	Uncertain

Charge Centers Q are shown in Figure 3.

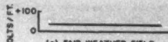
Fields From Lightning Discharges

The only definite means of distinguishing between shower and thunderstorms from the nature of the ground gradient are the rapid changes during a lightning discharge. Measurements of these field changes have been of two principal types, using the relatively slow capillary electrometer[12–17] and the cathode-ray oscillograph.[22–35] It is important to distinguish carefully between the data obtained from these two types of studies. The cathode-ray oscillograph is capable of measuring the magnitude and wave shape of the field during the period of the discharge, whereas the capillary electrometer records the field only just before a stroke occurs and the change produced after the cloud charge involved in the stroke has been neutralized. However, the time constants of pickup circuits used with oscillographs are necessarily too small to record the slow field changes during the charging process previous to a stroke. The zero axis of an oscillogram is, therefore, the field just preceding a stroke.

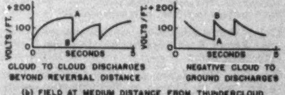
Types of Lightning Discharges

In Figure 2 are replots of typical records of ground gradient recorded by Wilson[13] with the capillary electrometer. As shown by Figures 2b and 2c the cloud charge builds up in an exponential manner and requires several seconds before reaching a substantially constant value. At the instant of a discharge the gradient is changed rapidly by the charge neutralization. This field change is the portion designated by *A-B*. Correlation of the analyses which have been made of ground-gradient data of this type in both England[13,17] and South Africa[15,16] with the balloon studies of Simpson in England and the records which have been obtained in America of the wave shape of the current in direct strokes to ground[37–39] gives considerable information on the types of discharges which can take place and their relative occurrence. As mentioned previously, Simpson's balloon soundings have shown that the type of charge distribution of Figure 1 was typical of all thunderclouds studied. There is positive charge in the upper regions of the cloud, negative charge in the main base of the cloud, and one or more smaller positive charge centers in the lower regions of the cloud. The components of the gradient produced individually by the first two charge centers have been pointed out by Wilson and are illustrated in Figure 3. The positive charge at a greater height above ground will produce a smaller ground gradient at close distances than the negative charge. However, it becomes greater after a certain distance. The effect of the third type of positive-charge distribution is also illustrated in Figure 3a. The gradient produced by it should be relatively small and should decrease rapidly with distance.

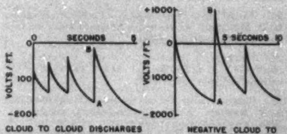
There are six conceivable types of discharges which may occur either in the



(a) FAIR WEATHER FIELD



(b) FIELD AT MEDIUM DISTANCE FROM THUNDERCLOUD. (POINT P OF FIG. 1)



(c) FIELD UNDER A NEGATIVELY CHARGED PORTION OF THUNDERCLOUD. (POINT Q OF FIG. 1)

Figure 2. Typical capillary electrometer ground gradient records obtained by Wilson[1]

form of a single-component stroke or in various combinations in a multiple stroke. The five involving discharges either between the three cloud charge centers or from any one of them to ground are listed in Table I, together with the net field change that would be recorded by the capillary electrometer. The sixth possibility is partial discharges which do not develop to earth or to other discreet charge centers in the cloud. Visual evidence is available to indicate that a glow discharge can occur infrequently from the top of the cloud into the air above.[36] In Figure 4 is reproduced a still photograph taken with about a ten-minute exposure by L. R. Abraham in Illinois which shows several discharges from the base of the cloud reaching only part way to earth.

As shown by Table I and Figure 3, strokes to ground discharging either of the two types of positive-charge center will produce a negative change in ground gradient at any stroke distance. Likewise, such a discharge from the negative charge center will always produce a positive field change. However, cloud-to-cloud discharges between charge centers 1 and 2 will, as shown in Figure 3b, produce a field change varying in sign with distance. Depending upon the amounts of charge involved and their mean heights, there will be a certain distance, designated by Wilson as the reversal distance, at which the sign of the net field produced by the two sets of charges will change from negative to positive. Thus, when the discharge occurs, the field change at distances less than this will be positive, whereas at greater distances it will be negative. The field change from a discharge between centers 2 and 3 is uncertain. It depends on which center lies at the greater height and on their horizontal separation. The change will be much smaller than for cloud to ground or the first mentioned type of cloud to cloud. At distances greater than about five or six miles it is probably relatively small.

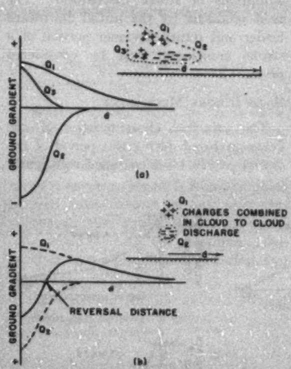


Figure 3. Ground gradients as produced by individual thundercloud charge centers

Figure 4. Reproduction of photograph of natural lightning showing cloud-to-cloud discharges at cloud base and partial discharges toward earth, taken by L. R. Abraham

Capillary electrometer records for cloud-to-cloud discharges indicate that for both South Africa[14,16] and England[15,17] the reversal distance ranges from about two to ten miles with a mean at about 4.5 miles. Of 145 records of cloud-to-cloud discharges at distances greater than six miles, Halliday[15] found 137, or 95 per cent, producing negative field changes. Schonland[14] found 130 out of 144, or 90 per cent, negative. For cloud-to-cloud discharges at distances less than four miles, Halliday records 86 out of 104, or 83 per cent, as negative, and Schonland 19 out of 19, or 100 per cent, negative. For strokes to ground Halliday has recorded 267 out of 283, or 94 per cent, which produced positive field changes. The field changes and charge moments indicated to be destroyed for both types of discharges have approximately the same magnitude. They are higher than would be expected by partial discharges into the air, which are known to occur only rarely, and, except at close distances, higher than would be produced by discharges between centers 2 and 3. They indicate that the preponderance, at least 90 per cent, of cloud-to-cloud discharges are between centers 1 and 2 and that about 90 per cent of strokes to ground lower a preponderance of negative charge to ground. However, that discharges only between centers 2 and 3 can occur is indicated by Figure 4 which shows horizontal discharges along the base of the cloud not connected to discharges to ground. This is less likely to occur between two sections of the negative-charge center than between positively and negatively charged regions unless the discharge is a multiple one to ground.

That practically all strokes to ground involve only negative cloud charge is shown by the direct-stroke current data. Thirty-three strokes to objects ranging in height from 85 to 585 feet have been recorded with the fulchronograph.[37] Twenty-nine, or 88 per cent, have shown entirely negative current flow to earth. McEachron has published data showing that 41 out of 49, or 84 per cent of strokes to the 1,250-foot Empire State Building, were of entirely negative polarity.[38,39] Of the four remaining fulchronograms, only one was entirely positive. It was a single-component stroke and is, as far as the authors are aware, the only record of a stroke that was definitely entirely positive. The remaining three fulchronograms and the five records of McEachron are of oscillatory surges. Of these eight oscillatory records, five were initiated by a negative discharge, and positive current only flowed toward the end of the stroke. However, one fulchronogram has been obtained of a discharge to a 360-foot radio tower which started initially as a positive continuing current flow of low magnitude followed by successive high-current negative components. McEachron[39] has recorded one stroke starting as a positive high-current component followed by a negative high-current component which in about five microseconds reversed to a positive low-current discharge, remaining positive for only about 30 microseconds. It seems most likely that strokes initially positive will originate from charge center 3 of Figure 3. However, Mr. D. D. Clark of

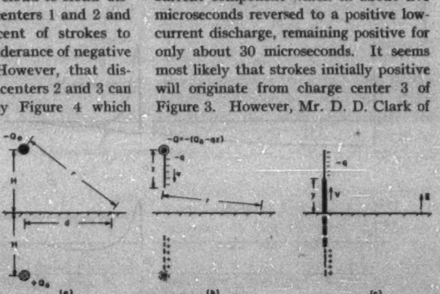


Figure 5. Basic stroke mechanism for calculating electric and magnetic fields

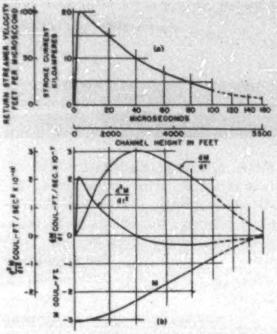


Figure 6. Charge moment and its derivatives for average stroke

Basic stroke mechanism of Figure 5. Crest current 20,000 amperes. Cloud height 5,500 feet

the Kansas City Power and Light Company reports observing discharges from the top of the cloud along its side to earth. The negative-polarity portions of the oscillatory stroke-current records involve predominantly greater amounts of charge than the positive portions. This, together with the fact that the smaller positive center 3 of Figure 3 is in the lower region of the cloud, indicates that it is most often involved in such strokes.

To summarize, the evidence available to the authors indicates that thundercloud charge conditions and the types of discharges which can take place are essentially the same in South Africa, England, and the United States. The preponderance of cloud to ground strokes, about 90 per cent, involves only the discharge of the negative charge in the cloud base. Also, about 90 per cent of cloud-to-cloud discharges are from the negative-charged region in the cloud base to the positive charge in the cloud top. There are undoubtedly considerable data not available to the authors that would give more information on this subject and the presentation of it would be most welcome.

Relation Between Cloud Height and Proportion of Strokes to Earth

It has been commonly observed that the greater the cloud height the lower the ratio of strokes to ground to discharge wholly within the cloud. In England, thundercloud heights have been found to range from 1,000 to 30,000 feet with an average of about 6,500 feet.[20] Wormell finds that for this average cloud height the ratio of positive field changes to negative field changes at greater than the reversal distance averages 1.7. This gives the ratio of ground discharges to those in the cloud. Schonland's data give a ratio of 0.22 for Cape Town where he states that the cloud heights are generally greater than in England. Halliday finds a ratio of 0.36 at Johannesburg, but he merely states that the cloud heights are lower than near Cape Town. Available data[1] on thundercloud heights in this country indicate the same range of variance and average as for England.

Wave Shape of Field Produced by Strokes to Earth

The fundamental data on the mechanism of lightning strokes to earth and the magnitudes of the various quantities necessary for the field calculations have been summarized in the previous paper.[1] Equations were also developed for sufficiently accurate calculation at stroke distances up to about one mile. These data will, therefore, be reviewed only sufficiently to enable the development of equations for greater distances which require some modifications in the assumptions of stroke mechanism. The field produced by the initial downward leader and return-streamer portion of a stroke component will be considered first.

Basic Stroke Mechanism

The principal characteristics of the fields produced during this period of the discharge can be determined sufficiently

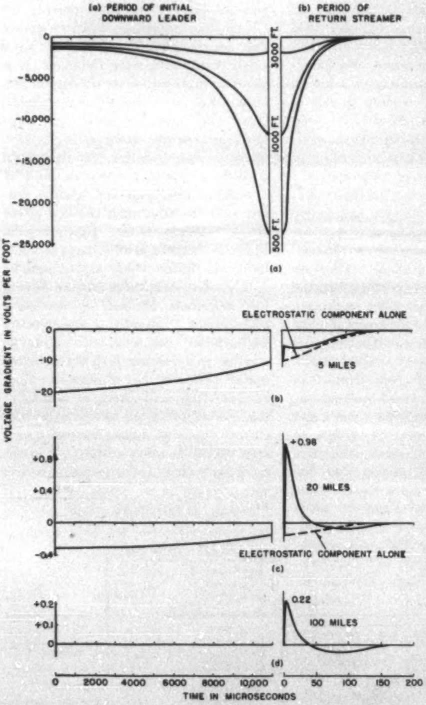
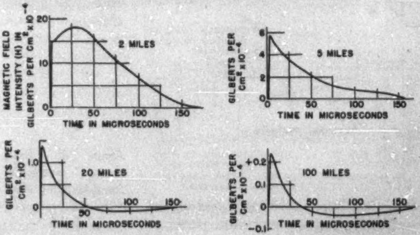


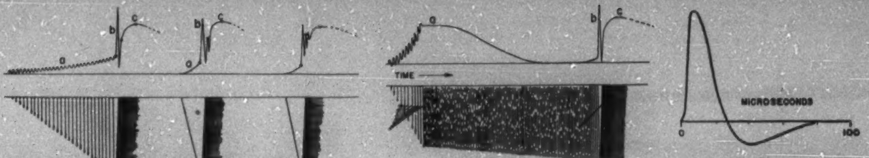
Figure 7 (left). Electric ground gradient during initial-leader and return-streamer period of a single-component negative stroke as function of stroke distance (*d*)

Stroke conditions of Figure 6

Figure 8 (below). Magnetic-field intensity (*H*) for a single-component stroke as function of stroke distance (*d*)

Stroke conditions of Figure 6





(A)

(B)
AT MEDIUM DISTANCES[29]

(C)
AT LARGE DISTANCES[29]

Figure 9. **Relation between stroke mechanism and ground gradients as observed by Schonland[29]**

accurately by the basic assumptions illustrated in Figure 5. The effects of variation from these conditions will be treated later. The cloud charge involved in this portion of the discharge is assumed to be lowered at a constant rate and distributed uniformly along a straight vertical channel. This charge is then lowered more rapidly the rest of the way to ground by the return streamer. The current flowing at the earth terminus of the stroke and in the return-streamer channel can be considered as determined by the distribution along the stroke channel of its charge and the velocity of the return streamer which instantaneously discharges each portion of the channel as it is reached. The stroke current flowing at any instant can be considered as the product of the return-streamer velocity and the charge density at the point on the channel reached at that instant. For the short distances considered in the previous paper, variation of these quantities along the channel did not affect the field calculations greatly. However, at greater distances they become important. For the basic calculations it is assumed that the stroke-current variation with time is determined entirely by variation of the return-streamer velocity along the stroke channel.

EQUATIONS FOR FIELDS AT LARGE DISTANCES

In the previous paper the field equations developed for short distances from the stroke channel were fairly complex. At larger distances, for which the distance *r*, shown in Figure 5, from the point of field measurement to any point involved in the stroke discharge varies only slightly from the horizontal distance *d*, considerable simplification can be made.

The equation for the electric ground gradient can be written as follows:

$$E=5.9\times10^{10}\left[\frac{M}{d^3}+\frac{1}{cd^2}\frac{\partial M}{\partial t}+\frac{1}{c^2d}\frac{\partial^2 M}{\partial t^2}\right]$$

volts per foot (1)

and for the magnetic field intensity the equation is

$$H=6.48\times10^{6}\left[\frac{1}{cd^2}\frac{\partial M}{\partial t}+\frac{1}{c^2d}\frac{\partial^2 M}{\partial t^2}\right]$$

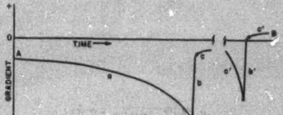
gilberts per square centimeter (2)

It is horizontal, and its direction, of course, conforms to the right-hand screw law with respect to the flow of current in the stroke channel.

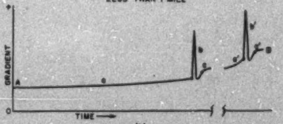
In these equations

d = distance from stroke channel in feet
t = time in seconds
c = velocity of light = 984×10^6 feet per second

M is the moment of charge in coulomb-feet at the time d/c before the field is measured at the point *P*. It is the integral at any instant of the product of each differential element of charge involved in the discharge and its height above ground. The effect of the image charge shown in Figure 5 is taken care of by a factor of 2 included in the equations. These equations give an accuracy of about ten per cent for distances that are twice the cloud height (*H*). They are thus sufficiently accurate for the average channel height at distances greater than about two miles. The equations contain three terms involving the charge moment and its first and second derivatives. These are commonly designated as the electrostatic, induction, and radiation components, respectively. It is only after the start of the return-streamer period of the discharge that the induction and radiation terms are appreciable. As shown by the equations, the three terms decrease respectively with the third, second, and first power of the distance. Thus their relative importance varies considerably with stroke distance. The equations for the fields during the initial downward leader and return-streamer periods have been developed in the appendix for the basic mechanism of Figure 5 and an exponentially varying stroke current.



(a)
LESS THAN 1 MILE



(b)
5 TO 20 MILES

Figure 10. **Ground gradient during period of a two-component multiple charge**

FIELDS PRODUCED BY A TYPICAL STROKE

To show the principal characteristics of the field disturbances and their variation with distance, calculations have been made for a negative stroke component of average characteristics. The average value of 0.5 foot per microsecond is used for the effective velocity of the initial leader for the first component of a stroke. For successive components of a multiple stroke the dart-leader velocity averages about ten feet per microsecond. The current and return-streamer velocity are shown in Figure 6a. In Figure 6b are the curves of the moment and its derivatives during the return-streamer period.

The use of an exponential expression that gives a reasonable representation of the high-magnitude portion of the stroke current does not represent the subsequent continuing flow of low current that may be present to varying degrees. It also results in too long a time for the return streamer to reach the cloud. Therefore, the wave shape after 100 microseconds was modified, as shown by the dotted portion of the curve of Figure 6a.

Figures 7 and 8 show the electric ground gradient and the magnetic-field intensity as calculated for various distances from the stroke channel. The curves of Figure 7a for distances below one mile were determined from the equations of the previous paper.[1] At these distances only the electrostatic component of the electric field is appreciable. Also, below about one mile the field increases negatively during the progress of the initial downward leader and then rapidly decreases as the return streamer propagates up the stroke channel. From one to about five miles it decreases during both periods. However, as distance is increased further and the first and second derivative components become important, the field during the return-streamer period has an increasingly positive peak. At distances above about 20 miles both the electrostatic and induction components of the field become negligible. No appreciable field change occurs during the initial downward-leader period, and the field during the period of the return streamer has the oscillatory wave shape of the radiation component.

The magnitudes of the currents flowing in the stroke channel and their rate of variation with time are only high enough to produce a magnetic-field intensity of appreciable magnitude after formation of the intense return streamer. As shown in Figure 8 the first derivative component of the field during this period predominates up to about two miles. Above 20 miles the second derivative term predominates and the wave shape is identical with that of the electric gradient.

Most field measurements have been of the electric field, using either a short horizontal antenna, a vertical rod, or sphere. Published cathode-ray oscillograms that are not distorted by propagation conditions (to be discussed later) do not have sufficient resolving power to show the wave shape accurately. Those of Schonland appear to give the most reliable information. For relatively close distances he has co-ordinated them with Boys camera records of the stroke mechanism,[26] and the effects of propagation can be eliminated from those he obtained at large distances.[34] In Figure 9 are shown typical records obtained in South Africa and published by him. Figures 9a and b illustrate the characteristic fields he recorded at medium distances. Figure 9c is typical of those recorded at greater distances and shows the characteristic oscillatory wave shape of the second derivative component of the moment. All of his measurements were made at distances for which the radiation component predominated. He found that the field change during the period in which the return streamer is propagating up to the cloud has the characteristic wave shape shown by Figures 7d and 9c. Figures 9a and b show the two principal types of initial leader conditions which he observed. Schonland designated the three principal periods of the discharge mechanism as *a*, *b*, and *c*. The symbol *a* designates the initial downward-leader period, *b* the return-streamer period, and *c* the period after the return streamer has reached the cloud and ceased to propagate upward. Figure 9a, typical of about 65 per cent of the recorded cases, indicates, as Schonland points out, that the effective rate of progress of the initial downward leader is uniform and that charge is distributed approximately uniformly along the channel. The ripples superimposed on the main field change in this period are produced by the stepped leader mechanism. About 35 per cent were typical of Figure 9b for which the downward progress of the stroke leaders starts at a uniform rate of the same order of magnitude as for the first case. However, at a certain point the stepped leaders suddenly become shorter, and the effective rate of propagation decreases. Because of the time constant of the amplifier used for the field measurements, the recording spot falls to zero during this period on account of the relatively slow change of gradient. This second type of leader mechanism has been attributed by him and others[42] to the presence of space charge in the air which tends to neutralize the charge lowered from the cloud. The difference between the two initial leader conditions does not appear to affect the more important and higher-magnitude field changes recorded during the *b* and *c* periods.

Figure 9 illustrates the third phase in the field change, the *c* portion which is the field change produced by the further slower lowering of charge to earth after the return streamer has reached the charge center at which the stroke originated. This process produces the low-magnitude long-duration portion of the stroke current. Fulchronograms of direct strokes show that the length of this period and the amount of charge lowered varies widely. For some records this portion of the stroke current has been of negligible duration and in others has lasted several hundredths of a second. Magnitudes of charge as high as 50 coulombs have been measured for the charge lowered in this period.[37] Lowering of charge does not necessarily continue until the start of another component in the case of a multiple stroke.[41] However, McEachron has found that this is generally the case for strokes to very tall objects.[38] As illustrated in Figure 9a Schonland found that subsequent components of multiple strokes sometimes produced additional lower-magnitude oscillation in the field just following the *b* period and just after the return streamer had reached the cloud. This will be discussed later.

The net change in field from the start of the initial downward leaders to the end of the *c* portion represents the field change *A-B* shown in Figure 2 that would be recorded by the capillary electrometer for a single-component stroke. It is, however, only one phase of the field change for a multiple stroke. The complete characteristics of the electric gradient during the whole period of the discharge is illustrated in Figure 10 which shows the field change at close and medium distances for a two-component stroke. At greater distances, of course,

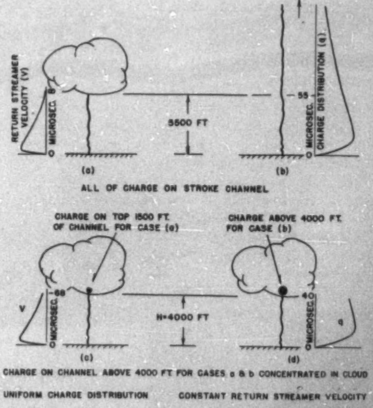

RETURN STREAMER VELOCITY (V)
8
MICROSEC.
0
5500 FT
55
MICROSEC.
CHARGE DISTRIBUTION (q)
0
∞
(a)
(b)
ALL OF CHARGE ON STROKE CHANNEL
CHARGE ON TOP 1500 FT. OF CHANNEL FOR CASE (a)
CHARGE ABOVE 4000 FT. FOR CASE (b)
V
68
MICROSEC.
0
H=4000 FT
40
MICROSEC.
0
q
(c)
(d)
CHARGE ON CHANNEL ABOVE 4000 FT FOR CASES a & b CONCENTRATED IN CLOUD
UNIFORM CHARGE DISTRIBUTION
CONSTANT RETURN STREAMER VELOCITY

Figure 11. Four assumptions of charge distribution, return-streamer velocity, and channel height that produce the same stroke current

Crest return-streamer velocity 100 feet per microsecond. Stroke current of exponential 2x40-microsecond wave shape with crest of 20,000 amperes where the electrostatic field component is negligible, the net change from A to B is zero, and the oscillatory components of field occurring during the b period each start from and return to essentially zero. The same condition is always true for the magnetic-field intensity, regardless of distance.

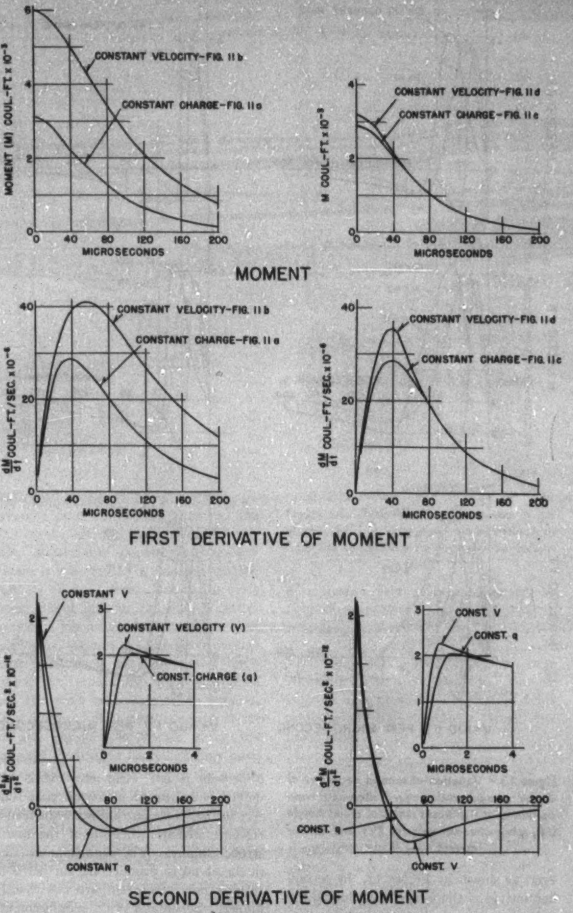


Figure 12. Comparison of charge moment and its derivatives for the stroke-mechanism assumptions of Figure 11

Variations From Basic Stroke Mechanism

When consideration was given to the fields at close distances from the stroke, it was found[1] that conditions in the lower part of the stroke channel near the earth are of most importance. For the distances considered in this paper, appreciable field changes are produced, not only during the first few hundred feet of propagation of the return streamer, but during the entire period that high rates of charge movement exist. Conditions at the top of the stroke channel and in the cloud itself assume greater importance. Only the conditions which were not treated sufficiently in the previous paper[1] for large distances will be considered. The justification for assuming that the channel charge is neutralized instantaneously by the return streamer has been considered. The effects of upward streamers from the ground were shown and found to be unimportant at stroke distances greater than 500 feet. Irregularities of the main-stroke channel have also been treated. Their effects decrease with distance, and, except for irregularities that they might produce in the wave shape of the stroke current, they are unimportant at distances where r of Figure 5 to any part of the stroke channel can be considered equal to the stroke distance d. This consideration applies to the field disturbance that propagates directly to the point of measurement. Irregularities in the shape of the main channel do become important at large distances for the field disturbances reflected from the ionosphere. This will be considered later.

Variation of Charge Distribution Along Channel

Although measurements of the return-streamer velocity show sufficient variation along the stroke channel to determine entirely the stroke-current wave shape,[1] and the field measurements of Schonland[26] indicate approximately uniform charge distribution along the channel, a study was made to determine the accuracy of the assumption of uniform charge distribution. The two limiting relationships which can be assumed between return-streamer velocity V and channel charge q are those shown in Figures 11a and b. Figure 11a represents essentially the basic mechanism conditions of constant charge already treated. Figure 11b represents the assumption of constant return-streamer velocity and variation of charge distribution to produce the proper stroke-current wave shape. If an exponential wave form is used for the stroke current in which it has decreased to zero by the time the return streamer has reached the top of the stroke channel, the required variation of V for Figure 11a produces infinite time for the return streamer to reach the channel top and the variation of q for Figure 11b, an infinite channel height. Equations 5 to 10 of the appendix give the charge moment and its derivatives for these two cases, and these quantities for the specific conditions in Figure 11a and b are compared on the left side of Figure 12. The greater height for the case of constant V produces a greater initial-charge moment and a greater change of moment. How-

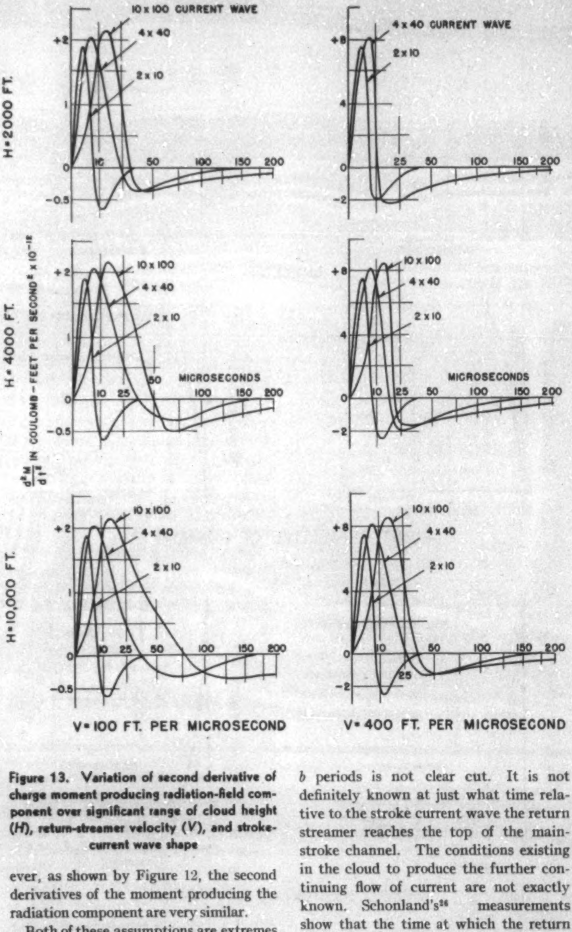


Figure 13. Variation of second derivative of charge moment producing radiation-field component over significant range of cloud height (*H*), return-streamer velocity (*V*), and stroke-current wave shape

ever, as shown by Figure 12, the second derivatives of the moment producing the radiation component are very similar.

Both of these assumptions are extremes that are not representative of actual conditions. A comparison of the two sets of conditions should be based on equal cloud heights sufficiently below 5,500 feet to produce a reasonable time for the return streamer to reach the channel top in the case of Figure 11a. For large distances from the stroke channel, the charge above this point can be assumed concentrated at the top of the channel.

RELATION BETWEEN CHARGES IN CLOUD AND ON STROKE CHANNEL

The transition between the two periods that Schonland has defined as the *b* and *b* periods is not clear cut. It is not definitely known at just what time relative to the stroke current wave the return streamer reaches the top of the main-stroke channel. The conditions existing in the cloud to produce the further continuing flow of current are not exactly known. Schonland's[26] measurements show that the time at which the return streamer reaches the cloud is well down on the tail of the high-current portion of the stroke current, because even the second or negative part of the radiation field component in his records is over before the return streamer reaches the cloud, and the current must have decayed to a low value by this time.

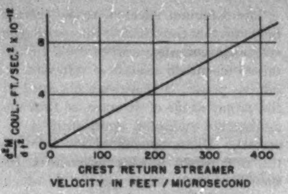
That the relation between the amount of the charge on the channel and in the cloud is not important if the latter be considered as concentrated at a point can be shown by the two conditions set up in Figures 11c and d. All of the charges above 4,000 feet for the two previous cases are concentrated at the channel top. This produces a wide variation in the ratio between the two sets of charge. It also provides a better basis for comparing the effect of channel-charge variation. The additional equations 11 to 15 are necessary for determining the charge moment and its derivatives when charge is concentrated at the channel top. The three components of the field are compared on the right-hand side of Figure 12. Practically equivalent results are obtained which show that not only is the transition period between charge movement on the main-stroke channel and in the cloud unimportant, but also the variation of charge distribution along the channel, as long as the stroke current is fixed.

WAVE SHAPES DIFFERING FROM EXPONENTIAL FORM

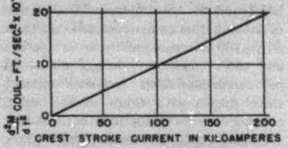
With uniform channel-charge distribution, calculations were made with the return-streamer velocity varying in several possible ways that still would produce the same standard definition of a current wave in terms of time to crest and time to half value. For any logical wave form the method of varying the tail was found to be unimportant, unless pronounced irregularities were produced. The greatest effect is caused by variations in the front of the current wave, and it appears principally in the crest and time to crest of the first peak of the radiation component. However, for a range of current fronts including a straight line producing a uniform rate of rise, an exponential wave, and a sine wave both passing through the 10 and 90 per cent points of the uniform front, the variations of the time to first crest of the radiation component and its magnitude were less than ten per cent.

EFFECT OF BRANCHING

Boys camera records published by Malan and Collens[40] indicate that the brilliancy of the stroke channel suddenly increases at the instant the return streamer reaches a branch point. Bruce and Golde point out that this probably is associated with an increase of stroke current, and thus irregularities are produced in the stroke current as each branch point on the channel is reached. As shown by equation 5 of the appendix, this should produce a similar variation in the charge moment which would be accentuated in its first and second derivative producing oscillations in the radiation component of the field. However, the electric-field records of Schonland[26] which are correlated with Boys camera



(a) CREST CURRENT 20 KILOAMPERES



(b) CREST RETURN STREAMER VELOCITY 100 FEET / MICROSECOND

Figure 14. Effect of return-streamer velocity and crest stroke current on crest magnitude of second derivative of charge moment producing radiation-field component

records show no evidence of such oscillation during the period the return streamer is propagating back to the cloud. Neither has any evidence of this been found as yet in stroke-current records.[37,38,39]

Additional Oscillations for Successive Components of Multiple Strokes

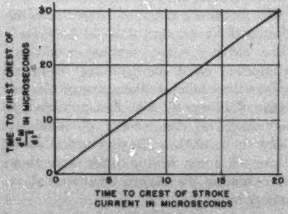
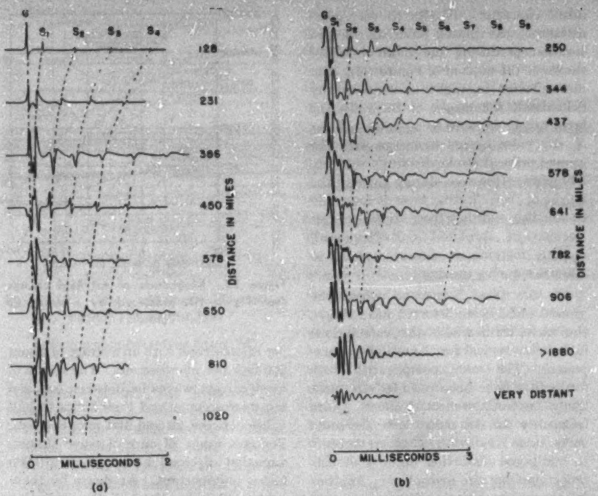


Figure 15. Effect of stroke-current wave front on front of radiation-field component



(a)

(b)

Figure 16. Train of pulses produced in the electric field at large distances by reflections from the ionosphere as observed by Schonland[34]

G represents field propagating directly to point of measurement. S_n are the reflected pulses

The successive oscillations recorded by Schonland[26] in the period following the return streamer's entry into the cloud (see Figure 9a) are difficult to account for quantitatively, since little is known about the stroke mechanism within the cloud. He found that in general the duration and number of oscillations increased progressively with each successive stroke component, indicating that each additional set of oscillations is identified with each new charge center tapped. The variation in height of these charge centers can be large enough that the streamer path connecting them to the main-stroke channel will be quite variable. As a certain successive stroke component is initiated by a steamer starting from its charge center, it may, in some cases, travel first to the charge center producing the component occurring just previously, thence to the next charge center, and so on until the center originating the initial component is reached and the path is completed to ground. Thus, although the return-streamer velocity and charge distribution along the streamer channel in the cloud may have no irregularities, variations in the cloud-streamer path might produce field irregularities, that would be accentuated in the radiation component, even though they are not produced in the stroke current.

Schonland himself points out[26] that Boys camera records do not indicate irregularities in the channel current below the cloud at this corresponding time, nor do they commonly appear in measurements of the stroke current at the ground.[37] The only measured stroke current known to the authors with such a characteristic is that discussed under "Types of Lightning Discharges," in which the current reversed polarity five microseconds after the start of the high-current component. The relative effect of such irregularities in the cloud-streamer path should decrease with stroke distance, and Schonland makes no mention of this effect in the fields recorded at large distances,[34] although he states that the fields from successive components were recorded. The wave shape of Figure 9c is given as typical of all records of fields undistorted by reflections from the ionosphere.

Successive Reflections From the Ionosphere

The wave shapes of fields measured at great distances are very complex and show considerable distortion from propagation which makes them difficult to interpret. Schonland,[34] however, has quite definitely established that this distortion is produced by successive reflections of the radiated field between the ionosphere and earth which, because each higher order of reflected wave travels a greater distance to the point of measurement, produces a train of waves. This is illustrated by the set of records in Figure 16 reproduced from Schonland's paper. As the stroke distance increases, the time interval between the field disturbance propagating directly to the point of measurement (the ground pulse *G*) and the sky pulse *S* becomes smaller, until at a certain distance the sky pulse S_1 of Figure 16 merges with the ground pulse and thus distorts it, so that it loses its initial character. Finally, at very great distances, the pulses have all merged together producing the type of record shown at the bottom of Figure 16b. For atmospherics at night as recorded by Schonland, the height of the reflecting layer ranges from 50 to 60 miles and the S_1 sky pulse begins to merge with the ground pulse at stroke distances of 250 to 300 miles. However, during the day the height of the reflecting layer is lower, 30 to 40 miles, and they merge much sooner. Records of Appleton and Chapman[32] indicate merging at distances of 50 to 100 miles during the day.

All records of Schonland where the ground pulse is undistorted have shown the wave form which the calculations indicate as typical for the radiation component. The more complicated wave forms that have been recorded are most likely due to this reflection effect. Quite frequently the sky pulses have the same wave form. However, they are subject to additional distortion because of this reflection from the ionosphere. Another factor causing variation from the ground pulse is stroke-path irregularity. The sky pulse reflected to a certain point on the earth is the result of the portion of the field radiated from the stroke at a certain angle with the vertical to the ionosphere. For successive pulses this angle is different. If the stroke path is considered as made up of individual segments inclined at varying angles, the field radiated at different angles will vary, and thus each segment can produce a different field for each successive sky pulse.

Effect of Cloud Height, Return-Streamer Velocity, and Stroke Current

The principal factors affecting both the electric and magnetic field are the cloud height, return-streamer velocity, and channel charge which can be specified in terms of the stroke current the quantity that has been measured. The effect of these on the radiation component can be analyzed quite simply, so that they can be established over the principal range of distances. However, at close distance (one to ten miles) their effects are too complicated and interrelated to warrant summary.

In Figure 13 are shown curves of the second derivative of the charge moment for cloud heights, crest magnitudes of return-streamer velocity, and current wave shapes covering the principal range of variation. As shown previously, the principal range for the crest magnitude of the return-streamer velocity as indicated by available data is 100 to 500 feet per microsecond with an average of about 200 feet per microsecond. The fronts of most current waves lie between one and ten microseconds and their times to half value between 20 and 100 microseconds. For this range of current wave shapes, variation of cloud height above 10,000 feet is unimportant. As shown by these curves, the duration of the radiation component increases with stroke-current duration. The field wave shape is independent of everything but stroke current for current waves as short as the 2x10-microsecond wave. For longer current waves, the duration of the first positive portion of the field increases somewhat with increasing cloud height and decreasing streamer velocity. However, the magnitude is affected little by cloud height and depends principally upon the magnitude of the stroke current and return-streamer velocity being directly proportional to both as shown by Figure 14. It is interesting that the time to the first crest is proportional to the time to crest of the stroke current as shown by Figure 15 and independent of the other factors.

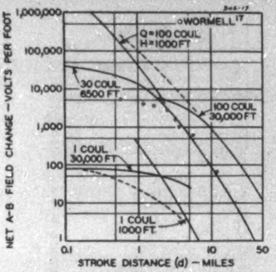


Figure 17. Magnitude of net field change (see Figure 10) produced by a stroke to ground

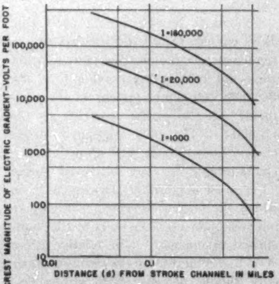


Figure 18. Crest magnitude of field change produced at close distances by a stroke component (see Figure 7a)

The dominant effect of stroke current indicates that considerable information on stroke-current wave shapes could be obtained from field records of sufficient resolving power, if they were obtained in the proper range of distance, so that the undistorted radiation component is recorded. Measurements of the front of the field would provide information on the fronts of the stroke current for which there are fewer data than for any other part of the current wave. Schonland[28,34] has found the total measurable duration of the radiation component to range from 50 to 250 microseconds with an average and most common value of about 80 microseconds. This indicates current wave shapes with times to half value ranging from 20 to 100 microseconds and averaging about 30 microseconds. This range of wave shape is also indicated from the range (20–70 microseconds)[34] of the measured durations of the first positive portion of the field. This checks quite well with stroke-current measurements.[37] Attempts at stroke-current analysis from field measurements have been made by Norinder.[23] However, they are inaccurate, because not enough knowledge of the stroke mechanism was available at the time.

Magnitude of Electric Fields

In Figures 17, 18, and 19 are given data on the range of magnitudes of the principal field changes produced during a discharge to ground and their variation with stroke distance. The solid curves were calculated from the formulas of this and the previous paper,[1] and the plotted points represent actual measurements. In Figure 17 are shown the range of the net *A-B* field change as produced by the cloud charge lowered by a stroke and the field change recorded by the capillary electrometer. The curves were calculated by assuming the total stroke charge to be concentrated at one point. Stroke-current measurements[37,38] indicate that one to 100, coulombs is the principal range for the amount of charge lowered with an average of 20 to 30. Cloud-height data indicate a range of 1,000 to 35,000 feet with an average of about 6,500 feet. The curve for the average conditions conforms quite closely, for distances above three miles, to the averaged plotted data of Wormell[17] who has published a summary of the records obtained at Kew up to 1939. The divergence below three miles may be due to the inaccuracy of assuming the charge concentrated at one point.

In Figure 18 are plotted curves for the crest magnitude of the field at close distances which has the wave form given by Figure 7a. For this range of distance crest stroke current is the principal factor determining the field. As shown by this curve quite high gradients can be produced as far away as half a mile. They are sufficient to produce sparking, even between relatively small metallic objects at close spacings. Induced sparks produced in this way are a consideration in structures which constitute a fire or explosion hazard. There is evidence that fires are started in wooden barns in this manner. No field measurements are known to the authors for this range of distances. Figure 19 applies to the crest magnitude of the radiation-field component which predominates above 10 to 20 miles. The magnitude of this component is a function only of the crest stroke current and return-streamer velocity, and it should vary inversely with distance (see equation 1). This is verified by the averaged plotted points of the field measurements shown on the figure. They also confirm the estimated average curve based upon an average crest stroke current of 20,000 amperes and an average crest return-streamer velocity of 200 feet per microsecond.[1] However, the field can be distorted somewhat at distances above 50 miles in the daytime and 200 miles at night by sky reflections. The good comparison between the calculated and measured values indicates that the distortion does not greatly affect the crest magnitude of the field. Similar field data published by Lutkin[31] are not plotted, because the averaged values are more than ten times those plotted in Figure 19 and lie above the calculated upper limit. It is felt that there is some error in his results.

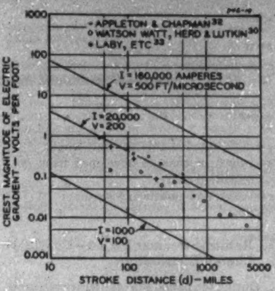


Figure 19. Crest magnitude of electric gradient at large distances

Radiation component (see Figure 7d)

Fields From Cloud-to-Cloud Discharges

Insufficient information is available on the mechanism of cloud-to-cloud discharges to calculate the fields they produce. Wormell has found that the total *A-B* field change of such discharges is the same order of magnitude as for discharges to ground, indicating the same range of magnitude of charge to be neutralized. However, the fields produced at close distances will be very much less and insignificant below one-half mile compared to those shown in Figure 7a for cloud-to-ground discharges. The reason for this is that the discharge mechanism takes place at a great distance from the earth. The radiation fields recorded at larger distances also have been found to be less[34] and are distinguishable from those produced by strokes to ground. The reason for this is that the rate of combination of charge is much lower. For the more common type of discharge between the negative charge in the cloud base and the positive charge in the cloud top, photographic records of the discharge mechanism indicate that it consists of a series of "dart" leaders similar to those initiating subsequent components of multiple strokes but unaccompanied by the intense return streamer.[34] Several such records have been obtained in the Westinghouse lightning investigation that are, however, too faint for reproduction. One of these showed six such dart leaders of very short duration compared to the typical return streamer and separated by time intervals ranging from 0.0005 to 0.015 second with a total duration of 0.109 second. The time intervals between these streamers is in general much less than for the interval between multiple components of cloud-to-ground discharges. The fields as recorded for such discharges[31,34] are a series of very short pulses, each lasting not more than about five to ten microseconds. Records have also been obtained[31,34] of fields which have the characteristic of the gradient produced by the step-leader process of a stroke to ground. They consist of very low magnitude and short pulses separated by intervals of the order of 50 to 100 microseconds. These have been attributed by Lutkin[31] to the type of discharge previously discussed, starting toward the earth but not reaching all the way to ground.

Summary and Conclusions

The variations of the electric ground gradient produced by lightning discharges have been compared with the gradient present at all other times both in fair and disturbed weather. These rapid variations provide the only definite means of identifying and locating thunderstorms from the nature of the ground gradient.

The field data now available indicate that, although there are several types of lightning discharges, the great preponderance is of two kinds; cloud-to-ground discharges involving only the negative charge in the base of the cloud, and cloud-to-cloud discharges between these negative-charge centers and the positive charge in the cloud top. Their relative frequency of occurrence varies with cloud height. The lower the cloud base above ground, the greater the number of strokes to ground. Field measurements indicate that their ratio to cloud-to-cloud discharges ranges from 0.22 in South Africa to 1.7 in England where cloud height conditions are about the same as those over relatively flat terrain in the United States.

The analytical study that has been made of the electric and magnetic fields produced by strokes to ground gives their magnitude and wave shape during the entire period of the discharge as a function of the stroke distance. Measurements of the net field change from the beginning to the end of a stroke to ground confirm the average of 30 coulombs, as found by stroke-current measurement, for the charge lowered to earth. The crest magnitude of the fields produced at distances below about a half-mile are determined primarily by the magnitude of the stroke current. Fields of sufficient magnitude to cause induced sparks even between small metallic objects can be produced at distances as great as one half-mile. The data on magnitude of the ground gradient provide a method of determining sparking conditions which are an important consideration in the protection of structures that are a fire or explosion hazard and which can be produced at even greater distances. Above 10 to 20 miles the magnitude and wave shape of the stroke current and the magnitude of the return-streamer velocity are the principle controlling factors. Calculated and measured values of the magnitude of the field in this range of distances are in good agreement. Averaged measured values conform closely to calculation of the field using magnitudes of return-streamer velocity (200 feet per microsecond) and stroke current (20,000 amperes) that other investigations indicate to be average. In the intervening range number of influencing factors is large and their effects complex.

Despite the complex wave forms that have been recorded at great distances, the field as radiated from the stroke has a relatively simple well-defined form determined principally by the wave shape of the stroke current. The complex records are produced by reflections from the ionosphere that begin to cause distortion at distances ranging from 50 to 300 miles. Analysis of available records of the wave shape of the electric field indicates that they are produced by strokes with currents having times-to-half value ranging from 20 to 100 microseconds and averaging about 30 microseconds. This checks measurements that have been made of stroke currents. More accurate records of the undistorted field should give considerable information on the wave shape of stroke currents, particularly their fronts for which relatively little information is available.

Cloud-to-cloud discharges usually produce a net change in field of the same order of magnitude as strokes to earth indicating that about the same amount of charge is neutralized. However, the field changes produced are usually distinguishable from those of ground strokes by being of lower magnitude and consisting of a series of pulses of very much shorter duration with time intervals generally much less than between successive components of strokes to ground.

Appendix. Equations for Lightning-Stroke Charge Moment and Its Derivatives

General equations can be developed for the moment and its first and second time derivatives for use in the field equations of the section, "Equations for Fields at Large Distances."

1. Before Start of Discharge

Just previous to the initiation of the downward leader, see Figure 5a

$$M=QH \tag{3}$$

where

M = moment of charge
Q = concentrated charge
H = height of charge

All units are in terms of coulombs, feet, and seconds.

2. Initial-Leader Period

During the downward leader process, Figure 5b, if uniform initial-leader velocity, v, and uniform channel-charge distribution, q, are assumed, the equation for the moment is

$$M=\int_H^0 q(H-z)dz+(Q-qz)H$$

where

$$z=vt$$

$$M=qHvt-\frac{qv^2}{2}t^2+QH-qHvt \tag{4}$$

where

q = distributed charge per-unit length
z = length of channel developed from concentrated charge
v = velocity of downward streamer
t = time

3. Return-Streamer Period—Constant-Charge Distribution

During the period that the return streamer is neutralizing the charge on the channel, the analysis is based upon an assumed current of the form

$$i=I(e^{-\alpha t}-e^{-\beta t})$$

From

$$i=qV$$

and Figure 5c, the expression for velocity, V, may be evolved

$$V=\frac{I}{q}(e^{-\alpha t}-e^{-\beta t})$$

then

$$y=\int_0^t V\,dt$$

$$y=\frac{I}{q}\left(\frac{1}{\alpha}-\frac{1}{\beta}-\frac{e^{-\alpha t}}{\alpha}+\frac{e^{-\beta t}}{\beta}\right)$$

The total length, L, of the channel is this same integral carried to infinity.

$$L=\frac{I}{q}\left(\frac{1}{\alpha}-\frac{1}{\beta}\right)$$

The moment is then from the definition

$$M=\int_y^L qy\,dy$$

$$M=\frac{qL^2}{2}-\frac{qy^2}{2}$$

Substituting the values for L and y in terms of t

$$M=\frac{I^2}{2q}\left[\left(\frac{1}{\alpha}-\frac{1}{\beta}\right)^2-\left(\frac{1}{\alpha}-\frac{1}{\beta}-\frac{e^{-\alpha t}}{\alpha}+\frac{e^{-\beta t}}{\beta}\right)^2\right] \tag{5}$$

Now by differentiation

$$\frac{dM}{dt}=\frac{I^2}{q}\left[(\alpha-\beta)(e^{-\beta t}-e^{-\alpha'})+\right.$$
$$\left.(\alpha+\beta)e^{-(\alpha+\beta)t}-e^{-2\beta t}-e^{-2\alpha t}\right] \tag{6}$$

and

$$\frac{d^2M}{dt^2}=\frac{I^2}{q}\left[2(e^{-2\alpha t}+e^{-2\beta t})-\right.$$
$$\left(\frac{\alpha}{\beta}+\frac{\beta}{\alpha}+2\right)e^{-(\alpha+\beta)t}+\left(\frac{\alpha}{\beta}-1\right)e^{-\alpha t}+$$
$$\left.\left(\frac{\beta}{\alpha}-1\right)e^{-\beta t}\right] \tag{7}$$

4. Return-Streamer Period—Constant-Streamer Velocity

If one starts again from the assumed current wave, the expression for charge distribution may be evolved

$$q=\frac{I}{V}(e^{-\alpha t}-e^{-\beta t})$$

$$y=Vt$$

Here the total channel length, L, must always be infinite. Then

$$M=\int_y^\infty qy\,dy$$

$$M=\int_t^\infty V\,I(e^{-\alpha t}-e^{-\beta t})t\,dt$$

$$M=VI\left[\left(\frac{t}{\alpha}-\frac{1}{\alpha^2}\right)e^{-\alpha t}-\left(\frac{t}{\beta}-\frac{1}{\beta^2}\right)e^{-\beta t}\right] \tag{8}$$

$$\frac{dM}{dt}=VI\left[\left(\frac{2}{\alpha}-t\right)e^{-\alpha t}-\left(\frac{2}{\beta}-t\right)e^{-\beta t}\right] \tag{9}$$

$$\frac{d^2M}{dt^2}=VI\left[(\alpha t-3)e^{-\alpha t}-(\beta t-3)e^{-\beta t}\right] \tag{10}$$

5. Part of Charge Concentrated at Top of Channel

The method of calculation of the three quantities where the channel height arbitrarily is chopped involves working through a point of discontinuity. All the charge which has been considered to be on the channel above the point of chopping is assumed to be concentrated at that point. If L is the fully developed channel height, H is the chopped height, and constant-charge distribution is assumed, the charge which must be concentrated can be expressed as

$$Q_c=\int_H^L q\,dy$$

Up until the time at which the return streamer reaches the height, H, the moment is

$$M=\int_0^H qy\,dy+Q_cH \tag{11}$$

After this time the moment varies directly as the current and

$$M=M'(e^{-\alpha t}-e^{-\beta t}) \tag{12}$$

where α and β are the constants from the assumed current expression. M' is a constant determined by letting M equal Q_cH and t equal the time at which the streamer reaches height H.

After the discontinuity

$$\frac{dM}{dt}=M'(-\alpha e^{-\alpha t}+\beta e^{-\beta t}) \tag{13}$$

$$\frac{d^2M}{dt^2}=M'(\alpha^2e^{-\beta t}-\beta^2e^{-\alpha t}) \tag{14}$$

With the assumption of constant velocity

$$Q_c=\int_0^t q\,dt$$

where t is the time at which the discontinuity occurs. The moment till this time is then

$$M=\int_{y}^{H} qy\,dy+Q_eH \qquad (15)$$

After this time the equations for M, dM/dt and d^2M/dt^2 are the same as given previously for the constant-charge distribution case.

Each curve requires a smooth transition through the point of discontinuity. The best system is a careful sketching of a reasonable curve.

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Staged Fault Tests of Relaying and Stability on Kansas–Nebraska 270-Mile 154-Kv Interconnection

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Synopsis: Results and conclusions are presented from 22 staged fault tests made on a 154-kv transmission line and terminal systems to check the characteristics of new long-line relays, discussed in companion papers,[1,2] which were designed to differentiate between faults and high power swings. Observed power swings and relay performance are compared with theoretical aspects and predictions. Data on the transient-stability limit of the interconnection, together with observations made during the tests, are also presented. Measured values of line constants and short- and open-circuit impedances are given, together with results of attenuation tests at carrier frequencies. Manner of initiating the faults, scheduling of power flows, and organizing personnel are briefly discussed.

FULL-SCALE faults under observed and recorded power-flow conditions were placed on the Kansas–Nebraska 154-kv interconnection and on other lines near the terminals of this interconnection to check operation of newly developed long-line-type carrier-controlled protective relays, to observe interconnected system operation during fault conditions, and to establish approximate transient-stability limits. From the records of these tests, breaker operating times were studied, performance of the new protective relays were examined, and necessary adjustments were made in relays and control circuits to realize desired over-all operating performance of interconnected systems during both normal and transient conditions. Measurements of line constants and attenuation at carrier-current frequencies were also made.

Description of Interconnection

The Kansas–Nebraska 154-kv interconnection includes 161/69-kv transformers and other terminal equipment of the Nebraska Power Company, the 154-kv transmission line, and 161/138-kv transformers and other terminal equipment of the Kansas Gas and Electric Company.[3]

The 154-kv line is 268.52 miles long without intermediate switching stations or taps. It is a single-circuit line of wood-pole *H*-frame construction with horizontal conductor spacing of 14 feet 6 inches and is one of the longest in the world. Ruling span is 600 feet. Since the terminal transformers are on the line side of the circuit breakers and are operated as part of the line, the electrical length is approximately 30 per cent greater than is indicated by its physical mileage. The conductor is 0.683 inch in diameter and is 250,000 circular mils of hollow hard-drawn copper. It is supported on tangent structures by ten ten-inch suspension insulators having $5^3/_4$-inch spacing, and 15 insulators are used on grounded steel supports and on dead ends. Two $^3/_8$-inch high-strength galvanized-steel overhead ground wires shield the line. These have vertical clearance above the power conductors of 12 feet at structures and approximately 20 feet at mid-span. A steel cross member supports overhead ground wires and connects these to both pole grounds at each structure. There are three complete transposition barrels in the line.

The terminal facilities in Nebraska consist of a 40,000-kva 161/69/13.8-kv three-winding transformer bank, three 5,000-kva shunt reactors each connected through circuit breaker to the tertiary winding of the transformers, and a 69-kv oil circuit breaker on the low-voltage side of the transformers for switching the interconnection, together with the necessary controls and protective-relaying equipment.

The terminal facilities in Kansas consist of a 45,000-kva 161/138/12.5-kv autotransformer bank, three 5,000-kva shunt reactors, each connected through circuit breaker to the tertiary winding of the transformers, and a 138-kv oil circuit breaker on the low-voltage side of the transformers, together with the necessary controls and protective relaying equipment.

The oil-circuit-breaker specification called for five-cycle clearing of faults and automatic reclosing within 20 cycles after the trip coil is energized. If one to two cycles are allowed for protective relay time, the interconnection should be reenergized from both ends in approximately 22 cycles after the occurrence of a fault.

To clear the terminal transformers in case of internal faults, differential relays are connected to trip open the corresponding terminal breaker on the low-voltage side of the faulted transformer bank and to close a spring-operated three-phase grounding switch which grounds the 154-kv line. It is then cleared from the opposite end by action of the protective relays and terminal breaker. The ground switches are also used to ground the line when it is out of service for maintenance.

This interconnection adds a substantial new section to what was already the largest interconnected power-transmission system in the world which extends over 20 states, has a total generating capacity of more than 12,000,000 kw, and is normally all operated in synchronism.

The length and importance of the line and the dependence which will be placed on it for transmission of relatively large blocks of power created several unusual problems requiring solution. Of particular importance were: determining the requirements of protective-relay system

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The scope of the staged tests which affected operating conditions and involved dispatchers, operators, relay men, and engineers of five principal transmission systems in four states makes it impracticable to give proper and complete credit to each of those who participated in the tests and made them successful. However, the authors particularly acknowledge the help of H. E. Margrave who directed the application of faults and scheduling of power flows in Kansas and R. E. Phillips who supervised installation and testing of the long-line relays and control work, both of the Kansas Gas and Electric Company; I. H. McNeil and his assistants who installed terminal equipment and initiated the faults and Charles Turner and his assistants who scheduled power flows in Nebraska, both of the Nebraska Power Company; Ralph Kramer and staff of the Nebraska Public Power System; C. A. Streifus who supervised the fault tests and installation and testing of the long-line relays at the Nebraska terminal and F. C. Poage for his advice during the preparation of this paper, both of Ebasco Services, Inc. During the progress of the tests A. R. van C. Warrington and A. J. McConnell of the General Electric Company guided engineers in the operation of the oscillographs and in the analysis of the oscillograms and gave valuable advice on adjusting relays and changing settings to obtain satisfactory performance. The interest and co-operation of the operating organizations of the Empire District Electric Company, the Oklahoma Gas and Electric Company, and Nebraska Public Power System are also gratefully acknowledged. Their assistance in scheduling and controlling power flow over the interconnections contributed in a large measure to the successful consummation of the tests.

needed as analyzed in a companion paper,[1] development of new long-line relays to meet these requirements as discussed in a second companion paper,[2] design of the controls which would include immediate reclosing of both terminal breakers to maintain synchronism following a transient fault, and relaying to protect terminal transformers.

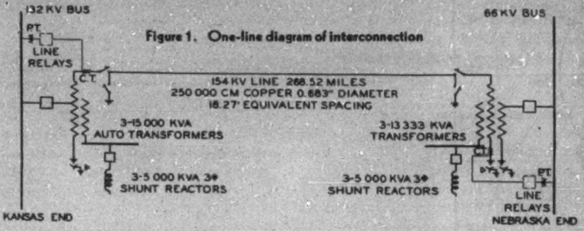

132 KV BUS
P.T.
LINE RELAYS
C.T.
154 KV LINE 268.52 MILES
250 000 CM COPPER 0.683" DIAMETER
18.27' EQUIVALENT SPACING
3-15 000 KVA AUTO TRANSFORMERS
3-5 000 KVA 3φ SHUNT REACTORS
KANSAS END
66 KV BUS
3-13 333 KVA TRANSFORMERS
3-5 000 KVA 3φ SHUNT REACTORS
P.T.
LINE RELAYS
NEBRASKA END

Figure 1. One-line diagram of interconnection

Network-Analyzer Studies

During preliminary studies of the interconnection, a series of a-c network-analyzer studies was made to determine among other things the operating characteristics of the line and the best operating arrangements at terminals. These studies indicated that the interconnection would have a transient-stability power limit between 25,000- and 35,000-kw received power, depending upon operating conditions, type of fault and location, and breaker operating time. These studies also showed it would have steady-state-stability power limit in excess of 50,000 kw.

The transient-stability limits determined by these studies were based on six-cycle clearing of two-wire-to-ground faults at the sending end and 21-cycle reclosing of circuit breakers. Calculations made during these studies showed that although the tendency to reach instability is greatly influenced by breaker clearing and fault time, a much greater factor in determining the transient-stability power limit is the length of time the systems are separated during the open-close reclosing cycle of circuit-breaker operation. The transient-stability limit should be somewhat greater for faults near the receiving end of the interconnection.

Purpose and Scope of Tests

Although the relay and control scheme had been explored thoroughly from a theoretical standpoint, and factory tests, simulating field conditions, had been made on the new type of relays, it was desirable to test thoroughly the completed interconnection under actual system-operating conditions by application of staged faults, rather than to await the results of accidental faults during normal operations. Fourteen staged fault tests were made with zero power transfer over the line, and later eight were made with power transfers approaching the predicted transient-stability power limits.

These tests, made by placing faults deliberately on the 154-kv line and on the terminal systems, had the following objectives:

(*a*). To check and assure proper operation of new long-line-type protective relays for faults occurring on both the 154-kv interconnecting line and on the terminal systems.

(*b*). To check ability of new long-line type of relays to differentiate properly between power swings on the interconnection and actual fault conditions.

(*c*). To check and assure proper operation of relays on the 60- and 132-kv systems in Kansas and on the 66-kv system in Nebraska for faults occurring on the 154-kv line.

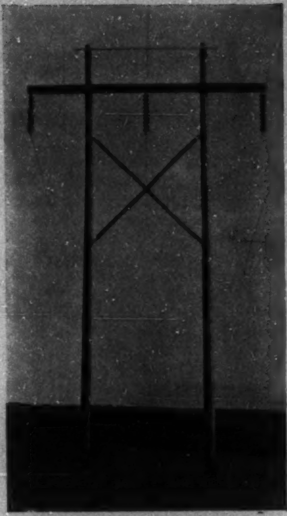


Figure 2. Typical structure, 154-kv line

Table I. Schedule of Staged Two-Wire-to-Ground Fault Tests

Kansas–Nebraska 270-Mile 154-Kv Interconnection—Power-Flow Conditions

	Fault Location		Power Flow on Interconnection at Time of Fault		Relay and Breaker Time* (Cycles) Nebraska				Relay and Breaker Time* (Cycles) Kansas						
Test No.	Line	State	Kw	Received	Trip Circuit Energized	Breaker Interrupted Circuit	Closing Circuit Energized	Breaker Contacts Closed	Trip Circuit Energized	Breaker Interrupted Circuit	Closing Circuit Energized	Breaker Contacts Closed	Net Time* Line Was Not Energized	Total Time* From Fault to Reclose Both Ends	Stability Condition
1	154-kv	Nebraska	15,000	Kansas	$^{1}/_{2}$	$6^{3}/_{4}$	$10^{1}/_{2}$	$21^{1}/_{4}$	$1^{1}/_{2}$	$6^{3}/_{4}$	$6^{1}/_{2}$	$24^{1}/_{4}$	$11^{3}/_{4}$	$24^{1}/_{4}$	Stable
2	154-kv	Nebraska	30,000	Kansas	$^{1}/_{2}$	$6^{1}/_{2}$	$10^{1}/_{2}$	$21^{1}/_{2}$	$2^{1}/_{2}$	$7^{1}/_{2}$	$7^{3}/_{4}$	$24^{1}/_{2}$	10	$24^{1}/_{2}$	Stable
3	66-kv	Nebraska	28,000	Kansas		$10^{1}/_{2}$							0		Stable
3*A*	132-kv	Kansas	29,000	Kansas						10			0		Stable
4	154-kv	Kansas	35,000	Nebraska	$4^{1}/_{2}$	10	15	$26^{1}/_{4}$	1	6	6	23	13	$26^{1}/_{4}$	Unstable**
4*A*	154-kv	Kansas	26,000	Nebraska	$13^{1}/_{4}$***	19	23	$34^{1}/_{2}$	$^{1}/_{2}$	$5^{1}/_{2}$	$5^{1}/_{2}$	22	3	$34^{1}/_{2}$	Unstable
4*B*	154-kv	Kansas	31,000	Nebraska	$4^{1}/_{2}$	10	14	$25^{3}/_{4}$	$^{1}/_{2}$	6	$5^{1}/_{2}$	$22^{1}/_{2}$	$12^{1}/_{2}$	$25^{3}/_{4}$	Stable
5	132-kv	Kansas	34,000	Nebraska						$8^{1}/_{2}$			0		Stable

* Operating time is on 60-cycle basis.

** The disturbance in the Kansas system resulting from this test, caused loss of synchronism between Kansas Gas and Electric Company and Oklahoma Gas and Electric Company.

*** Contact bounce in ground-relay element delayed cutting off carrier which caused pickup of *RB* carrier backup block-tripping relay with consequent slow trip-out.

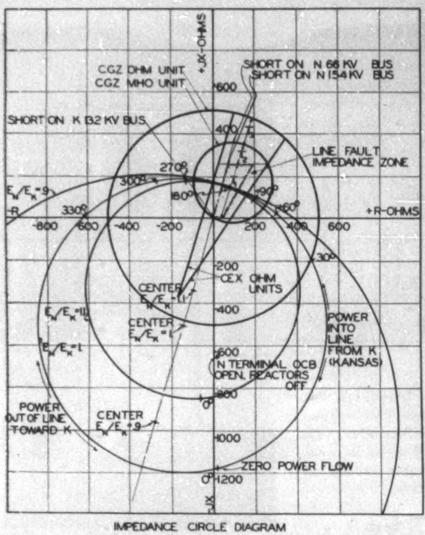


Figure 3. Impedance circle diagram of interconnection as "seen" by relays at Kansas terminal together with characteristics of relay units

(*d*). To check network-analyzer results from which transient-stability power limits for the interconnection were predicted, and to establish more accurately the actual stability limits in order that power flow over the line may be brought within these limits at the approach of storms.

(*e*). To check instantaneous reclosure features of control circuits and terminal breakers, to observe operation of high-speed relays and to make oscillograms of their performance.

(*f*). To obtain information on interconnected system performance and operation during transient conditions helpful to power-plant operators, system dispatchers, relay men, and engineers.

Table II

Positive-sequence open-circuit impedance computed from tests	648/88.13° ohms
Positive-sequence open-circuit impedance calculated	638/88.04° ohms
Positive-sequence short-circuit impedance computed from tests	258/70.6 °ohms
Positive-sequence short-circuit impedance calculated	250/71.74° ohms
Characteristic impedance conductor-to-conductor—computed from tests	409/8.77° ohms
Characteristic impedance one-conductor-to-ground—computed from tests	550/9.2° ohms
Zero-sequence short-circuit impedance computed from tests	744/69.2° ohms

In addition to the preceding fault tests, preliminary low-voltage tests and measurements were made on the 154-kv line with the following principal objectives:

(*a*). To obtain data from which line constants and unit values of resistance, reactance, and susceptance could be determined for check against the values used in design and performance calculations.

(*b*). To obtain the zero-sequence impedance of the line.

(*c*). To measure the characteristic impedance conductor-to-conductor and conductor-to-ground.

(*d*). To check the relative phase rotation of the systems to be interconnected and to check the phase identification which had been followed throughout construction of the line and terminal substations.

Results of Staged Fault Tests

Group I

Fourteen tests, designated as group I, were made with the systems synchronized but with approximate zero power flow over the interconnection.

These tests comprised:

(*a*). Two one-wire-to-ground faults placed on the 154-kv line, one near each of the terminals.

(*b*). Four two-wire-to-ground faults placed on the 154 kv line, two near each of the terminals.

(*c*). Two two-wire-to-ground faults placed on 66-kv lines near the Nebraska terminal of the interconnection.
One two-wire-to-ground fault placed on a radial 66-kv line 19 miles from the Nebraska terminal.

(*d*). Two two-wire-to-ground faults placed on 60-kv lines near the Kansas terminal.

(*e*). One two-wire-to-ground fault placed on a 132-kv line near the Kansas terminal.

(*f*). Two three-phase faults, one on each end of the 154-kv interconnection. These faults were established by tripping closed the grounding switches to simulate the action of transformer differential relays.

Both interconnection breakers successfully cleared the faults and reclosed immediately in four of the six tests made under *a* and *b* above. In one of the un-

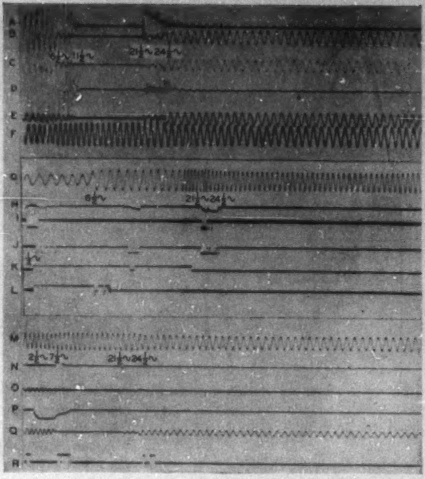


Figure 4. Oscillograms from test 2

Two-wire-to-ground fault on 154-kv line near Nebraska end with 30,000 kw received in Kansas

Nebraska oscillograph records
A—13.2-kv tertiary current
B—154-kv *A*-phase current
C—154-kv *B*-phase current
D—154-kv neutral current
E—154-kv *C*-phase current
F—66-kv *B*–*C*-phase potential
G—66-kv *A*–*B*-phase potential
H—Carrier receiver-relay current
I—Carrier grid-control circuit of phase relays
J—Carrier grid control
K—69-kv circuit-breaker trip and close circuits
L—Trip circuit of *CEX*-ohm and *CGZ*-mho units

Kansas oscillograph records
M—132-kv *B*–*C*-phase potential
N—138-kv oil-circuit-breaker closing circuit
O—154-kv neutral current
P—138-kv oil-circuit-breaker trip circuit
Q—154-kv *B*-phase current
R—Carrier grid control

successful tests, one breaker tripped and reclosed several times in rapid succession as result of a sticking contact in a reclosing relay. In the second unsuccessful test, involving a two-wire-to-ground fault, one breaker, after successfully reclosing, was tripped out immediately by its ground backup relay of the induction disk type. The contacts of this relay had closed during the fault and did not reopen quickly enough. Increase in time-lever setting corrected this trouble.

Neither of the interconnection breakers opened for the six faults on other lines, *c*, *d*, *e*, giving evidence of properly polarized directional-relay elements and correct action of carrier-blocking equipment. Correct relay operation was obtained in the two tests with permanent three-phase faults under *f* but with reclosure features made inoperative.

These tests with zero power flow on line gave proof of the performance of the oil circuit breakers and relay equipment at the two terminals, and they acquainted the operators and dispatchers with the system conditions during faults and laid the foundation for proceeding with group II tests. The tests also gave the men initiating the faults and terminal supervisors experience in co-ordinated procedure and timing of fault application for the more important series of tests to be made under power-flow conditions.

Several weeks elapsed between the tests of group I and group II. During this time and as result of analysis of test data, several changes were made in relay connections and settings, together with some modifications in contact circuits.

Table III

	A	B	C	D
Calculated using data from source 1	$0.851\underline{/2.82^\circ}$	$218\underline{/74.56^\circ}$ ohms	$0.001334\underline{/90.86^\circ}$ mhos	$0.851\underline{/2.82^\circ}$
Computed from test data	$0.851\underline{/2.99^\circ}$	$220\underline{/73.79^\circ}$ ohms	$0.001313\underline{/91.12^\circ}$ mhos	$0.851\underline{/2.99^\circ}$

Group II

Eight tests were made with the systems synchronized and the 154-kv interconnection loaded. These are summarized conveniently in Table I and footnotes. Figures 4 to 8 inclusive are sets of oscillograms taken during this group of tests.

With the exception of tests 4 and 4*A*, in which automatic reclosing of the interconnection was not successful in holding the systems in synchronism, power swings observed throughout the interconnected systems during these tests were small and had little or no effect on operation. In fact these swings during the successful tests were described by operators as being less severe than those experienced when the systems were brought together by the automatic synchronizer operating at the maximum difference in frequency for which it is normally set to work. It was observed that, except in tests 4 and 4*A*, normal load flows on the interconnections were re-established within four or five seconds after each fault. The graphic instrument charts reproduced in Figures 9 and 10 record the power flow on this interconnection during this series of tests.

Following the application of faults on the 154-kv line, the terminal breakers successfully cleared the fault and reclosed in every case where they were intended to do so. The same interconnection terminal breakers likewise remained closed in every test in which fault was applied to other lines near terminals of the interconnection, indicating that satisfactory prevention of tripping was being realized and that the relays functioned properly for external faults under heavy power-flow conditions.

Study of the test results revealed that the expected 21- or 22-cycle over-all time from fault to final reclosure of the last breaker was not being obtained. There was occurring a delay of two to four cycles in the relay action at the end of the line remote from the fault. This has been traced to the fact that contacts on ground relays were vibrating with minimum currents through the coils for a fault at the opposite terminal. Also the tripping zones of the *CGZ* relays as set at that time did not extend far enough beyond the

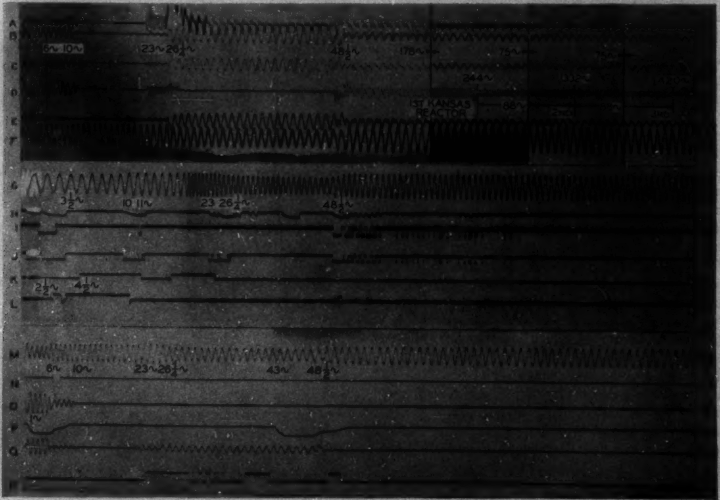


Figure 5. Oscillograms from test 4

Two-wire-to-ground fault on 154-kv line near Kansas end with 35,000 kw received in Nebraska. Unstable. For oscillograph legend see Figure 4 terminals of the line, and one characteristic line of the *CEX* relays was too close to the fault zone. Consequently, these relay elements did not have torque enough to give the desired high-speed operation when operating so close to the edge of their pickup zone, a condition which has since been corrected by changes in tap settings. It should be noted that this could be done without, at the same time, influencing the ability of the relays to differentiate between faults and power swings, because higher torque was obtained by moving their pickup zones a comparatively small distance.

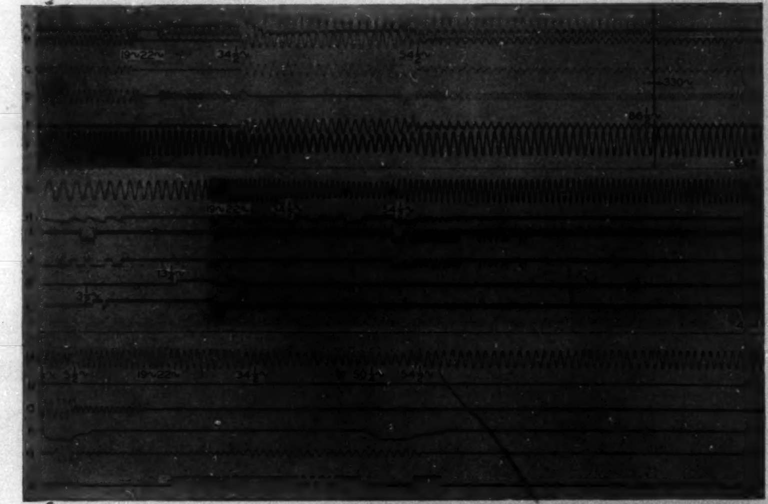


Figure 6. Oscillograms from test 4A

Two-wire-to-ground fault on 154-kv line near Kansas end with 26,000 kw received in Nebraska. Unstable. For oscillograph legend see Figure 4

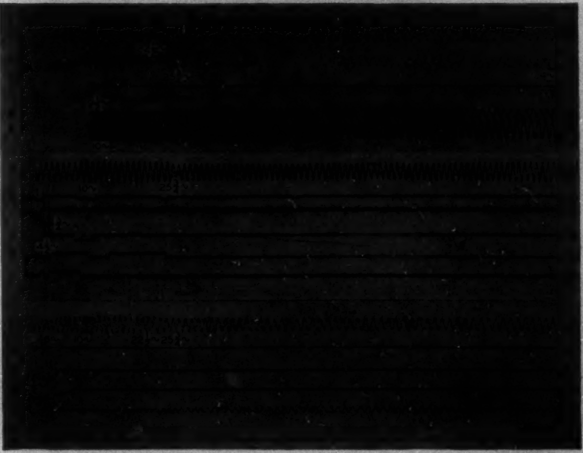


Figure 7 (below). Oscillograms from test 4B

Two-wire-to-ground fault on 154-kv line near Kansas end with 31,000 kw received in Nebraska. For oscillograph legend see Figure 4

The oscillograms of the tests demonstrated the need for improving contact performance of the ground relays in turning on and shutting off carrier. During and subsequent to the tests, certain adjustments were made on the contacts utilizing information from oscillograms, and excellent operation was obtained. Figure 11 indicates how positively the contacts as finally adjusted opened and closed to turn carrier on and off and how the relays were adjusted to realize short operating time of the carrier-relay control circuits.

In all of these tests of group II the long-line protective-relay systems correctly selected the location of fault, blocked tripping on faults occurring outside the protected zone and tripped on all faults in the protected zone, although, as explained above, some undesired delay occurred, which has since been corrected.

Staging of the tests and the scheduling of rapid load changes between tests gave the system operators definite knowledge of the ability of the line to carry load and has increased their confidence in its reliability under fault conditions, enabling them to make maximum use of its capacity. The load dispatchers also gained valuable experience for future scheduling of power in the interconnected systems, not only during normal conditions, but also during emergency conditions when rapid changes in power flow and generation necessarily will have to be made.

Relating Test Results to Impedance Circle Diagrams

In order to check adequately the relay operations during the tests, a continuous record of instantaneous power flow in the interconnection would be desirable to obtain both the magnitude and angle of the equivalent impedance as viewed from either end of the interconnection during the power swings. Since watt elements were not available for the oscillographs, instantaneous values of power were not recorded. However, the test oscillograms recorded the current and voltage from which the kilovolt-amperes and the magnitude, but not the direction, of the equivalent impedance can be calculated.

Figure 12 shows the impedance circle diagram for this interconnection as derived in reference 1 together with the tripping zones between the characteristic lines of the *CEX* angle-impedance relay units as discussed in reference 2. Voltage values from the oscillograms provide data to calculate the ratio of the sending and receiving voltages used to determine the circle corresponding to ratio E_r/E_s existing at a given time during a power swing. On this same diagram the locus of the equivalent impedance $Z = E/I\sqrt{3}$ existing at the same time is circle Z_s.

Point *A* at the intersection of these two circles indicates the equivalent impedance as determined by this method for the crest of the first power swing in test 2 as viewed by the Nebraska relays. Likewise point *B* indicates the equivalent impedance viewed at the same instant by the Kansas relays. These two points both fall outside the tripping zones of the relays, and this checks results of test 2 in which the breakers did not open following initial reclosure. The relays thus recognized the high power swing as such and did not trip.

It should be noted that point *A*, the equivalent impedance at the sending end, falls closer to the boundary of the relay tripping zone than does point *B*, the equivalent impedance at the receiving end at the same instant.

Points *C* and *D* are respectively the sending- and receiving-end impedances for tests 4 and 4*A* at the instant of trip-out following initial reclosure. Point *C* falls within the tripping zone of the *CEX* relays, checking results of tests 4 and 4*A* in which the sending-end breaker was tripped by the *CEX* and *CGZ* phase relays, as the systems swung out of synchronism. Again it will be noted that the receiving-end impedance at point *D* falls farther from the tripping zone than does the sending-end impedance and thus confirms the fact that the Nebraska (receiving-end) breaker did not trip when the systems swung out of synchronism, because the Kansas (sending-end) breaker opened first.

A check and confirmation of the equivalent impedances as determined by the method just described is also obtained from the oscillograms by use of the power circle diagram of the interconnection Figure 13. The circle $|P_s|$ represents the locus of the kilovolt-amperes $(EI\sqrt{3})$ existing at the same time and location during test 2 as is represented by circle Z_s in Figure 12. The power circle P_s is for the particular values of E_s and E_r existing at the same time. Intersection of circle $|P_s|$ with circle P_s represents the power flow existing at that instant. The angle δ_{rs} on this diagram is the angle in degrees by which the Kansas bus voltage leads the Nebraska bus voltage. Transferring this angle, which is also the line-voltage angle, to the impedance circle diagram, Figure 12, locates point *a*. This gives a close check on the location of the impedance calculated and plotted as point *A* discussed above.

Table IV

	Computed From Test Data	From Source 1	From Source 2
Resistance (ohms per mile)	0.254 at test temperature	0.2335 at 20 C	0.238 at 25 C
Reactance (ohms per mile)	$+j0.823$	$+j0.8027$	$+j0.8032$
Susceptance (micromhos per mile)	$+j5.14$	$+j5.22$	$+j5.22$

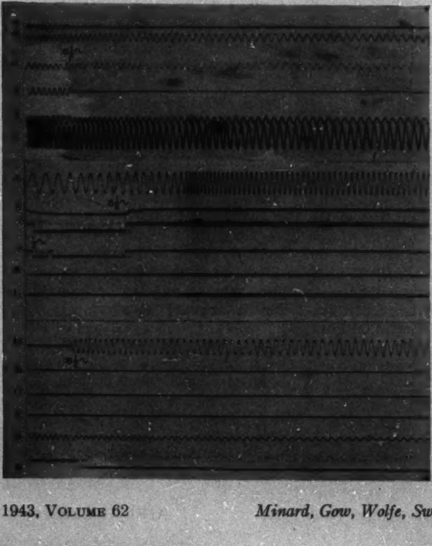


Figure 8. Oscillograms from test 5

Two-wire-to-ground fault on 132-kv system in Kansas with 34,000 kw received in Nebraska. For oscillograph legend see Figure 4

In similar manner points *b* to *d* inclusive give reasonably close check to the respective apparent impedances *B* to *D* inclusive in Figure 12.

The preceding method of checking and other results of the staged fault tests showed that the terminal relays, specially designed for protecting the long transmission line, operated satisfactorily, performed according to specifications, and followed theoretical predictions which were based on transmission-line electrical constants calculated from line dimensions and on estimated transformer impedances. Thus a gratifying confirmation has been obtained of the impedance-circle-diagram method of specifying long-line relay performance of the analyses presented in reference 1, and also of the design of the relays presented in reference 2.

Relating Test Results to Switching-Time Stability Curves

Curve *M-N* of Figure 14 indicates the approximate border line between transient stability and instability of this interconnection, as affected by the relation between power flow over the interconnection at instant of fault and the total switching time including reclosing time. The region below and to the left of the curve indicates stability. The region above and to the right indicates instability. The theory of this curve and its significance have already been presented before this Institute.[4]

The points *A* to *D* inclusive represent results obtained in the staged fault tests of group II. Points *E* to *H* inclusive are from network-analyzer studies made prior to the construction of the interconnection.

It will be noted that the results of tests are in reasonably good agreement with the theoretical values represented by the curve *M-N* and also with the values predicted from the analyzer studies.

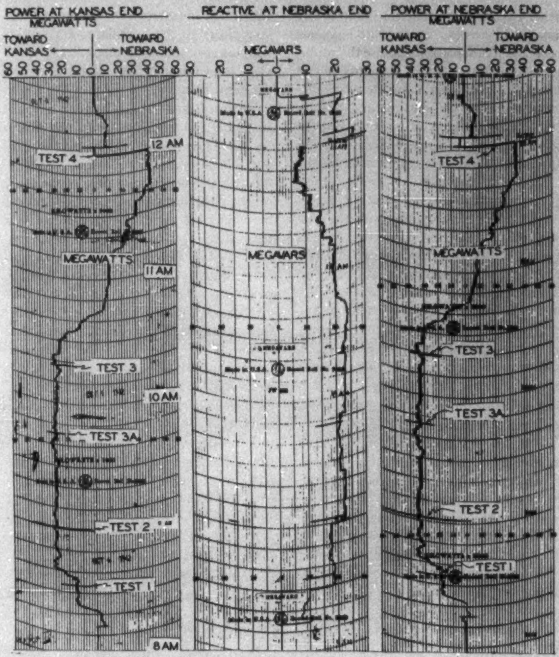


Figure 9. Charts of power flow on 154-kv interconnection during tests 1 to 4, inclusive

Unbalanced Ground Currents

The oscillograms in Figures 4 to 7 inclusive show a comparatively high harmonic ground current flowing in the tertiary winding and in the 154-kv neutral when the interconnection is energized from one end, as shown by traces *A* and *D*. Because of these harmonic currents, it was necessary to change the instantaneous elements of the ground directional relays over to the 154-kv neutral current transformers from their original connections in the tertiary current transformers. By careful study of ground currents in the tertiary and in the 154-kv neutral for ground faults both on the 154-kv interconnection and on the terminal systems, a nice balance of minimum ground-fault currents, observed harmonic currents, and proper relay settings was obtained, as demonstrated by satisfactory operation of these relays during the staged fault tests.

Interconnected Operation During Tests

The tests directly involved co-ordinated operation of five important power systems in four states, including operation of normal generation, providing spinning reserve, and the operation of tie lines to absorb power swings following the applications of the faults. The Nebraska Power Company system was interconnected as usual with the Nebraska Public Power System through a 25,000-kva transformer bank, and the 60-kv system of the Kansas Gas and Electric Company was connected to the 66-kv system of the Oklahoma Gas and Electric Company and thus to the interconnected systems of the South through a 10,000-kva autotransformer. The Kansas system was also interconnected with the Empire District Electric Company through a 66-kv line. Except in test 4, these interconnections remained closed during the tests demonstrating their value and performance in major system disturbances.

About ten days before group II tests were made, a meeting was held, attended by system operators and operating department heads of the systems most directly involved. At this meeting the test program and basic schedules for tie-line and generator loadings were placed in memorandum form and mutually approved. Each system operator, following the memorandum outline, undertook the detailed scheduling of generation and rate of change of load on generators within his own system and, by mutual agreement with operators of neighboring systems, scheduled the power flows in the tie lines and the necessary changes in these loads for each of the tests.

To facilitate the progress of the tests, two supervisors, one stationed at each terminal of the 154-kv line, were in direct charge of the tests. These men were in constant communication with each other and with the system operators of the Kansas and Nebraska systems, who in turn were in communication with the operators of neighboring systems. In effect the system operating authority for the interconnection was transferred temporarily to the two terminals of the interconnections with the responsibility of proper and safe maneuvering of men and equipment definitely placed in the hands of the two supervisors. In this manner no reduction in safety was introduced, but a saving in time was realized. These two supervisors directed the principal work of posting observers, checked oscillograph operation, collected and made records of observations, supervised rigging of fault equipment, gave signals for initiating faults, and, in general, mutually co-ordinated the work of system operators and test men at their respective ends of the interconnection.

Types of Faults and Their Initiation

Most of the faults applied to the 154-kv line and to lines radiating from the terminal substations were two-wire-to-ground arcing faults. This type of fault was chosen, because it imposes a comparatively severe test on the systems from the standpoint of transient stability and is also a type of fault which occasionally occurs. Since most faults of this type are initiated by lightning, followed by power current, it was desirable to simulate arcing-fault conditions created by lightning as nearly as practicable during the tests.

Unpublished results of experiments cited by General Electric Company engineers indicate that an arc started by fusing fine iron wire has characteristics quite similar to an arc initiated by ionization of

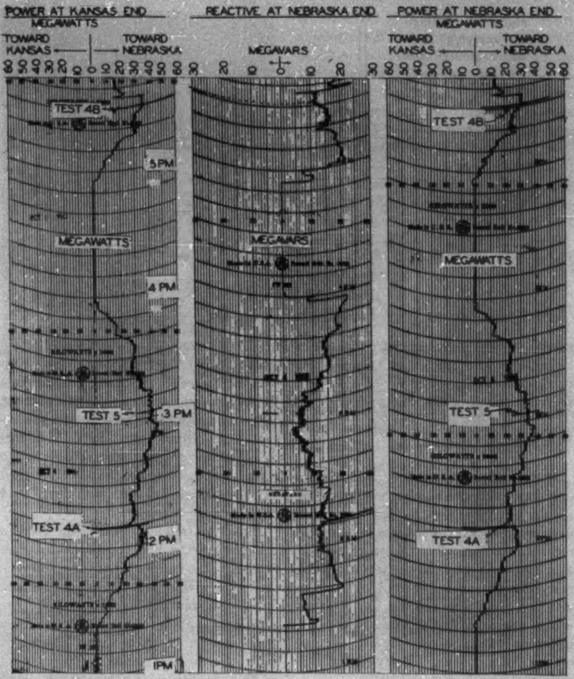


Figure 10. Charts of power flow on 154-kv interconnection during tests 4A to 5, inclusive

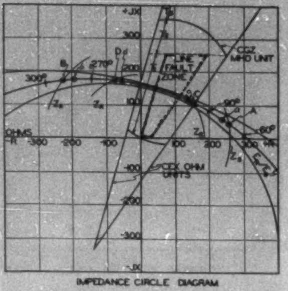


Figure 12. Impedance circle diagram with relay operating zones

Impedance values reached during power swings on tests, as computed from oscillograms, are shown at *Aa*, *Bb*, *Cc*, and *Dd*

air at high potential. Since it was desirable to keep to a minimum the amount of vaporized metal and to provide sufficient mechanical strength to maintain the fault equipment in place during the interval between rigging and fault occurrence, number 32 iron wire was used between protective jumpers connected to the conductors and ground wires on the structures.

Additional switching equipment by which staged faults could be initiated was not available on the 154-kv line. Consequently, the arrangements of iron wire, insulated cords and ropes, and weights shown in Figures 15 and 16 were devised to initiate an arc simulating those started under actual fault conditions. This equipment was installed during shutdowns preceding the tests. After all of it was in place, the line was re-energized, the systems were synchronized, line power flow and voltages were adjusted, oscillographs and instruments were placed in readiness, and observers were stationed at their respective stations. At prearranged signals which were given by the supervisors and which were also transmitted over the regular carrier-current telephone to the opposite end of the line, the fault was established by a man pulling on a long rope extended to a safe distance from the arc location. It should be noted that, out of the nine two-wire-to-ground faults applied to the 154-kv line by this method, examination of the oscillograms reveals that, in eight cases, arcs were made to the two wires within less than 2 cycles of each other and, in one case, within $4^1/_2$ cycles.

Two one-wire-to-ground faults were also applied to the line by these methods, making a total of 20 arc paths established in 11 tests. In each of these the initial trip-out was followed by successful instantaneous automatic reclosure. No restriking took place in any of these cases, although the line was de-energized for a maximum period of only 13 cycles in test 4 and for as short a time as three cycles in test 4*A*. Deionization was complete in each case, although wind velocities were very low and there was almost no attenuation of the arc path. In Ne-

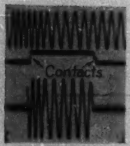


Figure 11. Oscillogram of carrier-current control circuit contact operation

Note the "step" which indicates sequential operation of the directional and overcurrent contacts of the *GFC* ground relay

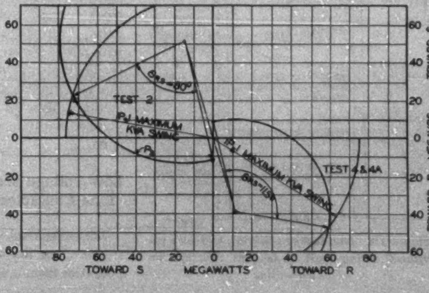


Figure 13. Power-circle diagram illustrating step in computation of angle between terminal bus voltages, δ_{rs}, for location of impedance values, *a*, *b*, *c*, and *d* on Figure 12

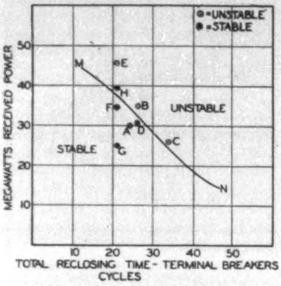


Figure 14. Power-switching time curve of approximate transient-stability limits

Points *A*, *B*, *C*, and *D* are from staged fault tests, and *E*, *F*, *G*, and *H* are from network-analyzer studies

braska the wind velocity during both series of tests was estimated to be from one to two miles per hour, and in a few of the tests the air was perfectly still. In Kansas there was a slight breeze during most of the tests, and in test 4*A* in which the line was de-energized for only three cycles, the wind velocity was two to five miles per hour.

Most of the arcs observed in Nebraska had the appearance of a round white flame approximately two feet in diameter as shown in Figure 17. Figure 18 shows a staged two-wire-to-ground fault in Kansas in which a slight breeze blew the arc sideways.

For faults applied on the 60-kv, 66-kv, and 132-kv systems near the terminals of the interconnection, circuit breakers on isolated radial circuits were closed on previously prepared fault circuits, and the resulting faults were cleared immediately by the same breaker and its protective relays. These fault circuits consisted of either fine wire connected between two conductors and ground or of a ground switch with one of the blades open.

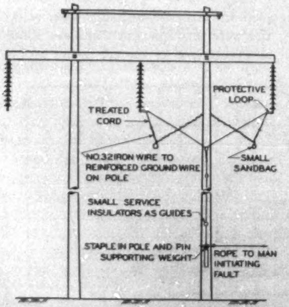
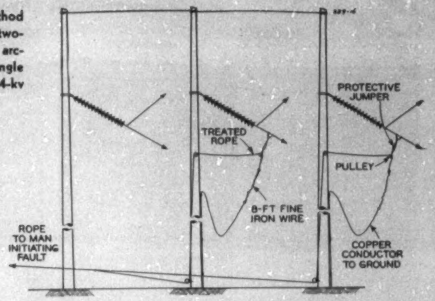

TREATED CORD
PROTECTIVE LOOP
NO. 32 IRON WIRE TO REINFORCED GROUND WIRE ON POLE
SMALL SANDBAG
SMALL SERVICE INSULATORS AS GUIDES
STAPLE IN POLE AND PIN SUPPORTING WEIGHT
ROPE TO MAN INITIATING FAULT

Figure 15. Method of applying two-wire-to-ground arcing fault at tangent structure on 154-kv line

Figure 16. Method of applying two-wire-to-ground arcing fault at angle structure on 154-kv line



Methods of Observation

Power plant, substation, and system operators, and special observers assisting regular operators were notified when each fault was to occur. Each was expected to record any observable effects at his station. At each terminal the action of each relay element on the faulted phases was watched and recorded by an observer whose sole duty at the moment of fault initiation was to watch only one element of one relay. These observations were recorded after each test.

Some discrepancies were noted between the observers' reports of action of some of the high-speed relay elements and action recorded by oscillographs and targets. These experiences demonstrated the importance of high-speed recorders, such as oscillographs, in tests of this character and the limitations of visual observation of high-speed relay action.

During the 14 tests of group I with no power transfer over the line, one six-element automatic oscillograph was in operation at each of the terminals, making a total of 12 elements connected to principal current, voltage, and control circuits. During the eight tests of group II with power transfer over the line, a second oscillograph was in operation at the Nebraska terminal, giving a total of 18 elements connected to principal current, voltage, control, and carrier-current circuits.

Low-Voltage Measurements of Line Characteristics

Measurements of the electrical constants of the line were made by applying potential from 2,400/4,160-volt wye-connected 60-cycle source supplied from 150-kva 13,800–2,400/4,160-volt wye-connected transformer bank in Nebraska with the conductors at the Kansas end appropriately connected for various short- and open-circuit tests. Portable ammeters, voltmeters, and wattmeters of good quality and recent calibration were used with outdoor instrument-type current and potential transformers.

Exact long-line formulas were used in calculating the line constants and unit values. It is believed that the results are accurate within plus or minus one per cent. Summarized in Table II are the pertinent values computed from these tests compared with values calculated from the line dimensions.

Data in Table III compare calculated values of the *A*, *B*, *C*, *D* constants with those computed from data taken during the 2,400/4,160-volt tests. Table IV gives a comparison between unit values per mile of resistance, inductive reactance, and susceptance computed from the test

Table V

Test		
A	Voltage rise—1.18	Three-phase voltage applied. Conductors open at far end
B	Current rise—1.2	Three-phase voltage applied to three conductors short-circuited and grounded at far end
C	Voltage rise—1.36	Single-phase-to-ground voltage applied to three conductors paralleled at both ends but not grounded at far end
D	Current rise—1.4	Single-phase-to-ground voltage applied to three conductors paralleled and grounded at far end
E	Voltage rise—1.26	Single-phase-to-ground voltage applied to one conductor with far end open. Other conductors open at both ends
F	Current rise—1.34	Single-phase-to-ground voltage applied to one conductor with far end grounded. Other conductors open at both ends

Figure 17. Staged two-wire-to-ground fault on tangent structure of 154-kv line

data and as obtained from two sources. Ratios of receiving to sending voltages and currents of interest are given in Table V. If data from two of the tests in Table V are used, the ratio of the zero-sequence short-circuit impedance to the positive-sequence short-circuit impedance is 2.88.

All of the measurements include the impedance of carrier-current wave traps at both ends of the line which were in service as part of the communication system used throughout the tests. The 60-cycle impedance of about 0.2 ohm of the traps was considered too small to be included in the calculations. Since sag and tension add only about one half of one per cent to the actual length of the conductors over surveyed line length, this factor was not taken into consideration.

Atmospheric temperature during the five or six hours of testing varied between 75 to 90 degrees Fahrenheit with perhaps 85 degrees Fahrenheit as an average value.

With no potential applied, there was no indication of induced voltage on the line when measured by potential transformers and voltmeters. During the attenuation tests at carrier frequencies made at a later date, a two- or three-volt potential was observed between conductor *A* and ground using a vacuum-tube voltmeter shunted by 550-ohm resistor, indicating that any induced voltage effects were small. During one test in which 2,400 volts to ground were applied to the *A* conductor only, the potential between conductors *B* and *C* and ground measured at far end was 300 volts. As indicated by the values of data taken, there is no appreciable unbalance of reactance among the three conductors. Three impedance values of 257.5, 257.5, and 258 ohms line-to-neutral were computed, using the three-phase values taken during one test.

The phase rotation at the two ends was compared by using a phase-sequence instrument and was found to check the transposition plan. This was further confirmed when the systems were synchronized for the first time.

Carrier-Current Attenuation Tests

The *A* phase conductor was completely isolated from the carrier-current capacitors and line traps and from all other connections to the systems. In Kansas between *A* phase conductor and ground a 550-ohm resistance was connected, corresponding to the 550 ohms characteristic impedance for one conductor to ground as computed from the 2,400-volt tests. Across this resistor was connected a vacuum-tube voltmeter having a range of zero to ten volts.

The oscillator of the carrier-current relay set in Nebraska was connected to the *A* phase conductor through the concentric cable and impedance-matching transformer. The carrier coupling capacitor and tuning inductance of the carrier-current coupling equipment were disconnected and by-passed. A voltage divider of approximately 100,000 ohms resistance was connected between *A* phase conductor and ground to measure the input voltage to the line at carrier frequency. A calibrated vacuum-tube voltmeter was connected to a measured tap on this divider. The carrier-relay oscillator was tuned to a known frequency by comparison with a calibrated signal generator. The voltages at the sending and receiving ends were measured by means of the vacuum-tube voltmeters. This procedure was followed at steps of five kilocycles from 35 to about 59 kilocycles.

The tests indicated ratios of receiving-to sending-end voltage between 0.064 and 0.086 in the range of frequencies covered. During the measurements at the Kansas end, it was observed that the vacuum-tube-voltmeter reading would drift periodically through a range of about one volt above and below its mean value, each cycle of drift requiring several seconds of time, indicating that there was a periodic change of potential on the line as a result, possibly, of the movement of the earth's magnetic field or the presence of stray voltages. The sending-end voltage was on the order of 105 to 112 volts and receiving-end voltage five to seven or eight to ten depending on frequency and on the induced-voltage effects.

Figure 18. Staged two-wire-to-ground fault on angle structure of 154-kv line

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Kilowatts, Kilovars, and System Investments

Discussion and author's closure of paper 43-25 by J. W. Butler, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, March section, pages 133–7.

E. E. George (Southwest Power Pool, Little Rock, Ark.): This paper contains much interesting and useful material on the use of capacitors in improving system operation, but the economic comparisons involving the cost of generating equipment are based upon so many incorrect assumptions as to be almost useless.

It is known, of course, that reactive kilovolt amperes supplied at the load may be twice as effective as reactive kilovolt amperes supplied at a generating source, particularly in the case of transmission systems.

The only generating-equipment cost properly chargeable to the supply of reactive kilovolt amperes is the incremental cost of a low-power-factor generator over a high-power generator. This cost runs from one to three dollars per kilovar.

Even if multiplied by two, the cost of supplying kilovars in a low-power-factor generator (when purchasing new equipment) is materially less than for synchronous condensers or for static capacitors.

S. B. Crary (General Electric Company, Schenectady, N. Y.): Mr. Butler has presented a method by which the amount of system investment chargeable to supplying the kilovar requirement may be determined. He has approached the problem from a fundamental point of view, determining the amount of system capacity required when, (1) voltage drop, (2) ampere capacity, and (3) generator equipment, are the limiting factors and then combining them in their proportionate amount to obtain an over-all requirement for the system. To illustrate the method, Mr. Butler has assumed 0.8-power-factor generators that could be operated at unity power factor rated kilovolt amperes at peak system loads. If this is possible in a system, a large saving in system capacity can be realized by supplying the kilovars at the load instead of at the generators. It would be unfortunate if his example were to lead engineers to believe that they should attempt to supply *all* of the reactive kilovolt amperes at the load and that they could use the full kilovolt-ampere rating of the generators right up to *unity power factor*. Pursuing this course still further, one *might* be led to believe that the generating stations should be designed to operate at unity power factor full kilovolt amperes. Although Mr. Butler did not intend that his results should be extrapolated to this extent, some discussion may be in order as to why such a procedure cannot be generally followed.

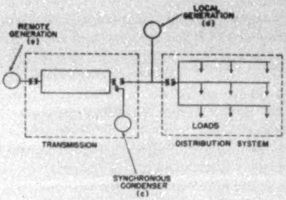

Figure 1

For purposes of economy the *average* transmission-line loadings of a system will be operated somewhere near unity power factor. For the system of Figure 1 of this discussion this will require the correction of the load power factor at the receiving end of the transmission system and would be accomplished by the combined use of

(*a*). Unswitched shunt capacitors.

(*b*). Switched shunt capacitors.

(*c*). Synchronous condenser capacity.

(*d*). Local generating capacity.

The determination of the best amount of each will depend upon the system characteristics and will naturally be based on economics and operation. It has been well established that the use of capacitors in the distribution area is economical.

At peak load within the distribution area (see Figure 1), the required transmitted load may increase, and kilovars will have to be supplied both at the sending and receiving ends of the line for the transmission of power; that is, (*c*) and (*d*) will be required to carry more reactive kilovolt amperes and so will (*e*), the group of generators at the sending end of the line.

If a fault should occur to trip out one of the parallel transmission lines, then the required reactive kilovolt amperes at both ends would be increased, particularly if the outage occurred at peak system loading. Accordingly, there must exist a reserve capacity of reactive kilovolt amperes both at the sending end and the receiving end of the major transmission links to provide for peak and emergency loading conditions. For a system which has a large proportion of transmission compared with the distribution this reserve kilovar capacity may be an appreciable part of the total reactive kilovolt-ampere requirement of the system. This is particularly true when the transmission system is used to transmit power in both directions. For such systems the generator power factor must be lower necessarily than unity at peak power and particularly during a system emergency. This reserve kilovar capacity for a system having an appreciable amount of transmission can be best supplied by synchronous condensers and generators.

It can be further reasoned that if a system requires synchronous kilovar capacity to meet the varying transmission-line needs, some of this capacity could be reasonably allocated to correcting the load power factor, since lagging capacity and smooth control of voltage are necessary for best operation of the transmission system. Also use of the diversity of kilovar demand in the distribution-system burden may be made best at the synchronous condenser location, Figure 1 of this discussion. Accordingly, systems which have large and important transmission-line links may have an appreciable amount of synchronous condenser and generator capacity devoted to the supply of kilovars.

It may be concluded that the shunt capacitor finds its greatest field of application in the distribution area for correcting the power factor of this part of the system while the varying needs of the transmission system require at the present time synchronous kilovar capacity. With reduction in the cost of shunt capacitors, it will naturally follow that more switched capacitors will be used to help meet the varying kilovar system demand. The amount which can be justifiably used is again a question of economics and operation. The evaluation of the amount of system capacity required for the transmission of kilovars to the load must necessarily recognize the system requirement for reserve kilovar capacity, and recognize that this reserve may be best stored in the synchronous machines.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): The author makes a strong case for the widespread use of static capacitors in conserving system investment. Unquestionably there is an urgent need for focusing attention on the over-all system kilovar requirements and the most feasible method of supplying them. The writer agrees that in some cases the installation of sources of kilovar capacity near the loads, such as synchronous condensers or the judicious use of static capacitors may quite likely be indicated. However, certain cautions are offered with the hope that they may be of use in keeping the conclusions reached within the realm of credibility.

The cost of equipment covered by the term "generation" is one of the larger items considered in system-investment analysis, and if the conclusions of the author are followed a new plant could have the size of its boilers reduced by the installation of distribution capacitors. His viewpoint would also show a very attractive profit for the installation of banks of static capacitors at the generator terminals large enough to improve the generator power factor to unity.

While it is true that many generators are now being operated at power factors higher than their purchased rating they still may be used to carry sizable kilovar loads. In the example cited by the author (see reference 1 of the paper) most of the machines which had their kilowatt outputs increased by operating at higher power factors are still being used at 90 per cent power factor, so that, for example, the 90,000-kw generator can still supply some 44,000 kilovars without exceeding rated stator current. Tests on recent machines with name-plate ratings of 80 per cent power factor have shown that continuous kilowatt loads equal to the kilovolt-ampere rating may be carried with kilovar loads of about half this amount without exceeding rated field amperes and with such small increase in stator losses (at theoretical stator overload) that the temperature rise is well within desirable limits. It has been found that the kilovar ability of a machine so operating may be further increased some 25 per cent by the addition of higher pressure hydrogen cooling at a cost of roughly one dollar per kilovar. Thus, as far as generating equipment is concerned, the cost of incremental kilovars varies widely with the base operating power factor. In any event, the cost of the entire production plant should not be charged against kilovar capacity which originates solely within the generator, this equipment being a small portion of the total plant cost.

Care must be taken to discriminate between power factor at the load and at the generator terminals. In a typical system studied, the effect of "kilovar loss" combined with transformer magnetizing current was such (even after taking account of the constant kilovar capacity provided by transmission-line charging current) that a peak-load power factor at the points of delivery between the transmission and subtransmission systems of 90 per cent required a power factor at the generator terminals of 82 per cent. Analysis of kilovar requirements at peak load indicates that magnetizing current and "kilovar loss" in the generator step-up transformer may amount to 15 to 20 per cent of the generated kilowatts. "Kilovar loss" in the transmission lines may account for another 15 per cent which, together with the "kilovar loss" and magnetizing current of the transformers delivering power to the subtransmission system, makes a sizable amount of kilovar capacity which may be very logically supplied from the generators.

In discussing the improvements brought about by capacitors, voltage drop must not be loosely confused with voltage regulation and voltage control. The author very correctly points out that rates which encourage customers to install kilovar capacity may take the control of such capacity out of the hands of the utility where it really belongs for the best interests of all concerned. Then for consistency, only those capacitor installations which are adequately switched should be considered as reducing the investment in equipment where "voltage drop" is taken as the limit.

Some of the investment considered by the author as limited by "voltage drop" may not be justified. Following the reasoning of the author, the increase in size of an overhead transmission conductor (which decreases the R/X ratio) is done for the purpose of reducing that portion of the voltage drop in the line caused by the flow of kilowatts. Actually the larger size copper may have been specified to reduce losses so that even the transmission line may be considered as "ampere-limited" equipment. Here again, the cost of transmitting incremental kilovars is markedly influenced by the beginning power factor. Also, several items of transmission-line expense could not be materially reduced by improving transmission power factor, and similarly, much equipment in the transmission and distribution plant such as circuit breakers, lightning arresters, busses, disconnecting switches, and so forth, are little affected in cost by moderate changes in ampere or voltage requirements. These should not be included in the total investment considered.

While the transmission of kilovar loads affects voltage drop directly, this cannot necessarily be translated directly into terms of transmission and distribution system investment. For example, it is commonly found desirable to supply distribution loads from trunk feeders of number 4/0 copper or larger and to utilize feeder voltage regulators to compensate for changing kilovar loads, thus the distribution-feeder limit becomes one of amperes rather than voltage drop. Similarly, daily schedules of negative regulation at generator terminals may be utilized to compensate for changing kilovar loads carried through the transmission system. In this connection, the possible use of tap changing underload equipment should not be ignored.

With an exercise of caution in interpreting the conclusions reached, this paper should assist in determining the best basis for sound engineering studies. It is believed that it does a distinct service in focusing attention on the desirability of separate analysis of system kilowatt and kilovar requirements.

J. H. Foote (The Commonwealth and Southern Corporation, Jackson, Mich.): Mr. Butler's paper, "Kilowatts, Kilovars and System Investments," gives the unfortunate impression that the plant investments of power-supply companies could have been greatly reduced in the past if greater use had been made of capacitors. It is also implied that since generating plants have short-time or emergency capabilities of 125 per cent nominal rating, they could be scheduled regularly to be operated at these overloads, thus releasing kilowatt ability if capacitors were used. A more intimate knowledge of design and operation of power systems would show that this margin is essential for satisfactory and reliable operation, especially under normal conditions. Normal fluctuations in load and spinning reserve requirements make it necessary that the turbine and steam-generating equipment be capable of carrying short-time loads in excess of their continuous ratings. For a co-ordinated plant design the 125 per cent rating referred to by the author should not be considered a continuous rating and therefore should not be taken as limiting the kilovars available from the generator to a negligible amount.

In the normal development of a power-supply system it is always necessary to provide some duplication of facilities in order to obtain satisfactory reliability of service. It has been possible therefore to supply substantial quantities of kilovars from generating plants without excessive voltage drop or energy losses, and under emergency conditions with lines or equipment out of service, the quality of service has been commercially satisfactory. In some cases a careful study may show the desirability of supplying more of the kilovars at points nearer the load than the original design contemplated; however, the application of capacitors for this purpose is limited to a small fraction of that which might be inferred from the author's conclusions.

B. M. Jones (Duquesne Light Company, Pittsburgh, Pa.): The power factor of the Duquesne Light load in Pittsburgh is about 87 per cent, and further improvement will have to be handled rather carefully because we now operate some of our generators above 90 per cent power factor at times, and the matter of generator stability may become a problem. I would like to echo a word of caution on this.

The point of letting the installation of the condensers and corrective equipment, particularly the smaller ones, get out of the control of the utility company struck me as very important. One certainly is heading for trouble when this happens, as the one or two instances I will cite herein will show, and these can be multiplied many times if the condenser installation becomes the rule rather than the exception on a utility system.

In connection with the installation of smaller size capacitor units adjacent to industrial loads, we have encountered several adverse conditions, such as introducing a harmonic under light load periods, which causes the fuses to blow. In one case fuses were blowing in a small industrial plant supplied from the four-kilovolt system through a bank. This happened after the plant shut down at noon on Saturday as the workmen shut off motor after motor and turned off the lights. After the main supply fuses would blow, the whole plant would be out. We made an investigation with an oscillograph and instruments, and found a fifth harmonic, very distinct, which was causing the fuses to blow. This was solved by disconnecting one half of the condenser bank just before light load periods.

In other cases we have found that the condensers on the customer's property have increased the voltage at light load periods to values which caused excessive lamp burnouts.

E. J. Boland (General Electric Company, West Lynn, Mass.): While this paper concerns itself principally with system economics and how the required electric services (active and reactive) can be provided in the most efficient manner, in the final analysis the utility-meter engineers and the meter manufacturers may also be concerned.

The benefits from released capacity, reduced losses and reduced voltage drops within a power customer's distribution systems should often be sufficient to encourage the installation of capacitors. However, for some years to come it is not improbable that the meter manufacturers may be called upon to provide metering equipment by means of which the cost of the reactive requirements can be factored into a particular schedule.

When analyzing the general requirements for such metering equipment certain problems become apparent. They are not those of providing measuring devices as such. Kilovolt-ampere and kilovar-demand meters have been available, and have been used for many years. Rather, one of the problems seems to be a matter of first cost and maintenance in relation to the expected revenue return associated with the factoring of small consumers' reactive requirements. The relatively few consumers in the medium and large power-requirement classes probably need not be considered. For these, it is reasonable to assume that indicating kilovar-demand meters will be adequate. Their first cost and maintenance in comparison with printing or recording kilowatt-demand meters should be reasonably in line with the revenue returns, based on present day metering standards.

Present indications are that by far the major part of the total cost associated with the necessity of providing kilovars must be allocated against smaller consumers. Numerically, they may comprise more than 90 per cent of all consumers on a system. Thus, proportionate allocations of the cost from kilovar requirements would point in the direction of attempting to factor it in the schedule applicable to these smaller consumers. This will present problems. It is quite evident from Mr. Butler's paper that the cost for kilovar-demand metering must be a good deal smaller than for kilowatt-demand metering. From a design standpoint, there is now little reason to believe that this can be achieved. If anything, the kilovar-demand meter is more costly to provide than the kilowatt-demand meter.

There is, however, one solution; namely, the so-called active-reactive demand meter, first discussed some 30 years ago by Arno and revived spasmodically since then; (for example, Charles P. Steinmetz, Association of Edison Illuminating Companies, Report of Committee on Power Factor, 1921). The advantage of the active-reactive demand meter is that it can be produced at substantially the same cost as present kilowatt-demand meters. In operation, it functions to add arithmetically to the kilowatt demand a predetermined percentage of the kilovar demand; that is, the indication of the device is kilowatts plus c (kilovars) where c is the predetermined ratio of the unit kilovar to kilowatt charges.

It is not suggested that the active-reactive demand meter is the ultimate solution to the problem; it is referred to simply because it is a measuring device already available (and used extensively by at least one utility), and because it fundamentally falls in line with the basic considerations that the metering cost of factoring the reactive requirements must of necessity be small.

In making plans for the future, the meter engineer must consider trends. The cost per reactive kilovolt ampere of capacitors, or the equivalent, has been markedly reduced during the last ten years or so and, if the past is to be any criterion, the cost-reduction trend may continue. Thus, the relative importance of measuring the reactive component in a demand schedule may be less and less. The additional expenses chargeable to installation and maintenance of metering equipment for the sole purpose of measuring the reactive component must be weighed against the cost increase in system investments directly chargeable to consumers' requirements for reactive power. In future years, it may be considered a better over-all policy to invest more in equipment to produce reactive power and less in metering equipment by means of which charges can be allocated for the reactive power consumed.

It would also appear to be a reasonable requirement that the active-reactive meter should be provided with a relatively simple adjustment by means of which the constant c referred to previously can be modified, from time to time, to be made to conform to the changes in the schedule. If, ultimately, it is decided to disregard entirely the reactive factor, it should be a simple matter to convert the active-reactive demand meter to a standard kilowatt-demand meter, thus circumventing the risk of investing in metering equipment which may not be utilized throughout its normal useful life.

H. K. Sels (Public Service Electric and Gas Company, Newark, N. J.): In attempting to allocate equitably the total system investment between kilowatts and kilovars Mr. Butler has ventured into new ground for the Institute. I think that the conception of separate kilowatt and kilovar supply is an excellent one to apply in the engineering development of an electric system. However, I am not so sure that it is the most practicable conception in economic studies of system investment upon which electric rates may be established as can be readily demonstrated.

In the first place, the random distribution of poor power-factor customers throughout the system is usually limited to certain industrial areas. Therefore, since most systems essentially are laid out with radial distribution from scattered bulk supply points, individual elements of the system are affected to a different degree and many of them not at all in certain years when plant investment has been made in anticipation of future loads and to provide adequate reserves. I feel that Mr. Butler has oversimplified the problem in a rather academic treatment and that some of his assumptions determine in advance the conclusions reached. For example, the general principle is followed that the division of system investment between kilowatts and kilovars should be on the basis of the kilowatt capacity used or proportionate voltage drop produced by each. Why not go to the other extreme and consider only those incremental investments added to certain portions of the system to carry the kilovar load for only the time that they are absolutely needed for that purpose? Considering the fact that many factors enter into the decision of when to add new elements to a system, the amounts charged to the supply of kilovars on this basis would be small indeed and the conclusions reached vastly different.

I have given much study to the effect of low-power-factor loads upon system investment and the various methods of supplying kilovar load with the best over-all economy. Mr. Butler concludes that 20 to 25 per cent of the system investment is chargeable to the kilovar requirement when it is supplied by generating stations operating around 80 per cent power factor. In analyzing the cost of specific cases I never have been able to demonstrate so high a figure but have found that the most expensive method of kilovar supply probably does not run over ten per cent of system investment at 80 per cent power factor when following the basis that I have outlined. Therefore, I feel that the ordinates of Figure 5 of the paper should be less than half the values given, while the ordinates of Figure 6 of the paper are about right. It is evident from this that only the poorest power-factor loads should be corrected by capacitors, and generators should not be operated above 90 per cent power factor. This is a point to be considered in correlating the capacities of generators and turbines.

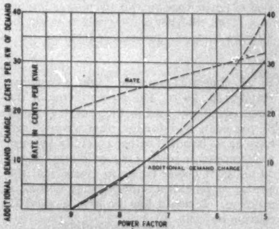

Figure 2. Comparison of rates based on kilovar and kilovolt-ampere metering

In giving further consideration to the various methods of supplying kilovars it is neither feasible nor desirable to concentrate their supply in capacitors alone because they would become unmanageable in system operation, particularly when they are installed quite generally by the customer. On an incremental investment basis the building of additional kilovar supply into the design of a generator is the cheapest method of all and will support several dollars of incremental distribution-system investment to carry this supply to the load. Synchronous condensers, with their desirable characteristics of holding up system voltages during disturbances or fluctuating load, are a method not to be disregarded in providing controllable kilovar supply, particularly when building space is available, and their losses can contribute toward building heating. Therefore, it appears to me that all of the cost elements although lower in value are as complex in arriving at the proper kilovar supply as they are for kilowatt supply and cannot be generalized simply.

Finally, Mr. Butler goes so far as to advocate the abandonment of the terms *power factor* and *kilovolt ampere*. I agree on *power factor*, but I feel that some things can be accomplished with *kilovolt-ampere* metering that cannot be accomplished with kilovar metering. For example, without holding any brief for either rate, take the rate clause proposed by the Connecticut Commission of charging 25 cents per kilovar in excess of 50 per cent of the kilowatt demand and compare it with another equivalent form of rate based on charging 45 cents per kilovolt ampere in excess of 112 per cent of the kilowatt demand as shown in Figure 2 of this discussion. It is evident that the kilovolt-ampere rate accomplishes more than the kilovar rate. First, it has the advantage of continuing the use of well-known terms, particularly with lay-minded customers, and provides an increasing incentive measured in cents per kilovar to lower-power-factor customers to correct at the load. Second, if the proper rate is selected, it reserves to the utility company the field of kilovar supply where it can best meet the needs of the industry. Admittedly, kilovar capacity should be provided at a lower cost by the utility than by individual customers as the capacity provided by the utility can be made useful to more than one customer, and, where the requirements of one locality change with respect to another, can be moved about the system as local conditions require. Therefore, I can only agree that a demand charge based on kilovolt amperes alone may be "somewhat inequitable," and nothing is wrong with kilovolt-ampere metering when properly used.

I should like to correct the impression given in a quotation used by Mr. Butler from a similar paper which Mr. Seely and I presented last year, that 20 per cent additional turbine-generator capacity was obtained by unity-power-factor operation of generators. Most of our generators will not deliver full kilovolt amperes at unity power factor as Mr. Butler assumes. Only nine per cent of additional capacity was due to increases in power-factor ratings from 0.8 to 0.9 using other sources of kilovar supply. The remaining 11 per cent was obtained from increased kilovolt-ampere ratings resulting from various steps outlined in the paper. Quoting further from the same paper, computations showed for the Public Service Electric and Gas Company system that:

1. "The static capacitor installations are the most economical because they are close to the load.
2. "The small added cost of providing reactive capacity in generators is next least expensive even with the added system investment and no saving in losses.
3. "Adding capacity in the form of synchronous condensers wherever located is the most expensive of all due to their higher initial cost."

Therefore it appears that while our approach is different we are in fairly close agreement on the method of kilovar supply with certain qualifications, but quantitatively we are not in agreement as to the method of analysis and the establishment of costs. Also, we are not in agreement with the method of metering that most closely represents the cost of service.

J. W. Butler: There seems to be a general criticism to the effect that the conclusions are invalidated by the somewhat sweeping assumptions adopted, particularly assumption number 2. This aspect will be covered first by a general discussion, then the remaining individual points answered.

Assumptions were made to establish a basis for a broad analysis to determine the relative costs of producing kilovars in different ways on an average power system, as well as establishing as a bench mark, the relation between these costs and the costs of producing kilowatts. The analysis was made on the basis of exploring a range in conditions of system operation from one extreme to another, namely,

1. Load kilovars all supplied by generators and prime movers able to deliver full kilowatt output at 1.0 power factor.
2. Load kilovars all supplied by auxiliary kilovar generators near the load.

In doing this it was appreciated that no system ever operated in either manner, but these cases did describe limits which encompassed all systems. Hence the results indicated the range in which we were working and the scope of the possibilities in either direction.

Following this general approach, the method was numerically applied to three specific radically different systems, designated as *A*, *B* and *C*.

Thus all types of systems were reasonably included somewhere in the area covered and the ratios between the fraction of the total system investment chargeable to kilovars when supplied by prime mover units, Figure 5 of the paper, as compared with the fraction of system investment chargeable to kilovars when supplied from equipment such as capacitors near the load, Figure 6 of the paper, were so large in any case that it adequately established the advisability of carefully checking the practice in any particular case of supplying kilovars from prime movers that had available capacity for more kilowatt output if the kilovar load were reduced. As an example, if the magnitude of the effect of assuming that only 50 per cent of generating equipment is capable of operating at full kilovolt-ampere unity power factor output instead of 100 per cent, as assumed, then at 0.8 power factor the curve points for the three types of systems would be 22, 21 and 16 instead of 26, 25 and 21, as shown in Figure 5. As these values are to be compared with values of about 2 and 5, depending upon whether the device for producing the kilovars is assumed to cost $10 or $25 per kva, as shown in Figure 6, it is seen there is little effect on the general conclusion that the ratio of these costs is high.

The kernel of the message is: Do the operating companies want to maintain control of their system kilovar sources? Such control is important in system design and operation, as pointed out by B. M. Jones in his discussion, and this control could readily pass into the hands of the consumer if the reduction in the price of capacitors is not factored into the over-all picture by the operating company. Obviously, if this control is to be maintained by the utility, the service costs must reflect the necessary system investment, and this investment must be made on the basis of what it costs to provide the requirements in the most economical manner. The relation of the price and price trend of kilovar sources in this respect is somewhat obvious.

The discussion by Mr. Crary is appreciated, as it will aid in clarifying the real intent of the paper. Synchronous condensers have a definite place in system operation, and they fall into the broad classification of a kilovar source and are to be used accordingly.

I am unable to agree with Mr. Brownlee's interpretation of the conclusions given in his second paragraph. Quite to the contrary, the size of the boilers might have to be increased if additional kilowatts are to be delivered by the prime movers and generators, after the station has been relieved of its kilovar obligations. Further, the addition of capacitors at the generator terminals would merely relieve the generator only of the job and not the transmission and distribution facilities, which represents a major portion of the system investment.

The possible magnitudes of kilovar loss emphasized by Mr. Brownlee and Mr. George when it is generated remotely from the loads indicates the conservativeness of the analysis as this factor (assumption 3) was neglected.

The use of switched capacitors, feeder voltage regulators or load-ratio-control transformers on lines to improve regulation has no effect on that part of the analysis where voltage drop is the limiting factor, as contended by Mr. Brownlee. Regardless of how the voltage drop is actually taken care of, if the kilovars flowing over a line cause half the voltage drop, it seems reasonable to charge the kilovars up with half the investment of that line. Voltage was merely used as a yard stick to determine the relative kilovar and kilowatt usages of the line, and not the usage of the regulating devices. The usage of these regulating devices was determined by thermal or current capacity usage.

Concerning Mr. Foote's remarks, the philosophy offered in the paper in this respect makes available the 125 per cent kilowatt capacity under peak or emergency conditions. How could this overload capacity be utilized, unless the required kilovars were supplied by some other source?

B. M. Jones clearly understood the real message, and I am indebted to him for presenting a system-planning engineer's viewpoint, indicating his appreciation of the philosophy developed.

Mr. Boland's remarks and conclusions concerning metering are very sound. All discussers have in substance agreed to the results indicated by Figure 6 of the paper, namely, investment allocable to kilovars when supplied by auxiliary kilovar generators. The magnitude of the allocation indicated in Figure 5 has been generally questioned, the feeling being it should be reduced. In any case, it is a question of the physical practicability or economic limit beyond which the separate metering and segregation of the two quantities can continue to be justified. As pointed out by Mr. Boland, this may influence the philosophy of handling the kilovar problem; namely, investing in equipment for supplying kilovars instead of in meters for measuring kilovars.

If the philosophy of "separate kilovar and kilowatt supply is an excellent one," as stated and agreed to by Mr. Sels, to apply in the design and operation of a system, it appears logical that the same philosophy should follow through in the analysis of the system investments. This, however, is of academic interest only as it does not necessarily affect the real message and conclusion of the paper as presented in the beginning of this closure. The use of the lower incremental costs as preferred by Mr. Sels or the data in Figure 6 to which he generally agrees, further emphasizes the message, that if kilovar control is to be maintained by the operating companies, these low kilovar costs must be reckoned with. This is clearly recognized by Mr. Sels and his associates as indicated by the extent to which they have carried and reported the kilovar program on their systems. I am indebted for the clarifying data, pertinent to the quotation taken from the Sels and Seeley paper, that only nine per cent of the 20 per cent reported gain was due to power factor improvement.

Self-Excited Oscillations of Capacitor-Compensated Long-Distance Transmission Systems

Discussion and authors' closure of paper 43-8 by R. B. Bodine, C. Concordia, and Gabriel Kron, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, January section, pages 41–4.

S. B. Crary: For discussion, see page 372.

R. B. Bodine, C. Concordia, and G. Kron: As a further emphasis of the second point of Mr. Crary's discussion, it may be re-marked that representation of the performance of any physical system by equivalent circuits rather than by a set of equations has some virtue in calculations even if a network analyzer is not available. For example, such representation of the steady-state performance of transformers and induction motors has been for many years a commonplace with practically all engineers, and the representation of more complex systems is a direct extension of this kind of thinking.

Equivalent Circuits for Oscillating Systems and the Riemann–Christoffel Curvature Tensor

Discussion and author's closure of paper 43-9 by Gabriel Kron, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, January section, pages 25–32.

R. B. Bodine (General Electric Company, Schenectady, N. Y.): The equivalent circuits for oscillating systems developed by Mr. Kron in this and associated papers[1] are a definite and valuable contribution to the theory of electrical machinery. The circuits give a better physical picture of the phenomena involved in the analysis of oscillating systems and in so doing give a clearer understanding of the variables involved in such analysis. Since the equivalent circuits may be set up on the a-c network analyzer and results desired obtained from meter readings, much of the time and labor involved in making the necessary calculations is eliminated. A direct consequence of the use of an a-c network analyzer in the study of oscillating systems is that problems which have been left undone, or which have been done by less desirable methods such as model testing because of the weight or complexity of the mathematics needed may now be undertaken.

Some typical results obtained by use of the circuits and the a-c network analyzer are given in references 2 and 4. The data presented are the damping and synchronizing torques for the hunting of a doubly fed motor and for the hunting of a synchronous machine connected to an infinite bus through a series-capacitor-compensated transmission system. Other cases in which the equivalent circuits have provided useful data are interconnected Selsyn systems, the slip coupling which is a derivative of the synchronous machine, and induction motors used under unusual conditions.

Examination of the equivalent circuits as presented in references 1, 2, and 3 shows that the circuit elements occurring most commonly are inductance and positive and negative resistance. If negative resistance units are not available, the impedance values, in the event that capacitance is not present, may be multiplied by $-j$ to convert to positive resistance, capacitance, and inductance. If all four circuit elements are present, methods of superposition may be used. These consist essentially of removing the branch in which the negative resistance occurs, replacing the circuit as viewed from this branch by an equivalent impedance and voltage, and completing the calculation by hand.

References

1. Equivalent Circuits for the Hunting of Electrical Machinery, **Gabriel Kron.** AIEE Transactions, volume 61, 1942, May section, pages 290–6.

2. The Doubly Fed Machine, **C. Concordia, S. B. Crary, G. Kron.** AIEE Transactions, volume 61, 1942, May section, pages 286–9.

3. Equivalent Circuits for Oscillating Systems and the Riemann–Christoffel Curvature Tensor, **Gabriel Kron.** AIEE Transactions, volume 62, 1943, January section, pages 25–32.

4. Self-Excited Oscillations of Capacitor-Compensated Long-Distance Transmission Systems, **R. B. Bodine, C. Concordia, G. Kron.** AIEE Transactions, volume 62, 1943, January section, pages 41–4.

S. B. Crary (General Electric Company, Schenectady, N. Y.): These papers illustrate how the network analyzer may be used for the study of self-excited oscillations or hunting in electromechanical systems. The use of equivalent circuits for the study of such problems is of great practical significance.

Physical problems can be analyzed and solved which heretofore were impossible of analytical solution because of the time and labor involved. Previously, either expensive trial and error tests on actual systems or experiments with miniature systems which were many times but crude approximations were necessary.

A better physical conception of the phenomena is obtained including a realization of the quantitative importance of the various factors. These are secured because

(*a*). Fewer idealizing assumptions are necessary.

(*b*). The equivalent circuit presents by its arrangement and the magnitude of its elements a visual and more easily understood expression of the underlying physical relations.

Gabriel Kron: Another advantage of the equivalent circuits may be mentioned, not brought out by the discussions of Bodine and Crary. While considerable work was expended in discovering how to interconnect the equivalent circuits of two oscillating induction motors (or rather two oscillating primitive machines energized by alternating currents) no comparable work was involved in setting up the rest of the equivalent circuits. Once the method of interconnection of two primitive machines or systems was known, then the interconnection of the equivalent circuits of any two other machines (or one machine and a network) followed automatically. The already known equivalent circuits of two isolated salient-pole synchronous machines (or of the compensated transmission line and a synchronous machine) were interconnected without writing down a single equation, irrespective of whether their performance involved steady-state or small oscillations. The work consisted simply of repeating the method of interconnection of two primitive machines and making the appropriate changes necessary.

Expressed in another way, in analyzing a family of related systems, such as stationary or rotating electrical networks, by means of the tensorial approach, even in the absence of equivalent circuits, it is sufficient for the student to familiarize himself thoroughly with the physical and mathematical analysis of only one particular system, the so-called "primitive" system. Then by knowing only the "transformation tensor" $\mathbf{C}$ by which any particular system under review differs from the primitive system, the engineer can establish the physical analysis and the equations of performance of his system by routine manipulations (employing the laws of transformation of geometric objects and tensors) without the necessity of starting from scratch for each given system.

Now, with the use of equivalent circuits the engineer doesn't even need to go through the pick and shovel work of establishing the equations of his system. (Even this routine task of simply writing down on paper the equations of hunting of a capacitor-compensated transmission system is a forlorn undertaking, without considering how the engineer could keep track of the physical analysis.) The establishment of the equivalent circuit of any particular system from that of the primitive system and the method of their interconnection into more complex structures is purely graphical and involves no writing of equations, provided the matrix of transformation $\mathbf{C}$ of the particular system is available.

Besides relieving the engineer from the tedious task of juggling equations (while still allowing him to retain a clear and complete physical picture) the use of equivalent circuits takes off the designing or application engineer's back the tremendous burden of computation. It is interesting that while engineers unanimously dislike pushing the slide rule, the crew running the network analyzer actually enjoys working with equivalent circuits.

Shunt Capacitors

Discussion and author's closure of paper 43-48 by the AIEE general systems subcommittee of the committee on power transmission and distribution, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, May section, pages 222–30.

M. C. Westrate (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper on a very timely subject covers the application of shunt capacitors and their effect upon the system. The term "kilovar" is naturally very useful in analyzing the contribution of capacitors, and this paper makes use of the two-component concept: namely, kilowatts and kilovars. It is a bit disappointing, however, to find the introduction of a term "negative kilovars." In system load-division studies, it is usually convenient to indicate the flow of power in an assumed direction and then to use a minus sign whenever the direction of flow turns out to be opposite to the way assumed. This is a convenience for diagrammatic work and calculations, but the kilovar component is still considered positive the same as kilowatts. Thus, an overexcited generator should be regarded as supplying kilovars to the system rather than drawing negative kilovars as mentioned in this paper. Similarly, in the example under "Current Limitation" and as shown in Figure 3 of the paper, the capacitor should be considered as supplying kilovars rather than consuming them. The use of the term "kilovar" and the two-quantity concept has been so helpful in overcoming some of the difficulties experienced with "power factor" that it would be very unfortunate to complicate it more than necessary.

Formula 1 in the paper shows kilovolt amperes equal to E_1^2 and so forth, which as explained in the text is on a per-unit basis and therefore correct mathematically. However, it would seem more desirable to have formulas in themselves equal in regard to electrostatic units which could be accomplished by inserting a per cent sign in front of each term on both sides of the equation.

In the example of how a capacitor can relieve the loading of a transformer, it is shown that the "increase in ability to supply load" is obtained at $11.55 per kilovolt-ampere for the capacitor and $10.00 per kilovolt-ampere for the transformer with the statement that "then considering the other advantages of the capacitor, the latter may be more economical." It would seem as though this should read "then *not* considering the other advantages."

R. M. Hawekotte (Bell Telephone Laboratories, Inc., New York, N. Y.): As pointed out in the paper, a considerable amount of work has been done by the Joint Subcommittee on Development and Research of the Edison Electric Institute and Bell Telephone System in regard to the effects of shunt capacitors on the inductive co-ordination problem. I should like to elaborate somewhat on this joint work.

The subcommittee work on shunt capacitors was initiated in 1935, when the use of capacitors was limited to low-voltage industrial-plant installations for power-factor and voltage improvement. The problem at that time was found to be one of voltage control and capacitor fuse operation during light-load periods rather than an effect of harmonic currents and voltages on exposed telephone circuit noise. However, the development in 1937 of the outdoor pole-type capacitor resulted in extensive use of capacitors on primary distribution circuits in the range from 2.3 to 13.8 kv, and the inductive effects of these harmonic currents and voltages became important. The subcommittee studied these problems and made the results of their preliminary studies available to the field about the middle of 1938. This information indicated what effects might be expected and what remedial measures, applicable to either the power or telephone systems or both, were available.

Although the demands of more vital problems in connection with the war effort have necessitated postponing the completion of a report summarizing the results of the shunt-capacitor studies, the groundwork laid by the subcommittee has been of considerable value during this emergency which has added to the number of inductive co-ordination problems because of the necessity for providing increased power and telephone service with minimum amounts of critical materials. For example, many shunt-capacitor installations have been made by the power companies to permit more power to be transmitted over existing lines without using large amounts of additional copper. The telephone companies have also had to make greater use of multiparty service employing grounded ringing in order to avoid placing additional wire and cable facilities.

Restrictions on the use of strategic materials likewise have made it increasingly difficult to obtain the material necessary for certain types of remedial measures: for example, neutral reactors for three-phase wye-grounded capacitor banks on multigrounded neutral systems. It is of considerable interest, that, as indicated in the paper, there are certain protection advantages in the operation of wye-connected capacitors with neutral isolated, since this method of connection limits the effect of the capacitors on the power-system influence more effectively than the neutral reactors.

The telephone station sets being manufactured before the current restrictions on materials had a very low inductive susceptiveness. The more recently manufactured sets, however, have a somewhat higher susceptiveness, because magnetic iron has been substituted for Permalloy in the ringers in order to save nickel. Also, since the manufacture of station sets has now been practically discontinued, it has become necessary to repair and reuse many older sets of discontinued types having relatively high susceptiveness. As an aid in these co-ordination problems, increasing use is being made of telephone-cable-sheath shielding. Where practicable, the cable sheaths are bonded to a low-impedance multigrounded neutral conductor as described in engineering report 43 of the Joint Subcommittee on Development and Research.

Various factors thus tend to increase the difficulty of securing adequate co-ordination and point to the desirability of being particularly alert in the matter of advance planning and advance study of proposed capacitor installations.

During 1942 several relatively large installations of shunt capacitors were made in connection with large-capacity multiphase rectifiers. The subcommittee has studied the inductive co-ordination aspect of multiphase rectifiers, and the results are described in engineering report 49 of the Joint Development and Research Subcommittee. It is now studying the combined installations of multiphase rectifiers and shunt capacitors. Briefly, the results in the cases investigated to date indicate that the net effect of these capacitor installations, from the inductive co-ordination standpoint, has been beneficial, particularly under balanced operation of the rectifier station: that is, with all rectifier units in service. However, there is a possibility of adverse effects from an inductive co-ordination standpoint, depending on a number of factors such as rectifier size and number of phases, size of capacitor bank, impedance and voltage of the supply system, and so forth. In such cases, the effects will be more severe for unbalanced station operation; that is, when one or two units of the multiphase rectifier are shut down for servicing or maintenance.

C. F. Wagner: In reply to Mr. Westrate's comments, I see nothing incongruous in the conception of a negative kilovar. As concerns the load dispatcher, it is desirable that reference be made only to positive kilovars as the quantity dispatched throughout the system. Thus an overexcited synchronous condenser should be regarded as supplying kilovars to the system or as generating kilovars and an underexcited condenser as drawing kilovars from the system or absorbing kilovars. We should not, however, disallow the use of negative kilovars any more than we disallow negative kilowatts. In analytical work we frequently have occasion to use the conception of both negative kilowatts and negative kilovars. A particular example is the circle diagram of the kilowatts and kilovars at the sending or the receiving end of a transmission line. Here of necessity, one must use both positive and negative values of both kilowatts and kilovars, and no confusion results from this practice.

With regard to Mr. Westrate's criticism of the form of equation 1, the insertion or omission of the term "per unit" on each side is one of preference. One could not, of course, simply insert per cent signs without altering the form of the equation to take care of the 100 factor on the left-hand side.

Mr. Westrate is quite correct in his criticism that the word "not" should have been inserted in the discussion of how a capacitor can relieve the loading of a transformer. Thus the last sentence in the third paragraph under "Current Limitation" should read, "Then not considering the other advantages . . . ."

Mobile Capacitor Units for Emergency Loading of Transformers in Open Delta

Discussion and authors' closure of paper 43-16 by Herman B. Wolf and G. G. Mattison, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, February section, pages 83–6.

Edith Clarke and **H. A. Peterson:** For discussion, see page 375.

Howard P. Seelye: For discussion see page 375.

Herman B. Wolf and **G. G. Mattison:** In the Edith Clarke and H. A. Peterson discussion, two other possible methods of balancing voltages of open-delta banks are suggested, one using series capacitors and the other a series reactor.

The purpose of the mobile capacitors is to permit loading of self-cooled transformers in closed delta to the approximate thermal limit and then, in emergency, by applying the mobile unit, to carry the entire bank load on two transformers in open delta with the voltage reasonably well-balanced and the transformer loading reduced to the extent that copper temperatures are kept within safe limits with the aid of supplementary cooling. Since neither of the suggested methods reduces the transformer loading, they are not applicable for this purpose.

Two other methods of balancing the voltage of an open-delta bank have been used by the authors; namely:

1. Raising the voltage of the phase lagging the open delta by raising the secondary voltage of the transformer connecting to this phase, this being accomplished by proper setting of the ratio adjuster.

2. By use of an autotransformer, the exciting winding being connected across the open-delta phase and the series winding in the phase lagging the open delta.

By either of these methods, the voltage at any predetermined load is balanced satisfactorily, but, as transformer loading is not reduced, the full closed-delta load cannot be carried safely on the open-delta bank, even with the aid of supplementary cooling.

The effectiveness of the shunt capacitors in reducing load of open-delta banks is illustrated by copper temperatures of transformers in supplements to Figures 4 and 5 of the authors' paper.

Supplement to Figure 4

	Closed Delta Self-Cooled Without Capacitors (Degrees Centigrade)	Open Delta Fan-Cooled 405 Kilovolt-Amperes in Capacitors (Degrees Centigrade)
Hot-spot rise above top oil	12	18
Hot-spot temperature with 35 degrees centigrade ambient	77	67

Supplement to Figure 5

	Closed Delta Self-Cooled Without Capacitors (Degrees Centigrade)	Open Delta Fan-Cooled 1,080 Kilovolt-Amperes in Capacitors (Degrees Centigrade)
Hot-spot rise above top oil	3.5	5.5
Hot-spot temperature with 35 degrees centigrade ambient	76.5	62.5

Open-Delta Transformer Banks—Analysis of Circuit

Discussion and author's closure of paper 43-45 by J. E. Clem, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, June section, pages 292–7.

Herman B. Wolf and **G. G. Mattison** (Duke Power Company, Charlotte, N. C.): The authors of the paper "Mobile Capacitor Units for Emergency Loading of Transformers in Open Delta" are highly gratified that a thorough analysis of open-delta regulation, losses, and corrective capacitance requirements has been made. In his paper, Mr. Clem calls attention to the increased heating of motors when supplied by an open-delta bank; this condition was impressed forcibly on the discussers recently when, in emergency, an attempt was made to supply several textile plants from a bank of transformers in open delta. The voltage was unbalanced six per cent. The plants with older motors made no complaint of this unbalance, but one plant equipped with modern thermal relay protected motors was unable to run, presumably because of the increased losses on motors of close design. Mobile capacitor units were connected to the station bus without interrupting service to the plants which were able to run with the unbalanced voltage; this balanced the voltage within two per cent, which permitted normal operation of all customers.

Edward W. Kimbark (Massachusetts Institute of Technology, Cambridge, Mass.): The purpose of connecting unbalanced shunt capacitors to the secondary terminals of an open-delta transformer bank (as shown in Figure 5 of Mr. Clem's paper) appears to be threefold:

1. To balance the secondary voltages or—in terms of symmetrical components—to eliminate the negative-sequence secondary voltage.

2. To raise the positive-sequence secondary voltage.

3. To reduce the currents in the transformers and the consequent heating of the transformers.

As Mr. Clem points out, both the lowering of the negative-sequence voltage and the raising of the positive-sequence voltage serve to reduce the currents and copper losses in induction motors fed from the transformers.

If the object were merely to balance the secondary voltages, it could be accomplished simply by connecting in series with the common conductor A on either primary or secondary side of the transformers an impedance (predominantly inductive reactance) equal to the short-circuit impedance of one of the transformers measured in ohms from the same side. This device without further adjustment would balance the secondary voltages for any load kilovolt-amperes and power factor, providing only that the load itself were a balanced one. The positive-sequence voltage drop through the bank, however, then would be three times as great as for a delta-delta bank; whereas, for the open-delta bank without the added impedances, the positive-sequence voltage drop would be only between 1.5 and 2.0 times as great as for a delta-delta bank, depending upon the negative-sequence impedance of the load. The negative-sequence secondary voltage of the open-delta bank is between 0 and 0.5 times the positive-sequence drop in the same bank or between 0 and 1.0 times the positive-sequence drop in a delta-delta bank.

A fixed capacitor in series with each transformer would very nearly balance the voltages and would decrease the regulation also.

By the use of two capacitors connected as shown in Figure 5 of Mr. Clem's paper, it is possible to obtain exactly balanced secondary voltages on an open-delta transformer bank feeding a balanced three-phase load. To maintain this balance, it is necessary to vary each capacitor as the load changes in kilovolt-amperes or in power factor.

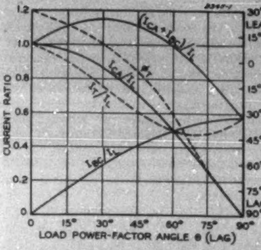

Figure 1. Proper corrective capacitor currents I_{CA} and I_{BC} to balance the secondary voltages of an open-delta transformer bank feeding a balanced load of current I_L lagging by angle θ. Also the resulting transformer current $I_T \angle \phi_T$

If balanced voltages are applied to a balanced load, line currents a and c will be equal in magnitude and 120 degrees apart in phase. These currents must be modified by the addition of the capacitor currents, leading their respective line-to-line voltages by 90 degrees and of such magnitudes that the resulting transformer currents are equal in magnitude (to each other, not to the load currents) and 60 degrees apart in phase, in order to give balanced primary voltages. Conversely, when the corrective capacitances so determined are used, the application of balanced primary voltages will result in balanced load voltages.

Figure 6 of Mr. Clem's paper presents charts which are intended for use in determining the proper amounts of corrective capacitance or capacitive current, but these charts are inconvenient to use, because they require a cut-and-try process as illustrated in the paper. The formulas for the proper capacitor currents are extremely simple. The currents I_{ca} and I_{bc} in capacitors C_{ca} and C_{bc}, respectively (see Figure 5 of the paper) are given by

$$I_{ca}=I_L \cos \theta \tag{1}$$

and

$$I_{bc}=I_L \sin \theta/\sqrt{3} \tag{2}$$

where I_L is the load current per terminal, and θ is the power-factor angle (positive for lagging current). The current in one capacitor should be equal to the active component of load current, and the current in the other capacitor should be equal to the reactive component of load current divided by $\sqrt{3}$. These values are independent of the transformer impedance.

The proper capacitor currents are plotted versus the power-factor angle θ in Figure 1 of this discussion. The arithmetical sum of the two capacitor currents is plotted also. The sum serves as an index of the aggregate kilovolt-ampere capacity of the two capacitors. The capacity is seen to be greatest at 30 degrees lag (87 per cent power factor); here the total capacitor current is 1.15 times the load current, or kilovolt-ampere capacity is 1.15/1.73 = two-thirds that of the load.

The current in the transformers is reduced by use of the capacitors. When the proper values of capacitance are used, as given by equations 1 and 2 preceding, the current in the two transformers of an open-delta bank are equal in magnitude and 60 degrees apart in phase. The magnitude is given by the curve I_T/I_L in Figure 1 of this discussion, and the phase with respect to the secondary voltage of the same transformer is given by the curve ϕ_T. For any load power factor with lagging current, the transformer currents are less with than without capacitors. Note that, in a delta-delta bank without capacitors, $I_T/I_L=0.577$. The magnitude and phase of the transformer current are given by the vector equation

$$I_T\angle\phi/I_L = 0.763\angle(19°-\theta)+0.288\angle(60°+\theta) \qquad (3)$$

From the magnitude and phase of the transformer current and a knowledge of the transformer impedance, the regulation can be calculated by ordinary methods or by Mr. Clem's new formula. The regulation of the open-delta bank with capacitors is not only less (algebraically) than the regulation of the same bank without capacitors, but it is also less than the regulation of the delta-delta bank without capacitors.

Edith Clarke and H. A. Peterson (General Electric Company, Schenectady, N. Y.): These papers indicate thorough and careful analyses of the possible difficulties that may be encountered when supplying load to motors through open-delta transformer banks. The shunt-capacitor scheme of balancing the voltages at the load is illustrated, and methods of calculating the performance are given.

Two other possible methods of solution could be used, namely:

1. Series capacitors in two phases.
2. Series reactor in one phase.

For the former method, the capacitive reactances should be inserted on either side of the open-delta transformer in the two lines marked *B* and *C* in Figure 3*a* of Mr. Clem's paper. In ohms, the capacitive reactance should be equal to the leakage reactance for one of the single-phase transformers referred to the side in which the capacitors are installed. This would have two advantages; namely:

1. Substantially balanced voltages would be applied to the motors for all loads and all load power factors.
2. No reactance drop would be encountered across the open-delta bank plus the series capacitors considered as an integral unit.

By overcompensating the leading phase and undercompensating the other phase by an equal amount, negative-sequence currents resulting from unbalanced transformer resistances can be eliminated also.

The second scheme employing a series reactance in the line marked *A* of Figure 3*a* can also be used to supply balanced voltage at the load for any condition of operation. The reactor should be equal in ohms to the leakage reactance of one of the single-phase transformers referred to the side in which the reactor is installed. Thus, since only one is required, the corrective kilovolt-amperes required is half that required for the series-capacitor scheme. However, this scheme has the disadvantage of increasing the voltage drop across the open-delta transformer and the series reactor considered as a unit. This may not be a limitation, however, because generally regulation at the load is not so important as the unbalanced voltages, or the problem would not have arisen in the first place.

For either of these schemes, the corrective kilovolt-amperes required is independent of the side of the transformer on which the corrective kilovolt-amperes is installed.

Howard P. Seelye (The Detroit Edison Company, Detroit, Mich.): Although the open-delta transformer connection has been used for many years, it has usually been considered to be in the nature of an emergency or temporary expedient. This is, of course, because, for a three-phase load, the transformer capacity required with open delta is 15 per cent greater than with a closed-delta installation, and the voltages produced are unbalanced, sometimes to an extent detrimental to the service. Several years ago a number of studies and tests made in The Detroit Edison Company showed that for loads consisting of single-phase and three-phase combined, the single-phase being on one phase only and of considerable amount compared with the three-phase, open delta could be used effectively and had several advantages over closed delta. Not only is there one less transformer to install, but also, if the proper phase relation is chosen, the total load-carrying capability per kilovolt-ampere of transformer capacity is larger than with closed delta, and the voltage balance is better. This was quite thoroughly discussed in an article in *Electrical World*, April 20, 1929.[1,2]

In 1934 the method of serving commercial customers having both single-phase and three-phase load on the Detroit Edison system was changed. Before that time separate transformers, services, and meters had been used for the two types of load. It was decided to combine these on one service and one meter. This created quite a problem in distribution. Three-phase distribution transformers had been the rule for three-phase service. Since these were wye-delta wound, they could not be paralleled with the single-phase transformers in the area. It was necessary to remove them and convert the distribution to delta delta.

In a great deal of the territory the smaller three-phase loads could be handled by adding a fourth wire to existing single-phase three-wire secondary circuits. The open-delta connection was ideal for this purpose. The existing single-phase transformers, which in Detroit were mostly connected in banks along the secondary mains, served as one leg of the V. A small single-phase transformer just large enough for the three-phase load was connected for the other leg, supplying the fourth wire. Figure 2 shows the plan of a typical installation. The three-phase loads are indicated on this plan, but the single-phase services are not shown. Primary circuits, which also are not shown on the plans, were 4,800-volt ungrounded delta. The experience with open delta quoted here refers only to that system; none has been had with four-wire, wye-connected primary circuits. Incidentally, it was found possible to revamp most of the smaller old three-phase transformers to single-phase transformers for this purpose.

This practice was found to be very convenient and efficient and is the standard for the Detroit Edison system today. There are some 5,000 of these open-delta installations scattered throughout the territory, serving a total of three-phase loads of the order of 100,000 kva. Some of these are relatively small and isolated, as in Figure 2 of this discussion. Others are more extensive, providing service to a number of customers along a street as in Figure 3 of this discussion. Although other companies have made similar use of open delta, it may be of interest, for those who have not done so, to review briefly the theory which underlies its application, showing why it offers particular advantages for such combined loads.

It is well known that, if a balanced three-phase load is carried on two transformers in open delta, the current in each transformer is the line current; that is, it is $\sqrt{3}$ times the current which would be found in the transformer if the delta were closed by a third transformer. Current in one transformer lags behind the voltage by an angle equal to the phase angle of the load *plus* 30 degrees; in the other transformer the current lags by an angle equal to the phase angle of the load *minus* 30 degrees. For a load power factor of 80 per cent, the phase angle in one transformer is approximately 37 degrees+30 degrees=67 degrees; the phase angle in the other is 37 degrees−30 degrees=7 degrees, or nearly in phase with the voltage. Figure 4 of this discussion is a vector representation of this condition.

If a sizeable single-phase load is now added across phase *ab*, the one with the large phase angle, this load being in phase with the voltage tends to decrease the phase angle or to bring the current nearer in phase with the voltage. If this single-phase current is relatively large compared with the three-phase current, the effect will be pronounced, giving a resultant current not far out-of-phase with the voltage and at the same time appreciably less than the arithmetical sum of the two components. Figure 5 of this discussion shows this effect. For example, with the 80 per cent power-factor three-phase load, the total current in such a case would be approximately equal, numerically, to the single-phase current plus one half of the three-phase. This should result in good efficiency in the use of transformer capacity, and that has proved to be the case. It should be noted that the effect described is only obtained when the single-phase load is added to the transformer having the large phase angle. That is, this should be the phase which *leads* by 120 degrees the phase on which the other smaller transformer is connected. If the wrong phase is used, the effect is just the opposite to that described, resulting in a disadvantage rather than an advantage in loading.

It has been mentioned that the ordinary use of open delta for three-phase load only results in an unbalanced voltage. This is due to the fact that the current in one transformer is nearly in phase with the voltage while in the other it is over 60 degrees lagging, as well as to the effect of the absence of the third transformer. With the combined load which has just been described, however, the currents in bo transformers are not far out of phase with the voltage, which results in less unbalance in regulation. Even when the single-phase load is not operating, as will be the case part of the time, the fact that the transformer carrying the low power factor is

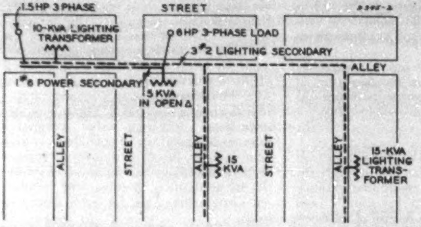
1.5 HP 3 PHASE
STREET
10-KVA LIGHTING TRANSFORMER
6 HP 3-PHASE LOAD
3#2 LIGHTING SECONDARY
ALLEY
1#6 POWER SECONDARY
5 KVA IN OPEN Δ
ALLEY
STREET
ALLEY
15 KVA
STREET
ALLEY
15-KVA LIGHTING TRANSFORMER

Figure 2. Example of use of open delta

larger than the other and hence has lower actual impedance tends to keep the voltage in better balance than it would be with two transformers of equal size.

There is the obvious question, of course, as to how such an open-delta installation compares with a closed-delta connection for the same purpose. Although a balanced three-phase load served by a balanced delta-delta transformer installation (all three transformers of the same size) results in equal loads on the three transformers and in balanced voltages, if the transformer installation is not balanced, but is made up of one large transformer and two smaller ones, the loading and voltages are unbalanced. The current in the larger transformer is larger than that in the smaller ones, with its phase angle remaining the same as that of the load. In one of the smaller transformers, however, the current is advanced in phase while in the other it is retarded. This is shown vectorially in Figure 6. For example with a 25-kva and two 5-kva transformers, assuming similar impedances, the 25-kva transformer carries 36.5 per cent more current than it would if all three were 5 kva. The 5-kva transformers in the unbalanced group carry 87.5 per cent of the current which they would have if all three were of the same size; this current is shifted 20 degrees 45 minutes, being advanced in one and retarded in the other from the power-factor angle of the load.

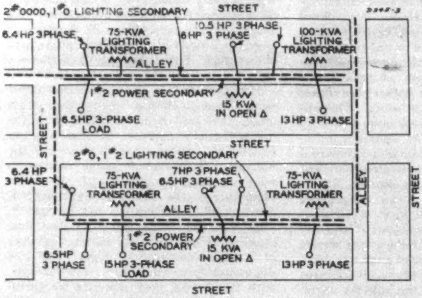
2#0000, 1#0 LIGHTING SECONDARY
STREET
6.4 HP 3 PHASE
75-KVA LIGHTING TRANSFORMER
10.5 HP 3 PHASE
6 HP 3 PHASE
100-KVA LIGHTING TRANSFORMER
ALLEY
1#2 POWER SECONDARY
15 KVA IN OPEN Δ
6.5 HP 3-PHASE LOAD
13 HP 3 PHASE
STREET
STREET
2#0, 1#2 LIGHTING SECONDARY
6.4 HP 3 PHASE
75-KVA LIGHTING TRANSFORMER
7 HP 3 PHASE
6.5 HP 3 PHASE
75-KVA LIGHTING TRANSFORMER
ALLEY
ALLEY
STREET
1#2 POWER SECONDARY
15 KVA IN OPEN Δ
6.5 HP 3 PHASE
15 HP 3-PHASE LOAD
13 HP 3 PHASE
STREET

Figure 3. Example of use of open delta

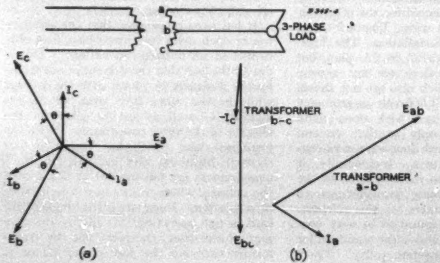
a
b
c
3-PHASE LOAD
E_c
I_c
θ
E_a
θ
θ
I_b
I_a
E_b
(a)
$-I_c$
TRANSFORMER b–c
E_{ab}
TRANSFORMER a–b
E_{bc}
I_a
(b)

Figure 4

(a). Line voltages and currents

(b). Open - delta voltages and currents. Three-phase load

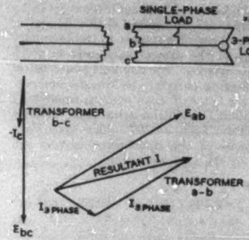
SINGLE-PHASE LOAD
a
b
c
3-PHASE LOAD
TRANSFORMER b-c
$-I_c$
E_{ab}
RESULTANT I
TRANSFORMER a-b
I_3 PHASE
I_3 PHASE
E_{bc}

Figure 5. Open-delta voltages and currents. Three-phase and single-phase loads

If a single-phase load is now added across the phase with the larger transformer, part of it will be carried by the two smaller transformers in series (for 25 and 5 kvas the proportion will be 1/11). This single-phase component in the smaller transformers is far out of phase with the three-phase load currents, and the effect of the vectorial addition is to increase the actual resultant current in one of the smaller transformers and decrease it in the other, as indicated in Figure 7. For usual cases, the total load which can be carried by an unbalanced delta-delta group is limited by the capacity of that one of the smaller transformers which has the larger current; both the larger transformer and the other smaller one will be underloaded when this one is loaded to its full capacity. This does not make for efficient use of transformer capacity nor is the voltage well balanced on such a combination, since each transformer has a different power factor, as well as a different proportion of loading.

For example, with the combination of a 25-kva and two 5-kva transformers with similar impedances, the maximum combined load, for single-phase load at unity power factor and three-phase load at 80 per cent power factor, is about 20-kva single-phase and 12-kva three-phase, or a total of 32 kva on 35 kva of transformer capacity (Load/Transformer capacity = 0.915). For the 25-kva and one 5-kva in open delta, the maximum combined load would be about 22.5 kva, single phase and 8.7 kva, three phase, or a total of 31.3 on 30-kva transformer capacity (Load/Transformer capacity = 1.05). The latter evidently has about 15 per cent advantage in load-carrying ability.

Table I

	Closed Delta	Open Delta
Total transformer capacity	13.0 kva	11.5 kva
Load: Three-phase at 78 per cent power factor	3.6 kva	2.6 kva
Load: Single-phase at 100 per cent power factor	9.5 kva	10.0 kva
Total	13.1 kva 12.32 kw	12.6 kva 12.0 kw
Ratio: $\frac{\text{Load in kva}}{\text{Transformer capacity}}$	*1.01	*1.09
Relative load ratio	$\frac{1.09}{1.01} = 1.08$	
Transformer currents	43.6 6.7 2.71	44.2 6.6 0.0
Phase voltages	240.2 238.2 234.6	240.4 238.2 235.8
Maximum difference in phase voltages	5.6 volts	4.6 volts
Maximum deviation from average voltage	3.1 volts	2.3 volts

* In both cases the transformers were slightly overloaded, thus giving ratios larger than unity in each case. The relative ratios are significant, however.

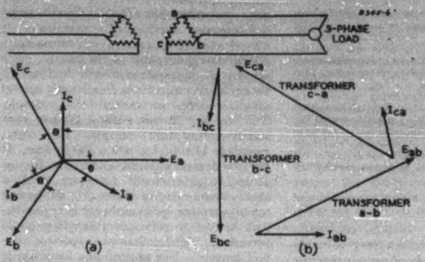

Figure 6

(a). Line voltages and currents

(b). Closed-delta voltages and currents — three-phase load

The example which has been given is based on a calculation of load division, assuming transformers of similar impedance and taking no account of the effect of voltage unbalance on the load. The results of an actual test will, therefore, be interesting for comparison. One 10-kva and two $1^1/_2$-kva transformers were used with resistances of 0.83 and 1.53 per cent and reactances of 1.8 and 1.4 per cent respectively. The results in Table I of this discussion were obtained.

It is evident that the open delta has a load-carrying capability eight per cent greater than the closed delta and at the same time maintains a somewhat better voltage balance, to the extent of about one volt.

In addition to the points which have been mentioned, the open delta has some other advantages. With closed delta, if the larger transformer burns out, the smaller ones attempt to carry the load and, unless individually protected by fuses against such a condition, are likely to burn out also. This cannot happen with open delta. The convenience and economy of having to install only one small transformer instead of two (in order to give three-phase service where single-phase is already available) has been mentioned and is a very real point in favor of open delta. Voltage dips on the phase carrying the lighting load caused by three-phase motor starting are of about the same order on equivalent installations of either open or closed delta.

Since combined single-phase and three-phase load is frequently served by three-phase wye-connected transformer installations, with four-wire secondary circuits at 120/208 volts, the question of how open delta compares with this practice is pertinent. The wye-connection has considerable advantage for large loads where the single-phase load can be conveniently divided among the three phases and where the transformers are relatively large compared with the starting currents of the motors which are served. It is well adapted to the heavy secondary network serving dense commercial areas, where it is most commonly used. For distribution in lighter commercial and residential areas, however, this type of secondary is usually not desirable. Three-wire single-phase secondary circuits are preferable in such areas, for the sections where there is only single-phase load and many of the services are relatively small. Occasional three-phase services can be conveniently served by "spot" installations of open delta, as shown in Figures 1 and 2 of this discussion. The voltage dip on the lighting services, caused by motor starting currents in such cases, is considerably less with open delta than it would be if the same transformer capacity were divided between three phases with lighting on each.

It is recognized that there are some conditions under which these advantages for open delta will not materialize. If the three-phase component of the load is quite large, compared with the single-phase, a closed-delta installation may be more suitable. It has been found, however, that, in general, the open delta is favorable for combinations in which the single-phase is at least two thirds as much as the three-phase (in kilovolt-amperes). There may also be uses in which the characteristics of the load or the transformers to be used may modify the general rules. For the usual situations, however, the advantages which have been discussed are real and practicable.

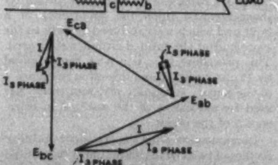

Figure 7. Closed-delta voltages and currents. Three-phase and single-phase loads

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1. OPEN DELTA FOR PERMANENT USE, Howard P. Seelye. *Electrical World*, April 20, 1929, page 775.

2. ELECTRICAL DISTRIBUTION ENGINEERING, Howard P. Seelye, McGraw-Hill Book Company, Inc., New York, N. Y., 1930, Chapter IX, page 158.

J. E. Clem: H. B. Wolf and G. G. Mattison mention a case in which motors for textile plants were unable to operate from an open-delta bank with the voltage unbalanced at six per cent. This is very interesting and indicates that the operation of such motors from open-delta banks must be scrutinized more carefully now than in the past.

In the discussion presented by Mr. Kimbark and also in the discussion presented by Miss Clarke and Mr. Peterson, it is said that balanced secondary voltages can be obtained by the use of a reactor placed in series with the common line to the two remaining transformers of the open-delta bank. This is true and has been known for many years, having been given in one place by C. LeG. Fortescue on page 410 of the fourth edition of the Standard Handbook.[1] This method of approach is impractical because the regulation is three times that for the complete delta bank. This fact is also mentioned by the discussers, and it, therefore, appears that there is general agreement on this method of solution.

Mr. Kimbark gives equations which he proposes for use to obtain the values of corrective capacitance necessary to produce reasonably balanced regulation on the open-delta bank. Mr. Kimbark does not give any development to support these equations. Applying Mr. Kimbark's method to the case shown in Table I of my paper, which is Figure 4 of the Wolf-Mattison Paper, we find that the capacitance currents would be 401 amperes for phase BC, 938 for phase BA, and 1,190 for the total. Actually, the values used by Wolf and Mattison for this case were 350, 700, and 900 amperes respectively.

Miss Clarke and Mr. Peterson suggest the use of capacitors in series with two of the lines supplying the remaining transformers of the open delta, and, again, the discussers point out the limitations of this method in that it does not balance the secondary voltages, but rather eliminates the reactive components of the voltage drop between primary and secondary circuits. In addition, the use of series capacitors involves greater engineering problems than the use of shunt capacitors.

REFERENCE

1. C. LeG. Fortescue. STANDARD HANDBOOK FOR ELECTRICAL ENGINEERS (fourth edition). McGraw-Hill Book Company, Inc., New York, N. Y., 1915, page 410.

Steady-State and Transient-Stability Analysis of Series Capacitors in Long Transmission Lines

Discussion and authors' closure of paper 43-13 by J. W. Butler, J. E. Paul, and T. W. Schroeder, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, February section, pages 58–65.

John G. Holm (Stone and Webster Engineering Corporation, Boston, Mass.): Anyone who is interested in the subject of long-line performance under normal and fault conditions will welcome the very fine paper presented by the authors. With opinions among engineers divided as they are on the subject of the best means applicable for the increase of transmission-system stability, the authors' paper is an important step forward in that it analyzes the series-capacitor compensation of a line in a transient state and approximately outlines the line length for which it may best be applied.

I agree with the authors that in existing lines of 150–175 miles in length where generators and circuit breakers cannot be replaced, the series-capacitor compensation is one of the most expedient and economical means of increasing the line-stability limits. (Resistor grounding of the sending-transformer neutral, insulation permitting, and shunt reactor on the generator bus are other methods for the increase of the stability limits of such lines.) I doubt, however, if in new projected lines of this length the series-capacitor compensation will provide the best means for lifting the stability limits, even though these lines are electrically heavy in relation to the transmission voltage. That in such lines means will have to be provided in order to take care of stability properly remains beyond doubt; but I think that means such as low generator transient reactance, high short-circuit ratio, high generator inertia, and high-speed circuit breakers will prove to be the more efficient and economical, as well as sufficient, for these lines.[1]

It seems to me that the 300-mile length the authors selected for the transient-stability study of two-circuit lines is about the best and most suitable length for the efficient and economical application of series-capacitor compensation, disregarding the question of critical materials.

As the authors point out, there is an optimum series-capacitor compensation for each electrical length of the line. I doubt if in lines 400 miles and longer one could go above a 75 per cent capacitor compensation. For even if this compensation should prove to represent for such lines the optimum value from the viewpoint of stability limits (and this may not necessarily be the case), such a compensation will probably leave a line of this length operating below its total over-all efficiency and with generally less desirable operating characteristics. Thus the most economical and generally most satisfactory condition of operation may not be attained, for a 75 per cent capacitor compensation of a 450-mile line, or longer, may not leave enough reactance for favorable operation of the line. Moreover, as pointed out by the authors, since the series capacitors used for the purpose would have to be of higher than normal dielectric strength, in view of the possible series-resonant conditions in the circuit,[2] the 287–345-kv voltages which will be used with lines of such length will place economic limitations on the series capacitors required. Hence, again disregarding the problem of critical materials, where the series-capacitor compensation has no rivals, it seems to me that from 200 to a little below 400 miles constitutes the region where series capacitors may be applied to best advantage. The value of the paper, I think, is in analyzing the optimum series-capacitor compensation in a faulty line and in removing any doubts as to the benefits which may be obtained from their application in lines plus or minus 300 miles long.

In the details of the paper there are a few points on which perhaps the authors would be willing to make a few additional comments:

1. If there were synchronous condensers at the receiving end of the system, in what kilovolt-ampere capacities were they placed in the various lines studied?

2. The receiving system was represented by two different values of reactance for steady-state and transient-stability studies. Was it intended that the receiving-system reactance represent a load on the system? If so, why is the infinite-bus representation adopted? It is a general understanding that the infinite bus indicates such a large synchronous capacity connected to the receiving end that the frequency and voltage are absolutely unaffected by conditions external to the bus; thus the use of the infinite bus seems to imply that the load is not considered. Whatever the receiving-system reactance represents, either primary distribution system or the load, on what basis were its values selected by the authors?

3. The margin curves of Figure 3 cross some of the stability-limit curves at more than one point. Why should it be so?

Since the authors made a very valuable step forward in the study of series-capacitor compensation, it may not be out of place to discuss the next necessary phase of the study, so that the problem may be further clarified. The series capacitor is merely one of the methods applied for the increase of system-stability limits; various other methods may or may not be used jointly with it. The only method, however, whose counterpart it represents is the intermediate-synchronous-condenser method. These are the two methods which more or less divide the engineering opinion. In so far as I know, the late C. L. Fortescue, for example, decidedly favored the intermediate synchronous condenser. It is therefore between these two methods—all other factors being identical—that a thorough comparison still remains to be made. Omitting the temporarily existing situation of the lack of critical materials, which alone would swing the decision to the side of the series capacitor, the effect of either the series capacitor or of the intermediate condenser on the highest stability limits on the over-all operating characteristics and on the total system cost (at prewar levels, of course) still remains to be studied. Both the capacitor and the condenser require intermediate switching substations of approximately equal cost. It remains to be investigated in what types and for what length of line the lower cost of the series capacitors will result in smaller total capital and annual costs and in better operating characteristics than those obtainable with the more expensive intermediate condensers. Thus far it cannot be stated on which side the balance will be, and to what it will amount. The authors have an opportunity that few engineers have for attacking what may be the final and most important step in the total solution of the problem, an important phase of which they have presented so notably.

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1. STABILITY STUDY OF A-C POWER-TRANSMISSION SYSTEMS, John G. Holm. AIEE TRANSACTIONS, volume 61, 1942, pages 893–905.

2. ABNORMAL OVERVOLTAGES CAUSED BY TRANSFORMER MAGNETIZING CURRENTS IN LONG TRANSMISSION LINES, H. A. Peterson, T. W. Schroeder. AIEE TRANSACTIONS, volume 62, 1943, January section, pages 32–40.

S. B. Crary (General Electric Company, Schenectady, N. Y.): The authors have presented a valuable guide for the rational application of series capacitors to long-distance transmission lines.

As stated in their conclusions, projected systems with transmission distances within the range of 200 to 300 miles may or may not require series compensation, whereas for lines exceeding 300 miles in length series compensation appears to be necessary. It was of interest to make a comparison from Figure 8 of their paper of the relative costs of series compensation versus increase in short-circuit ratio for a line length of 300 miles, in order to transmit, for example, 2.65 kv^2 per circuit with a 20 per cent steady-state-stability margin. This corresponds to 140,000 kw over a 230-kv line. If published generator-price additions for increase in short-circuit ratio are used, a short-circuit ratio of about 2.0, depending upon generator speed and rating, is equivalent in cost to series compensation of 25 per cent at eight dollars per capacitor kilovolt-ampere. For greater line loadings, it becomes more economical to use series compensation also than to increase the generator short-circuit ratio further. Series compensation is less necessary for smaller line loadings, as the required steady-state margin can be obtained more economically by increases in short-circuit ratio alone. This comparison, although very approximate, indicates that for transmission distances of 300 miles, a short-circuit ratio of the order of about 1.7 to 2.3 is probably justified with or without series compensation. Similar comparisons as regards transient reactance and generator WR^2 can be made from the curves in the paper.

R. D. Evans (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors have made a worthwhile contribution on the use of series capacitors on transmission circuits. The value of the paper lies in the curves which give the results of stability calculations for a wide variety of conditions. These should prove useful in general studies involving the longer transmission distances.

The results of the studies are given in terms of certain per-unit quantities. This system is to be preferred to one, based on surge-resistance loadings, proposed by associates of the authors in the paper listed as reference 3. Examination of several figures in the present paper shows that the contemplated transmitted loads are considerably different from the surge-resistance loading, particularly when series capacitors are used. In fact, it appears that the development of the series-capacitor scheme is already rendering obsolete the use of a per-unit notation for long lines based on surge-resistance loading.

In the present paper the studies of transient stability include comparisons of systems without reactance compensation and with several values obtained by series capacitors. For faults close to capacitor locations the transient studies have been based on capacitors which were promptly short-circuited upon the occurrence of a fault and which were restored quickly to the system after the faulted circuit was cleared. This procedure is in line with the proposal by Professor Starr and myself in reference 4 of the paper; it is quite different from that used by associates of the authors in reference 3 of the paper.

In connection with the transient-stability studies of two-circuit lines, it may be pointed out that these are limited to conditions involving three-phase faults. We presume the authors have not intended to imply by including only this type of fault that it only should be used for the basis of determining circuit ratings. Instead, we presume that the data on the three-phase fault have been included as a means for giving some information on the problem without the trouble of the more elaborate calculations required for unbalanced faults. For specific systems we are of the opinion that all types of faults should be investigated but that principal consideration should be given to the types and locations of faults which are most likely to be encountered. As the double line-to-ground fault is usually the most severe type of fault that is encountered with frequency, it is our opinion that this should be used normally in transient-stability studies to determine circuit rating.

The principal conclusion of the paper is that reactance compensation is an effective method for increasing the transient and steady-state stability limits of transmission systems. For most systems involving transmission from a hydroelectric source to a load center, continuity of service requires the maintenance of stability under transient conditions. Consequently, the principal conclusion is that reactance compensation is an effective method for increasing the transient-stability limits of systems.

The conclusions of the paper, in our opinion, while not particularly novel, are nevertheless sound. It is reassuring that the two groups of engineers actively studying the long-distance transmission problem should be in substantial agreement concerning the use of series capacitors on transmission circuits as an economical and practical method of increasing circuit rating.

J. W. Butler, J. E. Paul, T. W. Schroeder: All discussers of this paper have read into the conclusions more definite limitations concerning the line length at which reactance compensation is applicable than the authors intended. While it appears, as stated in conclusion 4, that reactance compensation is a fundamental requirement for transmitting economic blocks of power over lines above 300 miles in length, it is felt that for shorter distances the series capacitor may still prove justifiable, particularly in existing systems. For the series capacitor to be justified in the shorter line lengths, the loadings will have to be greater than that corresponding to unity-power-factor loading with no reactance compensation, which is generally considered desirable from an operating standpoint. But the application of series capacitors should be considered along with or in addition to other stability-increasing means, from an economic standpoint, as discussed in conclusion 3. Here it should be emphasized that, as series capacitors are employed, the unity power-factor loading increases, since the capacitors can supply the line I^2X, in addition to that supplied by line-charging kilovolt-amperes. If it is desired to operate at loadings greater than the uncompensated surge-impedance loading and to depend for stability on means other than the series capacitor, the additional line I^2X must be supplied by terminal apparatus.

Mr. Crary has indicated how one stability-increasing means, increased generator short-circuit ratio, may be compared economically with reactance compensation, as suggested in the preceding paragraph.

Mr. Evans' statement that associates of the authors in their paper listed as reference 3 propose a kilovolt-ampere or kilowatt base of the surge-impedance loading (2.5 kv² for a line having a surge impedance of 400 ohms) rather than a base of kv² as used in the present paper, indicates his absence of familiarity with the paper listed as reference 3. It was in that paper that the use of kv² as a very convenient base was first proposed, and all curves showing results of analyses made are in terms of such a base. In that paper the surge-impedance loading was used only in referring to existing uncompensated lines, and the fact that series capacitors increase the surge-impedance loading was recognized by the statement:

> "If series capacitors are used without shunt reactors, the over-all effective surge impedance is reduced, and it is possible to deliver more power at unity power factor per kv² for the same distance."

Further, it seems that Evans' and Starr's proposal (in reference 4) for protective equipment is no different from that suggested by associates of the authors in the paper listed as reference 3 in which they say:

> "The optimum arrangement (of capacitors) can be determined by a study of the particular case under consideration. Protective equipment similar to that used with present-day lower-voltage installations could be used to protect them during abnormal conditions."

Mr. Evans' assumption that the three-phase fault was used in this study to obtain the results quickly and simply is correct. This type of fault, used for all transient studies, also gives the worst possible condition, hence its use is conservative.

The authors listed what Mr. Evans terms the "principal conclusion" first, because it was thought logical, rather than novel. It is indeed far from novel, and any of the other conclusions certainly exceed it by far in novelty and usefulness.

Concerning other means of raising stability limits suggested by Mr. Holm, it should be borne in mind that resistor grounding of transformer neutrals will raise the transient but not the steady-state limit. The shunt reactor may be used where loadings are under the unity-power-factor loading, and hence excess line charging tends to remove generator excitation without their use, but for heavier loadings existing generators may not have the excitation capacity to supply kilovars to shunt reactors, and new generators can have short-circuit ratio built into them more economically.

Figure 7 of the paper shows that, above 75 per cent compensation in a 300-mile line, the steady-state power limit drops off rapidly for ordinary ratios of receiving-end to sending-end voltage, because power begins to be controlled by voltage rather than by angle difference. As Mr. Holm suggests, even at 400 miles the optimum compensation would probably not be much greater than 75 per cent, though increased line length is in the direction of making the optimum larger.

The series-resonant conditions referred to by Mr. Holm as requiring extra margin in series capacitors in reality will not require such extra margin, as these conditions are to be avoided. This overvoltage, analyzed in Mr. Holm's reference 2, is possible even without series capacitors and it occurs if a lightly loaded or unloaded transformer is left at the far end of a long line and only in certain regions of length. If extra margin is required in capacitor dielectric, it will be desirable for stability reasons not to have the capacitor protective equipment function because of swing currents after fault removal.

To answer the specific questions raised in Mr. Holm's discussion:

1. Excitation as needed to hold the indicated voltage was supplied from the infinite system at the receiving end. In the actual case this might very well be done in part by synchronous condensers in the system near the receiving end of the line.

2. The receiving system reactance used in the study represented the low-voltage system including generator reactance. Since it was reduced from 0.15 per unit to 0.10 in going from steady-state to transient conditions, some generator reactance was included. It was not necessarily the intent to call the receiving system infinite. Its inertia was so represented simply for convenience, and any receiving-system inertia may be considered, because in cases such as the ones studied where the receiving-end mechanical torque is the negative of the sending-end mechanical torque (neglecting losses), two systems of inertias H_1 and H_2 may be represented as one system of inertia H_0 against an infinite system, where $H_0 = H_1H_2/(H_1+H_2)$.

3. All curves in Figures 1, 2, and 3 will cut the zero margin line at two points, indicating two power limits for any value of compensation. The upper limit is dictated primarily by the transfer reactance, consisting of machine, transformer, line, and receiving-system reactance. The lower limit occurs at low loads and is dictated primarily by excitation, or lack of it, because of the otherwise unused line-charging kilovolt-amperes at such low loads. It is this condition that causes the switching time curves showing the results of the transient studies to bend under at low loads.

Mr. Holm suggests a comparative study be made of series capacitors and intermediate synchronous condensers. In the paper by Miss Clarke and Mr. Crary listed as reference 3, the effectiveness of intermediate stations has been studied. They state, in part:

> "In order to obtain an appreciable benefit by this method and so make it of practical importance, the intermediate system or station must have a relatively large kilovolt-ampere rating (or low reactance) compared to the power transmitted. It has not been considered economical to install synchronous condensers for this purpose alone."

These statements are borne out by the curves in Figure 6 of their paper, which we may use for a brief approximate analysis. This figure shows that the steady-state-receiver power limit for a 600-mile 60-cycle conventional line is about 2.0 kv², and that it may be increased to about 2.5 kv² by use of one intermediate condenser of 0.50 per-unit reactance on a 2.5 kv² base. If we call $X_{equiv} = 0.80$ for a condenser, the condenser kilovolt-amperes required is $(0.80/0.50) \times 2.5$ kv² = 4.0 kv². If one takes the price ratio of capacitors to condensers of 8/5, for this size installation this condenser would buy $(5/8) \times 4.0$ kv² or 2.5 kv² of series capacitors, which, for a through load of 2.5 kv² (that the condenser would permit) or 2.5 per-unit amperes, would give a series capacitive reactance of per-unit kva/(per-unit amperes)² $= 2.5/(2.5)^2 = 0.40$. On a kv² base a 600-mile line has about $(0.8 \times 600)/1000 = 0.48$ per-unit ohms; hence the same investment would permit a reactance compensation of 83.3 per cent, which would increase the power limit of the line considerably above the 2.5 kv² afforded by the condenser. Extrapolation to 600 miles of the curves in Figure 4 of the present paper would permit an estimate of a steady-state limit of perhaps 5.0 kv^2 for terminal reactances somewhat smaller than those used in Figure 6 of reference 3.

Pilot-Wire Circuits for Protective Relaying—Experience and Practice

Discussion and author's closure of paper 43-27 by the AIEE relay subcommittee of the committee on protective devices, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, May section, pages 210–14.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): The collection of information on experience with pilot-wire circuits for protective relaying should lead to a more widespread employment of this form of protection in making possible marked savings in major equipment. In addition to the critical examination of the per cent availability of pilot-wire circuits, other factors affecting the over-all performance of the protective system may well be considered. Experience has shown that excellent reliability and freedom from false operations may be secured by using either privately owned or leased circuits, provided the following major requirements are met:

1. The relay equipment should be immune to false tripping (in the presence of load current alone) in case the pilot-wire circuit is momentarily short-circuited, open-circuited, or grounded, or in case moderate extraneous voltages are momentarily introduced. Directional-comparison schemes of pilot-wire relaying are inherently immune to such disturbances, and in some instances directional fault detectors using current and voltage have been used to interlock pilot-wire relays of the two-wire differential type.

2. Adequate supervision of the condition of the pilot-wire circuit should be provided to detect defects and remedy them before the relay equipment is called upon to operate. Continuous supervision has been found particularly valuable in this connection, and supervision against high-resistance grounds on the circuit has resulted in detecting incipient pilot-wire short circuits or open circuits some hours (or even days) in advance of the pilot-wire circuit failure (since a single ground does not interfere with operation).

3. Thorough and proved measures should be employed to prevent momentary disturbances to pilot-wire circuits caused by rise of ground potential, induction, or other causes which by their nature occur at the same time as a power-system fault for which the relays must discriminate.

Mention is made of the ability of the "newer schemes" to provide both phase and ground protection and also to broaden application to cover greater distances. It is believed that the subcommittee intends this discrimination between early three-wire a-c pilot-wire systems and more recent developments, rather than between d-c-operated directional comparison schemes which have been in general use since about 1930 and the two-wire low-energy a-c differential pilot-wire equipment appearing some seven years later. Even the most recent types of a-c two-wire differential schemes have been found to be limited to appreciably shorter lengths of pilot-wire circuits than can be used with d-c directional-comparison schemes. In a recent application it was therefore necessary to choose d-c pilot-wire relays on account of the length and characteristics of the available pilot-wire circuits.

The results of the subcommittee investigation indicate some confusion in the matter of guarding against rise of ground potential and this is a very important point in the prevention of pilot-wire disturbances concurrent with power-system faults. The following suggestions are offered:

1. The use of three-winding neutralizing transformers on the pilot-wire circuits is strongly recommended for the usual application.

2. Extreme caution is required in segregating all portions of the circuit on the station side of the neutralizing transformer from any equipment which might be at remote ground potential and vice versa.

3. The use of staged fault tests to ground is highly recommended for checking on the over-all effectiveness of voltage neutralization and discovering difficulties which might otherwise be obscured.

Some common causes of trouble in connection with rise of ground potential are:

1. Plain and simple confusion based on the misconception that "a ground is a ground."

2. Communication circuits in which a momentary interruption during a power-system fault may be of little consequence are sometimes not provided with neutralizing transformers or other potential gradient equipment. Operation of carbon protectors on these circuits during power-system faults may in effect by-pass the neutralizing transformer protection installed on pilot-wire circuits at the same station.

3. The overenthusiastic use of carbon protectors may nullify an excellent protective system.

4. Lack of thorough discussion may result in failure to co-ordinate the protective equipment of the power company and the communication company from which circuits are leased.

5. Common neutral distribution circuits, transmission-line ground wires, and so forth, may extend the field of station ground influence to an unsuspected extent and thus destroy the effectiveness of remote ground installations.

H. P. Sleeper (Public Service Electric and Gas Company, Newark, N. J.): This paper on pilot-wire relaying seems definitely in order for three reasons:

1. It has been several years since the subject has been discussed before this group.

2. This branch of the protective-relaying art is definitely rising in popularity on the list of relay-parade schedules.

3. The art of pilot-wire relaying has made decided advances in the last few years, its scope of application has been extended, and its economies of application have been enhanced.

Without attempting to review or analyze the history or development of this art, it seems to this commentator that three major factors have tended to popularize the use of this type of protection. The first is the development of schemes which enable standard relays to be used with pilot-wire interconnections to obtain high-speed differential protection of transmission circuits. Examples of this are the use of directional relays to compare fault-current flows at the ends of such a circuit. Another is the use of the pilot-wire channel to transmit blocking signals between sets of standard carrier-current relays when the latter encounter technical or economic difficulties in a particular application.

The second factor has been the development of a simple network type of pilot-wire relay which is small in size, economical to apply, simple to test in service, independent of potential connections, yet supplying phase and ground protection in a single unit, and which has proved very successful in operation.

A third factor is the developments which permit standard-gauge telephone circuits to be used as the pilot conductors. It is not many years ago that all relay schemes of this type required low-resistance pilot conductors, and such sizes as number 6 (B. & S. gauge) were commonly employed. Obviously, the economics were against the extension of such schemes, and long-line applications were not considered, the usual installation being for distances of fractions of a mile. Today circuit sizes as small as number 24 (B. & S.) are in use, and a circuit of 27 miles in length is reported in the subject paper. Much longer pilot circuits can be used, even with the small gauge telephone wires with certain relay schemes.

It is this observer's opinion that pilot-wire relay protection has a definite place in the art of protective relaying today. There arise from time to time certain applications where this general scheme is definitely superior economically to any other. This is usually where short sections of a system are to be protected. The degree of protection afforded is equal to that of any of the standard modern relays on such applications. On longer lines the chief competitors are distance relays and carrier relays. On lines up to 25 miles, the annual costs of pilot-wire relays and carrier pilot relays are not very different.

The writer is associated with an electric utility company which has in operation pilot-wire relay protection on approximately 70 miles of three-phase transmission circuits. Two circuits totaling 10 miles operate at 26 kv, and eight circuits totaling 60 miles operate at 132 kv. Leased pilot circuits are employed throughout. On most of the longer circuits it has been the practice to install neutralizing transformers in the pilot wires, for longitudinal voltage reduction. The leased circuits have had all branch tap circuits removed so as to minimize pilot circuit troubles. All of these circuits are in lead cable and are mostly underground. No heat-coil protection is used, but standard 300-volt gaps are installed on both wires at all intermediate central offices and at terminals. The pilot circuits are continuously and automatically supervised.

In choosing a protective-relay scheme for a given application there are many factors to be considered, involving the financial, engineering, and operating aspects. One of the latter factors which a relay engineer should not overlook in deciding between, for example, carrier pilot relays and pilot-wire relays, is the operating results reported for both types of protection. The figure of 96.5 per cent correct operation reported in this paper is good, but in this writer's opinion is not good enough. It does not equal the figures which have been reported frequently for other types of protection.

It is important to inquire further into the causes of the incorrect operations. It is unfortunate that the committee compiling this paper was unable to do this. No attempt was made to report all causes of incorrect operations, and the only troubles given are causes of interruptions to the pilot-wire circuits. These may or may not have been the causes of the wrong relay operations. Furthermore, no statement has been given as to the cause of the short circuits, open circuits, grounds, and so forth, on the pilot wires. This is very pertinent information and might have a decided bearing on one's choice of a system of relay protection, especially for application to long transmission circuits. This would be especially true if the causes of the pilot-wire trouble were found to be proportional to the physical lengths involved.

One of the causes which falls in the latter category in the writer's experience is the matter of pilot-wire troubles caused by what we classify as "interference by personnel." All of our pilot circuits are very plainly tagged in all central offices as being of the emergency type and not to be opened or tested without special permission. Yet several failures of the pilot circuits have resulted from neglect of such warnings at these locations. Furthermore, certain physical locations of pilot circuits, such as in manhole splices, render special identification of pairs practically impossible. Specifically, a total of 14 failures of pilot-wire circuits from this cause has been experienced in a total of 748 circuit-mile years of operation.

It can be stated therefore that the use of facilities of another company in the maintenance of devices which affect the operation of a protective-relay scheme obviously increases the personnel errors which are beyond the direct control of the operating company. This is one reason why fault-detector relays are installed sometimes on important installations to avoid incorrect operations caused by pilot-circuit failures.

This is not intended to disparage the co-operation of the engineers of the telephone company involved. On the contrary, their co-operation has been of the best, and conditions are definitely improving in our experience. Nor is it the intention in this discussion to imply that our experience with leased pilot circuits can be classed as unsatisfactory. In general the service has been very satisfactory, and in all fairness it should be stated that the difficulties described occurred mainly during the early periods of operation. It is intended only to point out that when utility operating engineers are deciding on protective schemes for important power-system units, the probability factor of such occurrences as described previously should be considered in the analysis.

E. L. Michelson (Commonwealth Edison Company, Chicago, Ill.): The survey which was made by the relay subcommittee included all outages of pilot wires, such as those caused by circuit rearrangements and replacement of cable in which the insulation has reached a dangerously low level. If the survey were limited to only those interruptions caused by an actual breakdown of the pilot wires while the main line was carrying load, the results would be much more favorable for pilot-wire relaying. The experience in our company indicates that less than five per cent of the total number of interruptions are those caused by pilot-wire breakdowns. All the others are planned interruptions, which means that the main transmission line is deprived of the pilot wires only during those periods when the need for the main line is minimum. Therefore, in this respect, the continuity of pilot-wire circuits is much better than that indicated in the report of the subcommittee.

W. K. Sonnemann: Mr. Brownlee's discussion mentions the use of three-winding neutralizing transformers as a remedy for induction problems. It should be remembered that there is also available a scheme using a two-winding neutralizing transformer which is equivalent to a three-winding neutralizing transformer for differences in station ground potential and which handles longitudinal induction as well. As a matter of fact, the protection of pilot-wire circuits has recently been discussed in a paper "Protection of Pilot Wire Circuits" by E. L. Harder and M. A. Bostwick in AIEE TRANSACTIONS for September 1942.[1]

There is a difference in opinion, apparently, as to how the relay used with the pilot wire should perform in the event of a faulty pilot wire. Some operators purposely set the relays below load currents for inherent supervision of the pilot-wire circuit, so that if the pilot circuit is faulty, the circuit breakers will trip.

It was pointed out that phase and ground-fault protection over only two pilot wires has been available by the directional comparison means for a longer period of time than the new two-wire a-c schemes. Perhaps the recent popularity of the two-wire a-c schemes is explained on the basis of their simplicity compared with most directional comparison schemes.

In a continuing investigation of this subject, I feel that the causes of pilot-wire short circuits, open circuits, and grounds should be looked into as mentioned by Mr. Sleeper. Since this investigation primarily concerns the pilot wire itself as a unit in the protective scheme, I do not believe an investigation of faulty operations of the over-all scheme which may occur and which are attributed to defects or accidents not directly associated with the pilot wire should be considered by this working group.

REFERENCE

1. PROTECTION OF PILOT-WIRE CIRCUITS, E. L. Harder, M. A. Bostwick. AIEE TRANSACTIONS, volume 61, 1942, September section, pages 645-52.

A New Generator Differential Relay

Discussion and author's closure of paper 43-15 by A. J. McConnell, presented at the AIEE national technical meeting, New York, N. Y., January 25-29, 1943, and published in AIEE TRANSACTIONS, 1943, January section, pages 11-13.

E. L. Harder (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The paper by Mr. McConnell is commendable in presenting a brief yet comprehensive introduction of a new member of the relay family circle. If applied within its proper application limits it should prove a useful member.

The paper could have been even shorter and far more valuable had it omitted raising the question of a blind-spot danger with a variable slope relay. Every relay has its limit of application, this new relay included, and the description of what a relay may do at currents far beyond any at which it is ever applied is neither an asset nor a liability to the relay, and certainly no hazard to the application.

Variable slope means simply that as the current through the operating and restraining windings is increased, the restraining torque increases in percentage. At small currents the restraint may be negligible, at larger currents it may amount to 10 per cent of the operating torque, at still larger currents 25 per cent, 50 per cent and so on. Obviously, if the current were carried on up to a sufficiently large value, and if the restraining electromagnets did not undergo saturation in the meantime, a point might be reached where the restraining torque was in excess of the operating torque so that the relay would not operate. The whole question is where does this point occur, if it occurs at all?

One variable slope differential relay is designed for operation on secondary currents, either symmetrical or asymmetrical, having an rms symmetrical component up to 100 amperes, whereas the crossing point occurs at or above 150 amperes. As the relay is applied with current transformers that result in not over 100 secondary amperes (rms symmetrical component) in order to secure uniformly high speed operation, there is absolutely no danger of misoperation from this source whatsoever. This relay was developed several years ago for bus protection and to the best of my knowledge has a perfect record of operation both on staged tests and in the field.

W. K. Sonnemann (Westinghouse Electric and Manufacturing Company, Newark N. J.): The author of this paper has solved an old problem in a somewhat different manner. The use of product restraint combined with the directional feature is interesting and does appear to have limited possibilities. However, it must not be concluded that this is the immediate and simple answer to all of the differential relay problems. It is still not possible to apply generator differential relays, particularly of the high-speed type, without due consideration being given to the current transformer character-

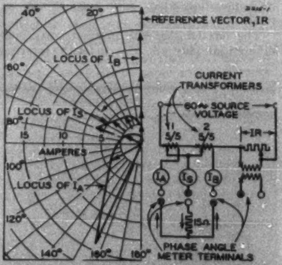

Figure 1. Performance of mismatched current transformers when common burden is high

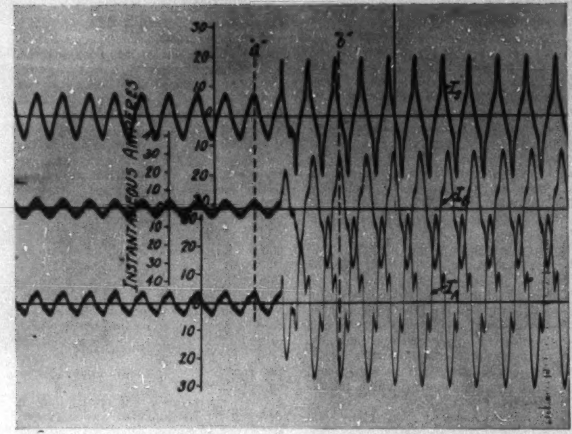

Figure 2

istics. If this relay is applied using current transformers which are not matched within definite limits, there is the hazard that it will not trip for the internal fault condition. This paper would be materially improved if the author had stated the limits permissible in the variation of the current transformer characteristics. Such limits have been given before for a relay of the summation restrained variable percentage type.[1]

It is felt that the author has been unduly pessimistic in dealing with the case of the summation restrained relay, particularly one having variable percentage characteristics. He has conveniently eliminated the scales from Figure 2 of the paper so that the crossing point of the operating and restraining coil torque curves can be anywhere. Obviously, if the relay designer does not know about this point, if it actually exists, or if he neglects it altogether, there is likely to be trouble. However, in the summation restrained variable percentage differential relay for generator protection,[1] this crossing point to all practical purposes does not exist. A relay of this type is just as applicable now as it ever was within the limits of its application which have been clearly set forth.

The hazard that the product restrained differential relay will not trip for the internal fault condition is severe in the case of current transformer reversal. In a paper on bus differential protection[2] presented in January, 1939, it was disclosed that when two dissimilar current transformers both feed current to a common burden which is high, there is the possibility that the stronger of the two current transformers may actually reverse the current through the secondary of the weaker. If this should occur with the product restrained relay, then the relay would view the fault as an external fault condition so that the restraining coils would actually produce restraining torque instead of the operating torque which was anticipated in the design of this relay. Since the relay has been designed with a wide margin of safety for the external fault condition, and since the internal fault would look like an external fault to the relay, it would not trip.

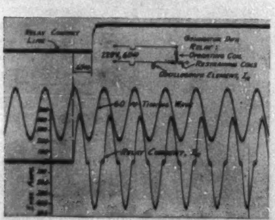

Figure 3

When the phenomenon of current transformer reversal was first observed, it was deemed worthy of further investigation. Accordingly a test set-up was made in line with the diagram of connections on Figure 1. Here two dissimilar current transformers 1 and 2 are arranged to feed current through a 15-ohm resistor. The terminals indicated in the current circuits for the phase angle meter are the places in the circuit where the current coils of the phase angle meter were successively connected to determine the phase angle positions of the currents flowing. In each case, the current was referred in phase position to the vector potential drop, *IR*. The polar curve shows that at light secondary currents, $I_A = 1.25$ amperes, $I_B = 1.25$ amperes and $I_S = 2.5$ amperes, the two secondary currents from the test transformers add together to produce I_S as expected in normal operation. However, as the primary current was increased, the ability of the weaker transformer to do its part in forcing current through the common burden was exceeded. This resulted in the secondary current of transformer number 1 being swung around in the hooked curve indicated as the locus of I_A. At secondary currents of $I_B = 14.5$ amperes, $I_A = 14$ amperes, it will be noted that the curve shows these currents to be 155 degrees out of phase. This reversal was accomplished with a 15-ohm burden, whereas the operating coil of the product restrained relay admittedly reaches a maximum impedance of 65 ohms.

A question may be raised concerning the accuracy of the phase angle meter readings under the distorted conditions involving saturation as shown in Figure 1. With this in mind, and in order to substantiate the conclusions reached from a study of Figure 1, an oscillogram was taken as indicated in Figure 2. This oscillogram was taken under the same conditions as Figure 1. For the left-hand portion, the currents were light so that a positive peak of I_A lines up with a positive peak of I_B, the sum of the two producing a positive peak of I_S as in normal operation. This is indicated at "*a*" on the oscillogram. The current through the primary of the current transformers was then suddenly increased and it will be noted that at point of time "*b*," a positive peak of I_B substantially lines up with a negative peak of I_A. This oscillogram substantiates the general conclusions reached from a study of Figure 1.

It was stated earlier that the crossing point of the torque curves of the author's Figure 2 does not exist to all practical purposes for the summation restrained relay with variable percentage characteristics. The oscillogram, Figure 3, gives adequate proof of this point. A source of 220 volts 60 cycles was connected directly across the operating coil of this relay in series with a restraining coil, resulting in the relay current I_R as shown. Prompt operation of the relay was obtained in 0.65 cycles as indicated. The wave form of the relay current is not sine wave because of the saturating nature of the operating coil impedance. However, the peak-to-peak value of the relay current is the same as a sine wave of 310 amperes rms would have. A current of 310 amperes is approximately equal to the maximum current which could be fed back into a faulted generator by eight other generators of the same size connected to the same bus. As a matter of fact, not all current transformers can develop a current of 310 amperes in their secondary circuits. It is thus shown that there is no need to fear nonoperation of the summation restrained variable percentage differential relay during an internal fault.

Summarizing, it is quite possible for current transformer characteristics to be mismatched to the extent that the product restrained differential relay will not trip for the internal fault condition. The degree of mismatch which can be tolerated should be stated. Similar limits of application have been specified before for summation restrained variable percentage relays so that it should be possible to do so here. Secondly, the discussion of the limitations of the summation restrained relay is more theoretical than practical and should not be taken seriously.

REFERENCES

1. A HIGH-SPEED DIFFERENTIAL RELAY FOR GENERATOR PROTECTION, W. K. Sonnemann. AIEE TRANSACTIONS, volume 59, 1940, November section, pages 608–12.

2. CONSIDERATIONS IN APPLYING RATIO DIFFERENTIAL RELAYS FOR BUS PROTECTION, R. M. Smith, W. K. Sonnemann, G. B. Dodds. AIEE TRANSACTIONS, volume 58, 1939, June section, pages 243–9.

A. J. McConnell: Both Mr. Harder and Mr. Sonnemann appear to have misunderstood the discussion of the limitations of variable slope with summation restraint. The paper does not imply, let alone state, that there is a practical limitation in regard to the particular relay previously described by Mr. Sonnemann.[1] The variable slope characteristic of that relay was used (curve *B* of Figure 1 of the paper) as an example of a relatively mild variable slope characteristic. A review of the paper will show that particular reference was made to the sharply breaking characteristic of curve *C* of Figure 1 in pointing out the limitations of variable slope with summation restraint. Nevertheless, since the subject has been raised, it is interesting to note that, according to Mr. Harder, the relay described by Mr. Sonnemann does have a blind spot above approximately 150 amperes. However, for some reason this does not appear to be borne out by Figure 3 of Mr. Sonnemann's discussion.

Mr. Sonnemann objects to the lack of scales in Figure 2 of the paper. This illustration was used to show the reason for the admitted limitation of variable slope with summation restraint. Since Figure 2 of the paper accomplishes its purpose, no definite values for the co-ordinates are necessary.

Both Mr. Sonnemann and Mr. Harder criticize the lack of stated limits permissible in the variation of the current transformer characteristics. This criticism is certainly justified, since it is always possible to have a combination of high burden and poor current transformers capable of causing the failure of any differential relay system. Before stating the limits, however, Figure 1 of Mr. Sonnemann's discussion requires comment.

Since the current reversal depicted by Figure 1 of Mr. Sonnemann's discussion is a function of burden and current transformer exciting current, both of these factors must be considered. First, the burden of the operating circuit of the product restraint relay is, in the significant range of operating currents, much lower than the 15 ohms used to obtain the data for Figure 1 of Mr. Sonnemann's discussion. Although the maximum burden at low current values is, as stated in the paper, 65 ohms, the sharp saturation of the operating circuit reduces the burden rapidly as the current is increased, the burden being six ohms at five amperes and less than one ohm at 60 amperes. In fact, the operating circuit voltage can increase very little above 50 volts. Secondly, current transformer 1 employed by Mr. Sonnemann is a rather poor current transformer. For example, its exciting current begins to increase rapidly at about 37.5 volts (15 ohms, 2.5 amperes) and has about 12 amperes exciting current at 70 volts. It is thus readily evident from both of these factors that the results obtained in Mr. Sonnemann's test have little significance in the case in question.

Inasmuch as such current transformer data as core area, secondary resistance and exciting current are either awkward to apply or seldom available, it is advisable to set limits based on more readily obtainable information such as ratio correction factor on overcurrent. This is permissible since ratio correction factor is also a function of current transformer exciting current. Accordingly, up to a total internal fault current of 150 amperes (secondary basis), the product restraint differential relay should not be used with current transformers having a ratio correction factor of greater than 1.13 at 20 times rated primary current and wye burden (15 volt amperes, 90 per cent power factor). This limit corresponds approximately to the poorest current transformer recognized for relaying purposes by the American Standards Association, namely 10 *H* (or *L*) 50.[2] The above current limitation disappears, for all practical purposes, if the current transformer is in the next higher accuracy class. If one current transformer is just within the 10 *H* (or *L*) 50 classification and the second current transformer has better characteristics, it is preferable to use the latter on the neutral side of the machine in which case the current limitation is a function of the better transformer.

Because of the wide margin of safety on through faults, and with the lower limit of current transformer accuracy set above, no further limitations are necessary. In other words, if one current transformer just meets the above accuracy limitation, the other current transformer may be perfect.

REFERENCES

1. Same as reference 1 of Mr. Sonnemann's discussion.

2. American Standard *C57.1*. Transformers, Regulators, and Reactors, 1942.

Correlation of System Overvoltages and System-Grounding Impedance

Discussion of paper 43-5 by working group on correlation of system-grounding impedance of the AIEE committee on protective devices, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, January section, pages 17–24.

R. D. Evans (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The report of the working group on correlation of system-grounding impedances shows that there are many causes of system overvoltages. A very useful part of the report is the list of causes of overvoltage and the discussion of each cause, which facilitate study of particular cases of experienced or anticipated trouble. The phenomena involved in production of overvoltages are often exceedingly complex, and only a few types have, from the present point of view, been examined adequately. The committee report will serve a useful purpose in defining the present status of available information on particular causes of system overvoltage and correlation with system characteristics.

The working group was organized for the purpose of clarifying the relations between system overvoltages and system-grounding impedances. The only characteristics of system that so far have been found useful from the standpoint of correlation with system overvoltages are the sequence-impedance ratios. More particularly, these relations include the ratios of the zero-sequence to positive-sequence series reactance, the ratio X_0/X_1, and to a lesser extent the ratios of the sequence shunt capacitances C_0/C_1, and their relation to X_1. It is hoped that one result of this report will be to stimulate interest in the correlation of system overvoltages with the sequence-impedance ratios. Such information will permit better evaluation of the frequency of occurrence of unusual systems having sequence-impedance ratios that are favorable to the production of high system overvoltages.

From a practical standpoint one result of the report should be the improved recognition of the cases under which lightning arresters may be overstressed on systems operating with either isolated-neutral or with high-impedance grounding. Another result of the report should be the increased recognition of an inherently necessary difference in system protection level for isolated-neutral and grounded-neutral systems. The difference in protection level of systems for the same circuit-voltage rating is a factor frequently overlooked in the cost comparison of systems differing in grounding characteristics.

Edward Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The subject of system overvoltages is pertinent to the use of lightning protective devices. As stated in the report, the subject may be complex. In many cases systems or parts of systems require individual study in order to make the best use of the data. However, if such a study is made, savings may be possible in lightning protective devices, and the best practical protective levels may be achieved, as pointed out in a contemporary paper, "Lightning Protective Devices in Wartime."[1] At the same time the knowledge of system voltage conditions to ground will also prevent too optimistic application of arresters whose ratings might be so low that they are likely to fail from such system overvoltages and thereby partly defeat the purpose for which they are installed—prevention of outages. Failure to evaluate the risk of system overvoltages in excess of arrester ratings engenders arrester failures. Thus the report has much value at this time, even if considered only from the standpoint of lightning protection. It calls attention to the factors that influence system voltages and therefore facilitates getting the best over-all operation of systems during lightning storms.

I would like to suggest that the curves of Figures 1 and 2 of the paper would be more readily useful if the voltages were expressed in terms of normal circuit voltage instead of in terms of normal voltage to neutral, since we are more accustomed to thinking of systems in terms of circuit voltages. If the curves were expressed in these terms, a calculation would be avoided and the voltages impressed on lightning arresters made more quickly apparent.

REFERENCE

1. LIGHTNING PROTECTIVE DEVICES IN WARTIME, I. W. Gross. AIEE TRANSACTIONS, volume 62, 1943, April section, pages 173–5.

The Effect of Current-Transformer Residual Magnetism on Balanced-Current or Differential Relays

Discussion and author's closure of paper 43-28 by H. T. Seeley, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, April Section, pages 164–9.

E. H. Klemmer (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper is an interesting treatise on residual magnetism in current transformers. However, its overemphasis of the adverse effect on relays may cause protection engineers unnecessary alarm.

To illustrate, I would like to cite a 1937 application of differential protection to steel mill motors provided with dynamic braking, wherein only one set of the differentially connected current transformers was subjected to d-c current during the braking action. On learning of this unusual application I had grave fears that the differential protection would not prove satisfactory on account of the residual-magnetism effect. As the equipment was then ready to be shipped, we decided to test it. Much to our surprise, we could not make the differential relay misbehave, even with extremely rapid transition from direct current through one set of current transformers to alternating current through both sets in series, or even when the most sensitive setting was used on the *CA* differential relay; namely, 0.04-ampere pick-up and the $2\frac{1}{2}$ per cent slope characteristic. For each of numerous tests the relay showed positive restraint action. Oscillograms showed substantially the same small differential current because of current-transformer error, whether one set of the 1,000/5-ampere current transformers had been magnetized previously with direct current up to 730 amperes, or whether both sets of current transformers had been completely demagnetized. In other words, the residual magnetism in this extreme case had no perceptible effect on the relay action, nor did the oscillograms show any unusual differential transient current. In five years of service, involving highly repetitive residual magnetism in one set of the differentially connected current transformers, there has been no report of trouble.

In general, the residual-magnetism effect on high-speed relays is much less than the maximum fault-current d-c component saturation effect, for which the relay scheme must be designed. Calculations for the latter may incorporate possible residual magnetism in a current transformer. However, these calculations generally include margins that permit neglecting the residual-magnetism effect entirely.

C. A. Woods, Jr. (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Engineers, familiar with the application of current transformers, have usually considered the adverse effects of residual magnetism in transformer cores in relation to resultant changes in meter registration because of the increased transformer errors. Marshall and Langguth, in their paper on "Current-Transformer Excitation Under Transient Conditions,[1] have shown the effects of residual magnetism on the instantaneous ratio errors of current transformers in connection with their investigation of current measurements under such conditions. Of particular interest were the oscillograph records showing that residual flux could either decrease or increase the transformer-error current under transient conditions, depending upon the relative directions of the residual flux and the unidirectional flux required by the d-c component of primary current.

From the many investigations[1–5] of unidirectional flux in current-transformer cores and the resultant effects on high-speed relay operation, the need for special consideration of the current transformers, relays, and circuit constants has been accepted. This paper emphasizes the need for such precautions as covered by the material given under the subheading, "Effect on Relay," wherein it is shown that difficulties were encountered in preventing incorrect relay operations, even without the presence of residual flux. A close study of the restraining coil and primary current waves shown in Figure 9 of the paper would lead one to expect that had the primary current in the test portrayed by Figure 9b contained a larger d-c component, the relay might have operated.

Even in the case of generator differential protection involving only two sets of current transformers of identical design, consideration must be given to the characteristics of the transformers, high-speed relays, and circuits in relation to transients expected. Current transformers, produced commercially, of same design and having the same magnetic history could have sufficient differences in error currents at saturated densities to cause operation of nonrestrained, sensitive relays under severe transient conditions.

From the data given in the paper, it is apparent that it is possible with certain types of relays to obtain incorrect operation which can be attributed to residual flux in the current transformer. Because of the demagnetizing effects of large error currents and normal load currents, the effects of the resultant residual flux are usually small compared to the errors which are caused by the unidirectional flux from the d-c component of asymmetrical primary currents. Hence, we agree with the author's conclusions, that in many high-speed relaying schemes each of the component parts, current transformer, relay, secondary circuit, and primary circuit must be checked to insure that in combination they will function properly under transient conditions. When satisfactory operation under these conditions is obtained, it would be only marginal cases wherein it would be necessary to take into account the additional effect of residual magnetism.

REFERENCES

1. CURRENT TRANSFORMER EXCITATION UNDER TRANSIENT CONDITIONS, D. E. Marshall, P. O. Langguth. AIEE TRANSACTIONS, volume 48, 1929, page 1464.

2. CURRENT TRANSFORMERS AND RELAYS FOR HIGH-SPEED DIFFERENTIAL PROTECTION, WITH PARTICULAR REFERENCE TO OFFSET TRANSIENT CURRENTS, E. C. Wentz, W. K. Sonnemann. AIEE TRANSACTIONS, volume 59, 1940, August section, pages 481–8.

3. NEW CURRENT TRANSFORMER FOR BUS DIFFERENTIAL PROTECTION, L. F. Kennedy, A. T. Sinks. AIEE TRANSACTIONS, volume 60, 1941, pages 1180–7.

4. TRANSIENT CHARACTERISTICS OF CURRENT TRANSFORMERS DURING FAULTS, C. Concordia, C. N. Weygandt, H. S. Shott. AIEE TRANSACTIONS, volume 61, 1942, May section, pages 280–5.

5. LINEAR COUPLERS FOR BUS PROTECTION, E. L. Harder, E. H. Klemmer, W. K. Sonnemann, E. C. Wentz. AIEE TRANSACTIONS, volume 61, 1942, May section, pages 241–8.

H. T. Seeley: In closing, I wish to refer to earlier work on the effect of transients on high-speed relays, reported in a paper entitled "Experiences With a Modern Relay System" and presented by G. W. Gerell in 1936.[1] His conclusions indicate the same remedy as mine; namely, a delay of one cycle or slightly more, to avoid false operation under transient conditions. Mr. Whitlow's collaboration in that earlier work provided a good background for the field test mentioned in my paper.

In answer to questions by C. L. Smith, the mutual inductance between the two current transformer secondary circuits was not present during the error current tests using the connections of Figure 7, but it was present during the relay tests shown in Figure 9; and the steady-state current transformer secondary currents in Figure 1 were equal in spite of the apparent difference resulting from different calibration on the two oscillograph elements.

Mr. Klemmer's report of factory tests on differential protection using a *CA* relay corroborates the paper's conclusion that safe operation will be obtained with relays having sufficient delay to permit the error current to reduce the residual flux. The oscillograms in the paper show that the phenomenon is unimportant after the first half cycle, and lead to the conclusion that induction disk relays should be safe from false tripping from this cause. Mr. Klemmer's statement on the relative importance of residual magnetism and maximum d-c component evidently applies to bus differential systems, and not to balanced-current protection where the d-c components of the two currents are equal for an external fault and are also usually small because of line resistance.

The two references cited by Mr. Woods are welcome additions to those listed in the paper. The oscillograms in the Marshall and Langguth paper are especially interesting, and their circuit for measuring error current provides greater accuracy, although the statement that "on tests which do not come up to the knee of the saturation curve, the residual will probably be so slight that demagnetization will not be required" might be re-examined on the basis of the test values of the residual flux shown in Table II of my paper.

The current in Figure 9b was initiated at the instant of zero voltage, but the time constant of the total circuit including the two secondary burdens was too short to give a d-c component of importance.

REFERENCE

1. EXPERIENCES WITH A MODERN RELAY SYSTEM, G. W. Gerell. AIEE TRANSACTIONS, volume 55, 1936, October section, pages 1130–6.

A Useful Equivalent Circuit for a Five-Winding Transformer

Discussion and author's closure of paper 43-2 by L. C. Aicher, Jr., presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, February section, pages 66–70.

W. M. Johnson (General Electric Company, Pittsfield, Mass.): The equivalent circuit described by Mr. Aicher for the five-winding transformer is an interesting extension of the commonly used three-winding equivalent circuit and the four-winding equivalent circuit given by F. M. Starr.[1]

Since the circuit is symmetrical in form, and the choice of subscripts is purely arbitrary, one would expect the expressions for the impedance links to be symmetrical. That is, if an expression for Z_6 has been obtained, it should be possible to write the expressions for the remaining mesh links by simply rotating the subscripts of the various through impedances. In the expressions given for the mesh impedances, this symmetry is not readily apparent. However, these may be rewritten in the following form in which the inherent symmetry is obvious:

$$Z_6=\frac{(MQL+QLP+LPS+PSM+SMQ)}{2PS}$$

$$Z_7=\frac{(MQL+QLP+LPS+PSM+SMQ)}{2SM}$$

$$Z_8=\frac{(MQL+QLP+LPS+PSM+SMQ)}{2MQ}$$

$$Z_9=\frac{(MQL+QLP+LPS+PSM+SMQ)}{2QL}$$

$$Z_{10}=\frac{(MQL+QLP+LPS+PSM+SMQ)}{2LP}$$

A number of years ago the writer of this discussion had occasion to analyze this circuit, and these symmetrical expressions were found to be more convenient for calculations and for analyzing the nature of the circuit.

The author of the paper has listed as one of the objectives of an equivalent circuit the elimination of negative branches in the circuit to facilitate its use on a calculating board. Since there is a freedom of choice as to which transformer windings are assigned to the various terminals of the network, there exists a number of different equivalent circuits, any of which will represent the transformer, depending on the order in which the various windings are assigned to the terminals of the circuit. It would be interesting to know if the author has established any criterion as to whether negative-impedance links can be avoided by selecting properly the order of the windings. For the case where resistances are neglected, it is apparent from the symmetrical expressions for the mesh impedances that in order to avoid negative links each of the terms P, S, Q, L, M must be positive. However, there appears to be no obvious criterion, except by trial and error in specific cases, to indicate whether it is possible to make these terms all positive by proper selection of the order of the windings. For the general case in which complex quantities are used, the possibility of establishing any such criterion seems even more remote. It should be borne in mind, however, that the presence of negative-impedance elements does not impair the usefulness of the circuit for purely mathematical calculations.

Reference

1. Equivalent Circuit of a Four-Winding Transformer, F. M. Starr. *General Electric Review*, March 1933, page 150.

W. C. Sealey (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): This paper fills a need which existed in the field of equivalent circuits for transformers. The development of equivalent circuits for transformers was gradual; equivalent circuits for two-winding transformers started the progression. Later the equivalent circuit for a three-winding transformer appeared, and afterwards the equivalent circuit for a four-winding transformer. Now the author has presented the equivalent circuit of a five-winding transformer.

The equivalent circuit presented is little more complicated than the equivalent circuit which was found necessary for a four-winding transformer, since the change in the number of elements in the circuit was only from eight to ten.

There have been occasions where the equivalent circuit of a five-winding transformer was extremely useful, and in a few cases the circuit of a six-winding transformer could have been used to advantage.

As a subject for a future paper the development of the equivalent circuit for a six-winding transformer is suggested. The development of the equivalent circuit of a six-winding transformer introduces sufficient additional complications so that it is an interesting problem and one requiring the exercise of ingenuity for its solutions. Such a circuit will require six points of entry, corresponding to the six windings of the transformer, and will have 15 independent elements in order that the 15 independent measurable impedances of the transformer may be simulated. The development of the circuit of a six-winding transformer would represent an extension of useful equivalent circuits.

L. C. Aicher: The symmetrical expressions suggested by W. M. Johnson are equally as applicable as those presented in the paper. If equations 38 to 41 inclusive are expanded and if terms are collected in the proper order, the expressions given by Mr. Johnson result.

A criterion for selecting the order of the windings to eliminate negative-impedance links is not easily established. It is chiefly a matter of inspection and cut and try. However, one concept that is beneficial though not infallible is to establish the windings in descending order of their mutual reactances after such have been adjusted to a common kilovolt-ampere base.

W. C. Sealey's suggested desire for an equivalent circuit of a six-winding transformer is corroborated by the author.

Two typographical errors have shown up which the author has not previously noticed. Equation 23 should read:

$$M=Z_{14}+Z_{25}-Z_{15}-Z_{24}$$

In the example, the solution of 3, the expression for the current in link 7 due to load current on winding 1 should read:

$$I_7=I_8=I_9=I_{10}=\frac{Z_6}{Z_t}I_1$$

A New Type of Adjustable-Speed Drive for A-C Systems

Discussion and authors' closure of paper 43-3 by A. G. Conrad, S. T. Smith, and P. F. Ordung, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, January section, pages 7–10.

Edward Bretch (The Century Electric Company, St. Louis, Mo.): This type of adjustable-speed a-c motor was disclosed in patent number 839,935, issued to the late B. G. Lamme in 1907. Latour also worked on this problem.

While there is no desire to detract from the value or interest in this paper, it seems that this type motor could hardly be classed as a new type. It is well known to those who have followed the development of the single-phase motor.

P. W. Robinson (General Electric Company, Schenectady, N. Y.): The wide demand for small adjustable-speed motors for industrial purposes and the extensive progress made in lower-cost voltage-control devices in recent years, make a renewed discussion of small a-c commutator motors very timely. Such a motor must compete with induction motors equipped with numerous forms of mechanical adjustable-speed drive, with small Ward-Leonard-type d-c equipments, and with Thyratron tube control of d-c motors. To meet this competition, the a-c commutator motor must have acceptable commutation and a simple low-cost method of speed control.

This paper presents a simpler and more detailed explanation of one of the adjustable-speed arrangements for single-phase a-c commutator-type motors than I have found in the earlier literature.

Dr. Alexanderson in his 1909 AIEE paper, "Repulsion Motor With Variable-Speed Shunt Characteristics," mentions both the armature control and field control systems, but the speed torque curves published are only obtained by field control.[1] However, he did not find it necessary to use a two- or three-phase power supply but used the voltage at the terminals of the exciting winding to supply the regulating transformer.

Mr. Maier's 1911 AIEE paper, "Methods of Varying the Speed of Alternating-Current Motors," includes some mathematical treatment but no vector diagrams or tests.[2]

Mr. Lamme in his discussion of Mr. Maier's paper gives no mathematical treatment, vector diagrams, or tests but did report that a 20-hp motor controlled by armature voltage and operated from three-phase power with an adjustable transformer was in commercial use for 18 months. He also stated that several other motors of this type were in commercial operation.

Any evidence available on the subject of essential constancy of armature impedance Z_a mentioned in the paper would be of interest. With three-phase commutator motors having line frequency voltage applied to the commutator there is more variation of armature reactance with speed than could be ignored in designing a motor and control or predicting characteristics.

In regard to the connecting of the compensating winding either in series with the armature or short-circuited, it occurs to me that if this winding were connected in series and provided with a small excess over the armature turns, the resulting overcompensation should help with the rotational element of commutation.

References

1. Repulsion Motor With Variable-Speed, Shunt Characteristics, E. F. W. Alexanderson. AIEE Transactions, volume 28, 1909, pages 511–21.

2. Methods of Varying the Speed of Alternating-Current Motors, Gus A. Maier. AIEE Transactions, volume 30, 1911, pages 2455–84.

A. G. Conrad, S. T. Smith, P. F. Ordung: It is not our intention to repeat experiments performed and reported by other investigators. Our presentation of this paper was not done with any oversight or disregard of work in the field done by others. We do not wish to detract from merits of work done by others. However, in view of the comments by Mr. Bretch, it might be that some of the differences between the motor described in our paper and that patented by Mr. Lamme are worthy of consideration.

In B. G. Lamme's patent of 1907, number 839,937, he states on page 1, lines 26–35:

> "the armature circuit may be, and is ordinarily, made with but little self-induction, so that the current in the circuit lags but little behind the electromotive force impressed upon it."

Our motor does not behave according to this description. In the machine that we have described the magnitude and direction of the armature current are dependent upon the magnitude and direction of the generated voltage. The armature current is not held in time phase with the field current. In fact the armature current shifts approximately 90 degrees as the load on the machine is changed from no-load to full-load.

On page 1, lines 35–39, Lamme states:

> "It is obvious, then, that if the electromotive forces applied to the two circuits (armature and field) differ 90 degrees in phase the currents in the armature and field-magnet circuits differ but little in phase."

This statement is not applicable to the motor that we have described. Here again Mr. Lamme has failed to recognize the presence of a generated voltage in the machine.

In lines 68–71, page 1, Mr. Lamme states that his motor takes small armature currents at no-load. This feature is not characteristic of our machine. Motors of the type that we have described may be made to correct power factor to the full extent of their capacity at no-load.

In lines 43–54, page 2, he states that if the magnitude of the impedance of the field circuit is charged by external means without charge of the field circuit power factor,

> "then the speed of the motor may be varied without varying the phase relations with respect to each other of the currents in the armature and field-magnet windings—that is, without varying the power factor of the motor."

In lines 95–105, page 1, Mr. Lamme implies that, when properly adjusted, speed charges for his motor do not affect power factor of operation for any given constant torque load. In our machine this is not the case for if the motor is delivering torque, a charge in speed is accompanied by a charge in power factor of operation.

Seven out of ten claims of this patent deal with control of speed by changes in field current. The authors are somewhat surprised that Mr. Lamme did not investigate this method more carefully. A careful investigation of this method would have revealed that the speed-regulation of the motor varies indirectly as the square of the field flux. Thus this method is of little value because of the large speed-regulation for weak fields. The other claims in the patent refer to changes in speed by changes in armature circuit impedance.

Throughout the patent Mr. Lamme has neglected to recognize the presence of a generated voltage in the armature. This is also true in other writings of Mr. Lamme. In discussing a paper written by Mr. Maier, AIEE, 1911, Part III, page 2488 in lines 3 to 8, Mr. Lamme refers to a motor such as he has patented and states:

> "With this arrangement the field current or field flux is still 90 degrees behind the exciting electromotive force but as the latter is practically 90 degrees out of phase with the armature electromotive force, the field or exciting flux will thus be practically in phase with the armature current."

It is difficult to understand how Mr. Lamme could have overlooked the presence of the generated voltage in this armature circuit. His statements reveal that it is his concept that the armature current is determined by only the applied voltage and the armature impedance, which is not the case. If we could disregard his statements and produce conditions that he has described for his motor, the generated voltage of the motor would be in direct opposition to the applied voltage. Under these conditions the motor would have a constant power factor and would draw large lagging currents in its armature circuit. Such a motor would be exceedingly large and impractical.

The virtue of the motor that we have described comes largely in the adjustments made on the machine that make it correct power-factor at no-load and to take its full load current essentially at unity power factor, thereby obtaining the greatest possible output from a given current supply.

Mr. Lamme and Mr. Latour are only a few of the hundreds of investigators that have worked on this problem.

Mr. Robinson's reference to comparative costs of this motor and other adjustable-speed systems should be considered not only from the standpoint of the initial cost of the units, but also of their operating costs. If the cost of this one motor and its controller be compared with cost of other adjustable-speed units, it is evident that the initial cost will be from one-half to one-fourth that of many of the systems now being sold and used. Likewise the over-all efficiency of the motor that we have described can be made much higher than the over-all efficiency of the systems requiring three or four units. This is undoubtedly the major advantage of the system over such drives as the Ward-Leonard or the Rototrol.

Direct comparisons between the three-phase motor described in our paper should not be made with a single-phase machine such as described by Dr. Alexanderson. The machines operate on different principles and their power-factor and commutation characteristics are different.

A thorough investigation of the constancy of the armature impedance Z_a for all speeds has not been made by the authors. However, curve 1 of Figure 4 of the paper was calculated from the constants of the motor. The proximity of this curve to the encircled points obtained experimentally indicates little error in the prediction of one speed-torque curve. The constancy of this armature impedance is dependent not only on the speed adjustment, but also on the degree of saturation produced by the main field. Slight changes in Z_a for different speeds are caused by the change in time of commutation of the coils undergoing commutation. Since the voltages induced in the coils undergoing commutation in this motor are transformer voltages and not the sum of the transformer and speed voltages that are present in the three-phase a-c armature, less change in value of Z_a is to be expected than in a polyphase armature.

Mr. Robinson's suggestion of improving the commutation by connecting in series with the armature a compensating winding having a few more turns than the armature is good, but it must be remembered that this arrangement will increase the armature circuit reactance over that obtainable with a good short-circuited compensating winding. This increase in the armature circuit reactance will make the speed regulation poorer. The problems of commutation on this motor are similar to those of the a-c series motor. On small machines commutation can be improved by shifting the brushes slightly in a direction opposite to the direction of rotation. On larger machines interpoles can be used.

References

1. United States patent 839,937; 1907.

2. Discussion by B. G. Lamme of Methods of Varying the Speed of Alternating-Current Motors. AIEE Transactions, volume 30, 1911, part III, pages 2487–90.

Factors Affecting the Design of D-C Magnets

Discussion of paper 43-47 by L. T. Rader, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, June section, pages 307–10.

B. M. Smith (General Electric Company, Schenectady, N. Y.): Factors affecting the design of d-c magnets as described by the author should be of utmost interest to designers of d-c magnets for, as he has pointed out, there is very little in the literature although the subject is old.

The factors covered, which deal mostly with the magnetic circuit, of course, are the determining factors for efficiency and reliable performance. It is interesting to note that the proper selection of material is given first consideration as it should be. Very often materials are selected without due consideration of all the properties and requirements. Here, however, the author gives a brief résumé of the materials usually available and points out the advantages and limitations of each.

The classification of d-c magnets as to type and the requirements which determine the choice are given and also information on how to change the shape of the pull curve for the two general groups. The method of calculating the pull for a given set of conditions is simply presented, and the designer should have no difficulties in following the procedure and arriving at a satisfactory result.

Perhaps the most valuable contribution in the paper is the data presented on the optimum pole-face area which will give the maximum pull for any one set of conditions. It is illustrated that the pole-face area for maximum pull varies for different air gaps which means that each magnet design can be shaped for a particular requirement.

Leakage flux based on experimental data is shown to be the most reliable means of determining the flux in a magnetic circuit whereas if conventional methods are used in which assumptions are made for constant ampere turns per inch and no fringing in the air gap, there are likely to be large errors in calculating air-gap flux. Leakage, it is explained, varies with magnet dimensions and no attempt is made to compare it for various structures.

The usefulness of the ballistic galvanometer for flux measurements is mentioned because of its accuracy and ease of use. I would like to add to this statement by saying that this is so only when it is overdamped and has a reasonably long period, so as to give a fluxmeter action. Under these conditions and with proper controls, it is a very useful instrument for flux measurements, for they can then be made rapidly and cover a wide range of sensitivity.

The author has made a splendid contribution to aid the designer of d-c magnets, and it is hoped that more experimental information will be published to lead to more accurate determination of leakage flux and other factors which affect the design of d-c magnets.

Intrasystem Transmission Losses

Discussion of paper 43-20 by E. E. George, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, March section, pages 153–8.

R. B. Gow* (Kansas Gas and Electric Company Wichita, Kans.): By reducing the computation of transmission losses to the proper evaluation of a single formula while still retaining reasonable accuracy, Mr. George has made a notable contribution to the electric utility industry. This contribution is particularly welcome at this time with interconnected operation of electric power systems involving transfer of energy between systems becoming more and more prevalent as demands of war industries increase and available reserve capacities decrease.

As Mr. George points out, the loss formula can be refined to give results which are as accurate as could be desired for any purpose. It is interesting, however, to check the accuracy of the formula as developed by the expenditure of the minimum time and effort and without benefit of an a-c or d-c calculating board. This was the case in the development of the loss formula for the transmission system of the Kansas Gas and Electric Company.

In order to check the accuracy of the loss formula load flow diagrams available from previous a-c calculating board studies were used. Since, as ordinarily used, the a-c calculating board does not indicate losses directly with any degree of accuracy, the losses were computed line by line from the load flow diagrams. The megawatt generation indicated at each power source was then substituted in Mr. George's formula and the loss computed by this method. Table I shows the comparative values obtained by these two methods:

Table I. Comparison of Transmission Losses as Computed by Formula and by Individual Lines Using Calculating-Board Load-Flow Data

		Kilowatt Transmission Losses		
Study Number	System Load Conditions	Calculating Board	Formula	Per Cent Deviation
1	Maximum	950	895	−5.8
2	Maximum	1,585	1,650	+4.1
3	Maximum	4,480	4,720	+5.4
4	Minimum	3,600	3,290	−8.6

Study 4 shows the greatest deviation, and this represents a minimum load period, whereas the loss formula was developed using a load distribution occurring during peak load conditions. This would seem to indicate that greater accuracy for loss calculations at minimum load periods could be obtained by developing another formula for use at these loading conditions.

Study 3 is interesting in that it is possible in this case to account for practically all of the apparent error of the loss formula. Approximately 75 per cent of the total loss in study 3 occurred on one heavily loaded circuit operating at a power factor of approximately 97 per cent. This compares with an assumed average power factor of 90 per cent used in the development of the formula. This difference between actual and assumed power factors is sufficient to account for the difference in losses indicated by the two methods.

The apparent accuracy as indicated above is well within the limits of accuracy of the basic data from which loss calculations could be made by any method. Furthermore, in our case the desired quantity was not total transmission losses, but rather the *increment* losses caused by the delivery of so much power to a certain point. It is apparent that the determination of increment losses will be more accurate than the determination of losses at a single loading condition since a portion of the error will cancel out.

In addition to its use in determining losses, we have found this superposition method a very handy tool in predicting and analyzing load flows on the transmission network. For this purpose, only the first part of the development is used. (See Appendix A of the paper for formula giving the megawatts flow in any line in terms of generation at each source.) This type of information is now being requested in various reports to government agencies and the use of this method has considerably reduced the time for preparation of these data.

H. L. Deloney (Louisiana Power and Light Company, Algiers, La.): The method for determining transmission losses as described by E. E. George has many practical applications. It should prove to be a valuable asset to the operating and planning engineers once their transmission systems have been studied and "set up" in the form recommended by Mr. George. Future loss calculations will be very much simplified after various system constants have been determined. With such loss information available, under various operating conditions, it is possible to set up generating schedules which will result in minimum over-all costs, taking into account incremental transmission losses and incremental generation costs (assuming generating costs are already known).

In system planning it will be possible to compare system losses with various assumed generation at two or more proposed sites. This will allow selection of the most favorable sites, after fuel costs and the other variables have been taken into account.

After the system has once been "set up," it will be possible also to study effects on various tie-line loadings under different generating conditions. This information can possibly be more valuable in some studies than loss consideration since it might bring out "bottlenecks" in transmission capacity which would lead either to revision of generating schedules and/or plant sites or to strengthening of transmission facilities. In making these tie-line loading studies it is necessary only to take the synthetic load-flow diagrams described in paragraph 7 of Mr. George's paper and to tabulate the information for the individual lines to be studied. This information will represent the amount and direction of power, expressed in per cent, tending to flow in the line from each generating source or interconnection. After multiplying actual generation of each source, for generating conditions being studied by percentage flow figures, the load flow in megawatts can be determined by taking the algebraic sum of their products. Proper consideration must be given to direction of flow indicated in each case. The general limitations described in paragraph 65 of Mr. George's paper, of course, hold true also for tie-line loading. Calculations when compared with actual log sheet and with calculating-board studies, indicate this method to be sufficiently accurate to warrant its use.

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* Now Ensign in United States Naval Reserve.

The method described by Mr. George will by no means do away with the necessity of calculating-board studies for some problems on individual systems, but will allow the user to answer many questions about his system that would have gone unanswered, due to lack of such board facilities. By knowing these answers he should profit by better planning and plant scheduling.

Ampere Load Limits for Copper in Overhead Lines

Discussion and author's closure of paper 43-22 by A. H. Kidder and C. B. Woodward, presented at the AIEE national technical meeting New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, March section, pages 148–52.

Leonard M. Olmsted (Duquesne Light Company, Pittsburgh, Pa.): There can be no question of the timeliness of this paper. With every electrical power utility and industrial plant striving to carry greater and greater war loads with the minimum use of critical materials, there is great pressure upon them to utilize their present facilities to the maximum advantage. When this pressure is directed to overhead transmission lines, however, considerable caution must be observed lest the system be rendered unfit for continued use. Before ratings can be assigned safely to any line, at least eight factors must be considered carefully, namely:

1. The ability of the conductor to withstand the increased rating without excessive loss of mechanical strength by annealing.
2. The ability of connectors and joints on the conductor to withstand the increased rating without oxidation of the contact surfaces and local heating in excess of the allowable conductor temperature.
3. The adequacy of span clearances to permit the additional sag caused by the higher conductor temperature associated with the increased rating.
4. The problem of providing short-circuit and overload protection to operate within the decreased margin between the increased current rating and destructive overloads.
5. The adequacy of substation and terminal equipment to carry the increased currents for which the transmission conductors may be rated.
6. Operating problems of the bulk power system with individual circuits rerated to such high values that reserve line capacity no longer is available to replace capacity lost when such a line goes out of service.
7. The ability of voltage-regulating equipment to compensate for the greater voltage variations which accompany heavily loaded lines.
8. The considerable increase in energy losses arising from the greater line currents.

No engineer would contend that all eight are of equal importance, but neither could he afford to ignore any one until he had satisfied himself that it does not limit the particular line which he wishes to rerate.

With these aspects in mind, let us turn now to Kidder and Woodward's paper. There is no doubt that the extreme lack of elasticity and low tensile strength of soft—or annealed—copper wire render it unsuitable for transmission conductors. The cold-drawing process causes considerable increase in tensile strength and elasticity and permits design sags based upon storm-loading stresses up to 50 to 60 per cent of the ultimate tensile strength. Once designed for this cold-drawn tensile strength it is essential that the original tensile strength be preserved, as otherwise it becomes impossible to maintain safe ground clearances. This phase of the problem resolves itself into the seemingly simple solution of keeping the conductor temperature below the annealing point. A common figure for the annealing point of copper is 200 degrees centigrade, but further investigation of metallurgical literature discloses that considerable annealing of cold-drawn copper wire has been reported at temperatures as low as 80 degrees centigrade. Unfortunately, damage caused by a slightly excessive temperature may not be detected until long after the overheating occurred.

No extensive treatise upon the annealing of cold-drawn copper wire seems to be available, but metallurgists assure me that it should follow relationships similar to cold-rolled copper sheet which has considerably more literature. An extensive analysis of cold-rolled copper by N. B. Pilling and G. P. Halliwell[1] indicates the following:

1. For any given degree of cold-rolling, a curve showing the time for complete annealing at temperatures from 250 degrees centigrade down to as low as 100 degrees centigrade shows a logarithmic relationship, with the time at 100 degrees centigrade a matter of years.
2. The tensile strength has a definite relationship with the amount of cold working.
3. The greater the cold working (and tensile strength) the faster the rate of annealing.
4. While annealing time is expressed with complete annealing from hard-drawn to soft copper as 100 per cent, the first two per cent of that time removed nearly 50 per cent of the strength imparted by cold working. Thus, while the annealing time at 200 degrees centigrade may be "known to be in the order of days," considerable damage occurs at 200 degrees centigrade in a few hours.

From such tests as these, covering at most only a few months, we are forced to extrapolate to cover the 10, 20, or 50 years during which it is desired to use the conductors.

Assuming that these data apply exactly to cold-drawn copper wire, it would seem that the maximum temperatures of 130 degrees centigrade and 175 degrees centigrade used by Kidder and Woodward are definitely at the upper limit of what might be considered feasible. Certainly lower temperatures are indicated for the hard-drawn bare copper commonly used for transmission conductors.

With conductors loaded to the point where high currents are to be expected, it becomes vitally important that the lines be in absolutely perfect condition from terminal to terminal. Every joint and clamped connection must be perfect to avoid hot spots, and the common flashover burn where two or three strands may be parted would be almost certain to develop into a conductor failure during a subsequent sleet and wind storm. Since any line is likely to have some such defects after a few years of service, it would seem advisable to establish the current rating low enough to allow some margin to avoid subsequent failures caused by local hot spots.

Several types of connectors used in the original construction of lines 15 or 20 years ago have been found by inspection and laboratory test to be inadequate. In general the trouble was found to be warping of the contact surfaces and insufficient mechanical strength in the bolts, both of which seem to have been corrected in many of the connectors now on the market. Before conductor ratings can be increased, however, it is vitally necessary that all unsatisfactory joints and connectors be replaced. Even then there remains the probability that operation of the joints and connectors at temperatures in excess of 70 degrees centigrade will cause oxidation of the contact surface with subsequent excessive heating and damage to both conductors and connectors. While the mass and greater dissipating surface of the connector tend to hold the temperature somewhat below that of the conductor alone, it would seem to be good judgement to hold conductor temperatures below 100 degrees centigrade even for emergency loading.

Many transmission lines are designed for safe clearances at conductor temperatures on the order of 50 degrees centigrade. Raising the operating temperature to 100 degrees centigrade or more causes a considerable increase in length by expansion of the conductor and reduces clearances to ground by nearly four feet in a 1,000-foot span. Obviously, then, it is important to ascertain that the line has adequate clearances before the operating ratings are increased, and some lines may be limited by inadequate clearance.

Short-circuit and overload protection must be provided to function at current values above the rating and still below the level at which the conductors will be damaged. As the conductor operating ratings are pushed up, the margin remaining below the relay limit is correspondingly reduced, and adequate protection becomes a more difficult problem.

Terminal equipment in substations often is designed for a comparatively low current rating. While some equipment may be modified for increased ratings and other equipment replaced, it still is a vital part of any rerating program to ascertain that the terminal equipment involved is adequate to handle the increased rating which is proposed for the conductors.

One of the most difficult problems arising from high line ratings is that faced by the system operators who must endeavor to find ways to maintain uninterrupted service with the facilities remaining after one of the rerated circuits goes out of service. Not infrequently they will have to choose between overloading the remaining facilities beyond their emergency ratings or dropping load, neither of which can be considered desirable.

For metropolitan areas such as Philadelphia, it is probable that transmission distances are short enough to eliminate voltage regulation and losses as limitations to transmission ratings. Most other utilities are not so fortunate, however, and may have lines limited by voltage drop and transient and static stability to ratings even below commonly accepted conductor ratings.

Undoubtedly Kidder and Woodward have considered all of the preceding problems in the analyses from which their paper was prepared. Undoubtedly also they intend their published ratings as absolute maximum values to be approached in practice but never exceeded. They have not taken full advantage of the probabilities found in the load characteristics, atmospheric temperatures, and wind velocities of the Philadelphia area. Even so, it would seem to be better judgement to set the ratings somewhat lower, at values where there is no danger of damaging the conductors. Other utilities would do well to conduct their own analyses of allowable conductor currents, incorporating the local atmospheric temperatures, wind velocities, and the load characteristics of the particular circuit in question. These analyses should allow for their own terminal-equipment limitations, types, and conditions of conductors, and operating problems, and probably will yield ratings materially lower than those published by Kidder and Woodward.

Reference

1. Softening of Hard-Rolled Electrolytic Copper, N. B. Pilling, G. P. Halliwell. *Proceedings*, American Society for Testing Materials, 1923, part 2, pages 97–119.

John G. Holm (Stone and Webster Engineering Corporation, Boston, Mass.): The authors' very interesting paper and its conclusions are of particular importance in connection with loading of overhead conductors in times of war emergency when power requirements are high and the lack of strategic materials acute.

One may not agree with the advisability of attaining what appears to be somewhat high copper temperature limits advocated by the authors; the fact remains, however, that conductors are being operated on the system of the Philadelphia Electric Company at the high temperatures indicated by the authors. If then a 500,000-circular-mil bare concentric stranded copper conductor may be operated normally at a current-carrying capacity of 1,090 amperes, the question suggests itself of what could be attained with hollow copper conductors under similar operating conditions.

For comparison an *HH*-type hollow conductor of the General Cable Corporation was selected. The current-carrying capacity of this conductor exceeds that of any other type of hollow conductors of the same copper cross section. Approximate, but fairly accurate calculations show that a 500,000-circular-mil *HH*-type conductor of the "normal minimum" design has a current-carrying capacity of 1,270 amperes under the same normal temperature conditions at which the concentric stranded conductor carries 1,090 amperes. At larger cross sections the difference in the current-carrying capacity of the two conductors increases, and at 1,000,000 circular mils it is about 22 per cent in favor of the *HH*-type conductor. This *HH*-type conductor, when operated at the normal temperatures given by the authors, has a current-carrying capacity more than 55 per cent larger than the 1,000,000-circular-mil stranded conductor designed to operate at standard conditions (50 degrees centigrade rise, 25 degrees centigrade ambient and wind velocity of two feet per second). If an *HH*-type conductor of the "undercut minimum" design had been selected for comparison, the percentage figures would be still higher.

If the temperatures suggested by the authors are safe, as indicated by their acceptance by the Philadelphia Company, then the great advantage of the *HH*-type conductor over the concentric stranded conductor would become immediately apparent, especially during the emergency operation. At the higher operating voltages, for which for obvious reasons the hollow conductor is especially suitable, the mechanical strength of the conductor requires a certain minimum copper cross section. If then, such a conductor is permitted to operate at temperatures advocated by the authors and is installed on a two-circuit line requiring the hollow conductor to carry the entire system load with a loss of one circuit or a section of it, its cross section must be increased toward the center, but comparatively little more than in the case where no such requirement is imposed on the conductor. When the line has three circuits, and the loss of one would mean an increase of 50 per cent for the normal load of the conductors, no increase in the normal cross-section design of the *HH*-type conductor would be required if temperatures given by the authors are allowed. This could never be attained with concentric stranded conductors.

Therefore, since during the present emergency it is important to have the copper carry all the current it safely can, it should be pointed out that copper may be made to go much farther as an overhead conductor when it is drawn in tubular form than when it is used in form of solid wires concentrically stranded.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): As might be expected from my publications on underground cable, I have been in favor of the principle of increased temperatures and ratings for overhead lines for some years. During the past several years new and higher ratings were established at various times for the system of the Commonwealth Edison Company.

The compounds in old-style coverings of weatherproof wire generally have softening points of 55 to 65 degrees centigrade, according to tests made at Purdue University about 1930 and tests made in Chicago subsequently. Accordingly, the temperature limits used in the special calculations for load ratings for such wire were 66 degrees centigrade for normal operation and 72 degrees centigrade for emergency operation. The corresponding limits for the URC type of covering were 77 and 88 degrees centigrade, respectively. Almost three fourths of our wire, however, had the old-style coverings and in most cases it was impracticable to determine which covering existed in a given circuit. That meant that in almost all cases the ratings used for the weatherproof wires were based on the temperatures of 66 and 72 degrees centigrade. With the advent of the war it was decided to allow rapid deterioration of the covering of the old-style weatherproof wires by using the same load ratings for them as for the URC type.

For our system the incentive to establish further increases in ratings is small because increased ratings would result in using smaller copper conductors in only five or ten per cent of the cases. In other words, regulation is more of a determining factor.

In 1936 we made some tests on URC-type wires with current applied continuously for six or for seven hours. The sizes were numbers 6, 0, and 4/0 and two or three tests were made on different samples of each size. The final copper temperatures were 85 to 157 degrees centigrade, and the conductors were practically at these temperatures for four or five hours.

Copper temperatures of 111 and 113 degrees centigrade on two different samples produced numerous pustules and some blisters in the coverings. In view of these facts for only one day's operation, and considering the data presented by the authors, it is not clear how the authors arrive at temperatures of 100 and 130 degrees centigrade for normal operation.

The Chicago tests with copper temperatures of 129 to 157 degrees centigrade for four or five hours on URC-type wire produced many blisters, the phenomenon being extreme and accompanied by practical destruction of the covering at 157 degrees centigrade. These data do not seem to justify emergency temperatures of 135 to 175 degrees centigrade, which are advocated by the authors. In general, the effects of the high temperatures obviously would be very much more severe on the weatherproof wires having the old-style coverings.

In setting up our ratings, one set was issued based on a summer ambient of 32 degrees centigrade and another set for midwinter based on an ambient of ten degrees centigrade. The former temperature merely takes into account the higher summer temperatures, while the use of the lower ambient in winter permits higher ratings in winter, which is of value in many installations. It is not clear from the article what the authors recommend on differentiating between summer and winter conditions.

The authors state that the effect of solar radiation is neglected. Using data of the United States Weather Bureau in Chicago, we find decreases of 5 to 15 per cent in ratings for summer conditions because of solar radiation and believe this factor should be taken into account.

Incidentally, the wind velocity assumed in our calculations is two feet per second, which is supposed to take into account the effect of the convection set up by the condition of having the weatherproof wire at a temperature considerably above the ambient. Also, it may be noted that the actual maximum temperatures in Chicago in summer and winter may be five or eight degrees centigrade above the values assumed for the calculations.

For bare wires, data given by H. P. Seelye and L. F. Hickernell at the meeting of the transmission and distribution committee of the Edison Electric Institute at Cincinnati last October indicate annealing of copper starting at 100 degrees centigrade. The amount of annealing increases rapidly with higher temperatures. One of these references shows about 40 per cent reduction in tensile strength of hard-drawn wire with just a few hours' operation at 175 degrees centigrade and correspondingly large increases in elongation. With the spacing of 14 or 18 inches between our wires, material increases in sag would result in short circuits between wires. From a private source we learn that annealing has been found in some cases starting at 85 degrees centigrade.

Another important point in connection with the use of higher temperatures is that the resistance losses are substantially increased all the way from the customer back through the entire system to the generating station. These increased losses in themselves may prove to be more of a disadvantage in regard to available reserve generating capacity than the gain in saving copper on new installations by using the extra-high copper temperatures on the wires.

It should be noted that an article like the authors' is unusual in challenging the thinking of engineers and thereby, in some cases, causing large benefits.

Earl H. Kendall (The Commonwealth and Southern Corporation, Jackson, Mich.): The authors are to be complimented on a very complete paper on a very timely subject. The following comments refer largely to the section entitled "Mechanical Limitations of Bare and Covered Copper."

The second paragraph of this section states that there is considerable uncertainty whether copper begins to anneal at 100 degrees centigrade or 130 degrees centigrade and that 100 degrees centigrade may be exceeded on occasion without effect on the mechanical properties of conductors. Considerable research has been conducted on the annealing of copper, and some of the data are presented in the American Society for Testing Materials symposium on the "Effect of Temperature on the Properties of Metals," June 1931. The data in the report indicate that the decrease in tensile strength is more rapid above 150 degrees centigrade, and from room temperature to 150 degrees centigrade the tensile strength decreases about ten per cent.

In the discussion of this report it is brought out that copper begins to anneal at 150 degrees centigrade and is completely annealed in about 96 hours. Also, it makes no difference whether the heating is continuous or intermittent so long as the total time elapsed at the temperature is the same. From this, it appears that for practical purposes the very limit for conductors should be somewhat less than 150 degrees centigrade.

The authors of this paper also say that 150 degrees centigrade to 200 degrees centigrade might be accepted for an hour or so without material impairment to the conductors. As 200 degrees centigrade is the recrystallization temperature for copper, and above this temperature copper deforms plastically under low stress, 200 degrees centigrade even for very short periods for wires in tension is very dangerous. In regard to the temperature tests on the sample conductors tested under tension, it is assumed that the tensile and the elongation tests were made on the complete conductor and not on the individual wires. Such tests are not conclusive because of errors introduced by such things as slippage, uneven elongation of the wires, stranding, and other such items. It would also seem that the sample tested at 200 degrees centigrade, which is the recrystallization temperature, was operated at too high a temperature to be of value. It would be of considerable interest to know how many tests were made and to know the length of the test samples as elongation and tensile strengths vary considerably even for new wire because of mechanical damage in handling, and so forth.

The paper accepts 175 degrees centigrade as a reasonable operating temperature ceiling for overhead conductors during the very rare emergencies when there might be one or two hours coincident of plant failure, maximum load, high ambient temperature, and low wind velocity. Such high temperature even for these rare cases will be very dangerous because this maximum temperature is based on conductors in good condition, which will not be the case of the ordinary overhead line over its entire length as there are always spots which have been damaged either mechanically or through electrical burns. Such spots when the conductors are operated at 175 degrees centigrade are, no doubt, above 200 degrees centigrade, which is the recrystallization temperature. It is indicated from other tests that much safer temperatures even for emergency use would be somewhere between 100 degrees centigrade and 125 degrees centigrade and certainly not greater than 150 degrees centigrade even for rare emergencies.

H. A. Enos (American Gas and Electric Service Corporation, New York, N. Y.): The authors of this paper have used the formulas presented by O. R. Schurig and C. W. Frick[1] in an article in the *General Electric Review* of March 1930, but state that they have rearranged these formulas to facilitate calculation. This has been done by regrouping the factors involved in the formulas so that one group can be set up as a constant called the atmospheric factor K and others grouped to provide another constant called the residual factor C. The authors have then evaluated these constants in terms of the climatic values derived from a study of weather bureau records in the Philadelphia area. A thorough examination of this method fails to disclose any considerable advantage to this method from the standpoint of calculation. On the contrary, it seems to introduce additional and quite unnecessary complication.

It is generally agreed that for the equilibrium condition the sum of the watts convected and the watts radiated minus the watts absorbed from radiation from the sun and adjacent objects is equal to the I^2R watts. For any given set of temperature and air-velocity conditions the watts dissipated by convection is equal to the ratio of the superficial area of the wire to the square root of its diameter, multiplied by an easily determined constant. Also, the net watts dissipated by radiation is given by an easily calculated constant multiplied by the superficial area of the wire. Furthermore, the watts absorbed in the form of solar radiation is equal to a simple constant multiplied by the diameter of the wire. Upon obtaining the value of the total net watts dissipated it is quite simple to determine the current which will provide those watts.

Because of the method the authors have used in presenting their data it is somewhat difficult to determine the actual values of some of the factors on which they have based their illustrative calculations. As near as can be determined, in the case of covered conductors they have used a surface temperature of 80 degrees centigrade for normal operating conditions and a combination of wind velocity and ambient temperature which is quite indefinite as to the actual air-temperature values used. If, however, an air temperature of 40 degrees centigrade is assumed, the corresponding wind velocity indicated by their curves would be 3.5 miles per hour. In the case of bare conductors the surface temperature has apparently been assumed at 100 degrees centigrade for normal operation, and for 40 degrees centigrade ambient the corresponding wind velocity would be three miles per hour. It is difficult to understand the discrepancy between the wind-velocity values for bare and weatherproof conductors. I find nothing in the paper to indicate what conductor surface temperatures were actually used in calculating carrying capacities for emergency operation. The paper hints that these temperatures are about 130 degrees centigrade.

The authors justify their use of relatively high wind velocities because the weather bureau records for several years in the Philadelphia area indicate that in less than three per cent of the days during June, July, and August, would there be temperature and wind-velocity values more unfavorable than the ones selected. This conclusion may have serious results if it is relied upon either by the authors or others, because weather statistics covering only a few years are no guarantee that a worse combination of temperature and wind velocity might not appear at any time and last for a sufficiently long period to endanger the conductors and to reduce seriously clearances. This is all the more dangerous because the authors have assumed that the direction of wind movement is always at right angles to the conductors. As a matter of fact, there is no reason why it could not be parallel to the conductors, in which case the dissipation because of convection would be reduced enormously. The fact that a worse combination of air movement and temperature is possible is shown by the tests made by Schurig and Frick in which low wind velocities combined with high temperatures existed several times for several hours at a time.

The authors have accepted the erroneous conclusion arrived at by Schurig and Frick that the effect of solar radiation is negligible. It is true that it is relatively small in the case of bare conductors, but it is quite large in the case of covered conductors. Furthermore, it is so easily calculated that there seems to be no good reason for ignoring it.

Schurig and Frick presented a formula for the calculation of the heat dissipation by radiation. This formula which is quite accurate for inside conductors where the temperature of the walls of the enclosure is the same as the temperature of the air, but it is not, as Schurig and Frick assumed, correct for overhead conductors outdoors. Unfortunately, the authors of this paper have fallen into the same error as did Schurig and Frick. The correct formula for outdoor conductors suspended in the open above the earth contains as an effective emissivity factor a factor equal to the product of the respective emissivities of the wire and the surroundings. Schurig and Frick's formula contains a similar factor equal to the emissivity of the wire only. The correct formula also contains an additional factor known as the angle factor which is a function of the solid angle through which both the wire and the surroundings "see" each other. One component of the radiation formula involves only the net interchange of heat between the earth and the wire. Other factors must be introduced which evaluate the radiation from the wire to the open sky. Under certain conditions the radiation to other surrounding objects should also be included. The effect of using the incorrect formula for radiation results in a reduction of about 50 per cent in the actual amount of heat dissipated by radiation.

Schurig and Frick based their conclusion that the effect of solar radiation was negligible on the results obtained from tests conducted on bare conductors. However, a close examination of their test data will bring out the fact that the tests they used to make such a comparison were made at different times and under different conditions and, therefore, have only partial validity. Since no tests were made on weatherproof wires, a conclusion that solar radiation has negligible effect upon the temperature rise or the current-carrying capacity of weatherproof wires is far from justified. The contrary is confirmed by the results of calculations which show that in the case of number 4/0 weatherproof wire the solar radiation reduces the current-carrying capacity about 55 amperes, or over ten per cent on a clear summer day. In the case of number 6 wire it reduces the carrying capacity about 11 amperes, or about eight per cent.

Calculation of current-carrying capacities of weatherproof wires by the correct formulas indicates that adequate current-carrying capacities are possible to meet any reasonable needs for additional current-carrying capacity under wartime conditions, over and above what has been considered good practice in the past, without increasing temperature limits to values which are on the verge of endangering the strength of conductors. Adequate current-carrying capacities are also possible without the necessity of assuming wind velocities which are greatly in excess of those which can be reasonably expected to occur sometime combined with high ambient temperatures and which can last for a sufficient length of time to cause unexpected excessive temperature rises. On distribution systems even the range of voltage variation which is considered satisfactory under wartime conditions, and which is more than double the usual range, will be reached and passed long before the current-carrying capacity values determined on the basis of the authors' methods will have been reached. Therefore, there seems to be little excuse for stretching current-carrying capacities until they closely approach the ultimate limit. Furthermore, it should be remembered that the resulting excessive wire temperatures will cause such excessive sags that there will be presented an almost insurmountable problem in constructing and maintaining overhead circuits. This whole problem of current-carrying capacities needs to be studied with a proper and rational consideration of all the factors involved. Adequate tests should be made also to check the accuracy of the methods of calculation adapted.

REFERENCE

1. HEATING AND CARRYING CAPACITY OF BARE CONDUCTORS FOR OUTDOOR SERVICE, O. R. Schurig, C. W. Frick. *General Electric Review*, volume 33, March 1930, pages 141–57.

A. H. Kidder: The authors are indebted to the discussers of their paper for their emphasis of points that may need some clarification. The need for caution in utilizing temperatures approaching a critical zone for copper is one of which the authors were very conscious in the development of ampere load limits for their situation. Differences of opinion are to be expected and have to be reconciled in each particular situation. The opinions given by the discussers are very similar to many that were weighed in developing ampere load limits for Philadelphia.

The authors have undertaken only to present a method by which the probable effects of ambient temperature and wind velocity upon ampere load limits for overhead conductors may be accounted for confidently by straightforward recognition of *coincident* wind and ambient temperature observations. In the situation which the authors have used to illustrate their method the mean ambient temperature was 24 degrees centigrade and the mean wind velocity was 11 miles per hour during the three hottest months of the year. The authors' analysis of the coincidences, summarized in their evaluation of the atmospheric factor K, has developed that the average minimum hour value of K occurs at 31 degrees centigrade and seven miles per hour; the design value for emergency operations occurs at 33 degrees centigrade and five miles per hour; the design value for normal operations occurs at 27 degrees centigrade and two miles per hour; while the absolute minimum values of K occurred only at 23 degrees centigrade and one mile per hour. Under average atmospheric conditions during the three hottest months, therefore, there will be over five times the wind velocity needed to keep the conductor temperature below 100 degrees centigrade at normal load rating or below 135 degrees centigrade at the emergency load limits in Philadelphia. Extension of their survey to include the past 15 years of weather observations has not disclosed any coincidence more severe than 23 degrees centigrade at one mile per hour.

The authors have recognized frankly that temperature ceilings might be reached under certain very improbable coincidences of load and limiting atmospheric conditions. The temperature ceilings could not be bona fide limits if they were not severe. The authors have emphasized the need to provide adequate clearance wherever there is a possibility that such ceiling temperatures might be reached. The integrity of a paper on ampere load limits would be questionable, if operations under the severest possible conditions could affect neither the cover of the wire nor the crystal structure of the copper. The product of the probability of load equal to the load limit of the conductor[1] by the probability that the most critical atmospheric conditions will obtain in those hours of load-limit loading indicates a contingency so remote in normal distribution operations and so very, very remote in emergency distribution operations that it will be a very rare occasion indeed when copper temperatures above 100 degrees centigrade are to be expected in the conductors of a circuit designed to observe the load limits in Philadelphia. In fact, the average maximum hour copper temperature to be expected during the three hottest months in Philadelphia is 85 degrees centigrade if operating at the emergency load limits, or 70 degrees centigrade if operating at the normal design limits, but only 39 degrees centigrade for the average circuit in which there is better than a four to one chance that the load will be below 60 per cent of the normal design limit. The authors, therefore, do not consider themselves to be advocates of high temperature operations.

Most readers of the paper will observe that it deals exclusively with the physical characteristics of the conductor as distinguished from other limitations which can become critical in certain cases before the ampere load limits of the conductor are reached. Points not already covered in this more general discussion will be reviewed now in the order in which they were raised by the discussers.

Mr. Halperin's evident concern about the cover of the wire is definitely a secondary consideration. Taking reasonable precaution against premature inadequacy of conductor sizes used for new installations will in all ordinary circumstances provide sizes which will operate for years well within Mr. Halperin's temperature limits. The value of load limit operations will be determined by the extent to which they will postpone retirements of conductors whose true adequacy is considerably greater than would be indicated by calculations based upon hitherto conventional assumptions. The authors consider the probability of somewhat accelerating deterioration in the cover on such occasions, to be definitely secondary to the alternative of preserving the cover by retiring the installation.

Review of solar radiation shows it to be much less of a factor than Halperin and Enos expect. "A Summary of Total Solar and Sky Radiation Measurements in the United States," by I. F. Hand[2] gives the maximum mean hourly total of radiation on a horizontal surface to be 65.6 gram-calories per square centimeter in Washington, D. C., 64.0 in New York City, and 57.7 in Chicago. The higher figure corresponds to 0.49 watt per square inch received on the (DL) square inches horizontal projected surface of the conductor and dissipated from the $(\pi\ DL)$ square inches of conductor surface. This would make from 1.8 per cent to 2.2 per cent reduction in the load limits calculated for bare wire by the author's method, or 2.3 to 2.9 per cent for covered wire. The load limits quoted in the paper, however, were so conservatively calculated that they are understated by a margin sufficient to absorb solar radiation without reaching the stated temperature ceilings.

Mr. Halperin quotes some opinions about annealing of copper wire. Conflicting hypotheses and opinions will make some controversy until more direct evidence has been accumulated. In the meantime, however, the authors have the assurance of knowing that the medium hard wire they tested lost 16.5 per cent of its initial strength in 44 hours at 180 degrees centigrade, an average loss of four-tenths per cent per hour. As in the case of the cover, the eventual cost of accelerating somewhat the present rate of deterioration in the strength of a conductor is preferred to the heavy expense of an immediate replacement. When one considers the improbability of temperatures approaching 100 degrees centigrade, it seems almost certain that the wire will have been retired for some other cause long before its strength shall have deteriorated by as much as 15 per cent. Some allowance for decreasing tensile strength may be made quite simply if and when desired, by some reduction in stringing tensions.

Mr. Enos' observation that the authors' method introduces some extra work in the calculations of wire rating is pertinent. The authors were willing to do the extra work in order to make their calculations recognize the probable *coincidences* of wind and ambient temperatures in which the conductors will operate. Their calculations include a 15-year survey of limiting atmospheric conditions. The authors discarded consideration of parallel versus crosswise wind, as soon as they realized that conductor sag and the natural turbulence of the wind would make the cooling effect much more nearly independent of apparent wind direction than would be developed from calculations based upon the arbitrary assumption of laminar air flow in the plane of an absolutely horizontal conductor. The vertical components of the natural air motion outdoors will be "crosswise" at all times.

Mr. Enos should have had no trouble recognizing the surface temperatures, because they are given in the titles of the Figures 2, 3, 4, and 5 to which he refers in the paper. Figures 4 and 5 summarize actual coincidences of ambient and wind with two different surface temperatures. Hence, the design value developed by the survey of Figure 4, for a 100 degree centigrade surface, should not be expected to occur at the same wind velocity as that developed from the survey of Figure 5, for an 80 degree centigrade surface. The authors would not have expected a better correlation than Mr. Enos found in his comparison between Figures 4 and 5 at 40 degree centigrade ambient. The calculations for emergency operation are based upon a 175 degree centigrade surface temperature ceiling for bare conductors and upon a 130 degree centigrade surface temperature ceiling for covered wire.

Mr. Enos alleges that Schurig and Frick have made a serious error because they did not, in their formula for radiation, separately identify either the "angle-factor," or the "emissivity factor" of the inclosure surrounding the conductor. For the case of a completely inclosed body which is small compared with the inclosing body, as in the case of a conductor suspended outdoors, Mr. Enos will find upon further investigation that the angle factor is unity and that the emissivity factor of the enclosure also is unity. Schurig and Frick have overlooked neither factor. The over-all accuracy of their formulas has been corroberated in the tests which the authors have made.

Mr. Kendall assumes correctly that the authors' tests were made on the complete conductor rather than on its components. The samples were over four feet long in each case. Tensile tests included both the wire and the associated automatic line splices at the ends of each sample, as left undisturbed in their places throughout both the electrical and mechanical tests. Three eight-inch extensometer measurements were made on each sample, one between benchmarks on the conductor, a second between a benchmark on the conductor and a benchmark on the adjacent splice sleeve at one end of the test sample, and the third, like the second, made between the wire and the splice sleeve at the other end of the test sample. The authors had expected some trouble with strand slippage, and so forth, but the extensometer tests from wire to splice disclosed no evidence of it. The lay of the strands had been pretty well compacted during the heat runs under tension in the test frame. The samples were moved from the electrical to the tensile test, without bending. The authors, therefore, have confidence in their test but realize the limitations of the eight-inch extensometer. The tests were made primarily to determine breaking strength.

The authors do not share Mr. Kendall's concern about the limiting effect of spots where there have been electrical burns or mechanical damage. They are satisfied that the copper has long since been completely annealed at the location of every burn which has any significant effect upon the conductor cross section. Mechanical failure is to be expected at mechanical weak spots. It is to be expected long before there would be any reasonable probability of further weakening by operations in Philadelphia of circuits designed to observe the stated ampere load limits.

Mr. Holm appropriately calls attention to the substantially higher ampere load limits which can be secured when desired by taking advantage of the greater surface to cross-section ratios that are obtainable in tubular conductor shapes or their equivalents.

Mr. Olmstead's review summarizes very well both the paper and the discussion of it. Each of the factors he lists is one to be considered in establishing line ratings, although the last five factors lie beyond the stated scope of the paper at hand.

References

1. Notes on Emergency Ratings, **A. H. Kidder.** AIEE Transactions, volume 58, 1939, November section, pages 602–3, Figures 2b and 3.

2. A Summary of Total Solar and Sky Radiation Measurements in the United States, I. F. Hand. *Monthly Weather Review*, volume 69, April 1941, pages 95–125.

Transmission-Line and System Problems in Supplying Large Electric-Arc Furnaces During Wartime

Discussion and author's closure of paper 43-29 by B. M. Jones, presented at the AIEE national technical meeting, January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, April section, pages 197–202.

O. B. Falls, Jr. (General Electric Company, Schenectady, N. Y.): The practical operating data on the arc furnaces, as well as the solution to the power-supply problem for these furnaces as given by the author, should be quite valuable. This is particularly true for the larger sizes of electric-arc furnace included in this analysis. However, it is our opinion that the actual operating problems involved in serving power to "small" arc furnaces as compared to "large" arc furnaces is quite different.

Operating experience has shown that the best over-all performance of a three-phase arc furnace used for steel melting is generally obtained with from 40 to 50 per cent reactive voltage drop in the total circuit with normal current flowing in the circuit. Normal current is that current in the low-voltage arc circuit and is not necessarily the rated current of the transformer furnishing power to the furnace. Values of reactance of 40 to 60 per cent are given in the Standard Handbook for Electrical Engineers, Section 18, paragraph 135.[1]

These reactance values are the sum of all the reactances in the circuit, starting at a point in the supply system where the voltage may be considered as constant down to the arc in the furnace. That is, it must include the reactance of the high-voltage transmission line, step-down transformer banks, supplementary reactance, and the low-voltage circuit to the furnace proper. If this reactance is considered as being all air-core reactance, the current swings in such circuits will not exceed approximately 2.5 times the rated current of the arc-furnace circuit for 40 per cent reactance. Generally, the swings will be less than this because of the resistance in the circuit.

As a basis of analysis, I will use the ten typical installations cited by the author in his Table I (refer to Table A of this discussion). In column 2 is shown the total *ohms* to neutral required in the arc circuit for the various sizes of furnaces on the assumption that the normal current of the arc circuit is approximately the rated current of the transformer. The two values of ohms given in column 2 for each item represent the ohms required for 40 per cent and 50 per cent reactance respectively for the values on the left and the right. In columns 4, 6, and 7 are given respectively the actual ohms referred to the low-voltage circuit of the step-down transformers, the transmission line down to the high-voltage terminals of

Table A. Ohms to Neutral of Various Component Parts of Arc-Furnace Installations Shown in Author's Table I

Item No.	1 Largest Transformer (Kva)	2 Approximate Total Ohms Required 40%–50%	3 Transformer Secondary Volts (Assumed)	4* Transformer Ohms at Secondary Voltage $\div 10^4$	5 Line Kilovolts	6 Line Ohms at Secondary Voltage $\div 10^4$	7** Low Voltage Circuit Approximate Ohms $\div 10^4$	8 Sum of Columns 4, 6, 7 (Ohms $\div 10^4$)	9 Supplementary Reactance Required (Ohms $\div 10^4$)
1	15,000	20–26	270	4.8	68	1.9	20	26.7	
2	10,000	28–36	270	7.2	68	2.9	20	30.1	
3	10,000	28–36	270	7.2	68	3.7	20	30.9	
4	7,500	36–45	260	7.2	23	1.3	20	28.5	8–17
5	7,500	36–45	260	7.2	23	5.1	20	32.3	4–13
6	6,000	44–55	250	10.4	68	5.8	17.5	33.7	10–21
7	5,000	50–63	250	10.0	23	2.5	17.5	30.0	20–33
8	4,000	56–70	235	11.0	23	5.4	15.0	31.4	25–39
9	3,000	68–84	225	16.9	23	1.6	15.0	33.5	34–50
10	3,000	68–84	225	12.7	11.5	1.4	15.0	29.1	39–55

* 68-kv transformers—10 per cent reactance on own base assumed.
23-kv transformers—8 per cent reactance on own base assumed.
11.5-kv transformers—6 per cent reactance on own base assumed.

** Total *estimated* ohms to neutral of low-voltage circuit to furnace bus bars, flexible leads, and electrodes.

the step-down transformer, and estimated figures for the secondary circuits from furnace transformer to furnace. Column 8 shows the sum of columns 4, 6, and 7.

Now, if we compare the values given in column 7 with those shown in column 2, we find that for the larger installations represented by transformers rated 7,500 kva and larger, the total *ohms* in the arc-furnace circuit required for satisfactory performance is in general inherent in the system, and no supplementary reactance, or at most only a few per cent of supplementary reactance, would be required. On the other hand, for the smaller installations, represented by transformers rated approximately 6,000 kva and less, indications are that a rather sizable amount of supplementary reactance would be required in order to obtain satisfactory furnace performance.

In what manner, however, does this conclusion affect the data submitted by the author? The values of supplementary reactance shown as being required for the smaller installation, are based on our original assumption of from 40 to 50 per cent reactive voltage drop required for good furnace performance. This does not mean that if this reactance is not in the circuit, the furnace will not operate, and for this reason it is entirely possible to operate furnaces in the smaller sizes without these values of reactance. However, as the total reactance in the circuit is decreased, the frequency of occurrence of load swings exceeding any given magnitude increases quite rapidly. Also, for any given rated current value, the magnitude of the individual swings will increase as the reactance in the circuit is decreased. Both of these factors have a direct bearing on the amount of voltage variation and the frequency of occurrence of the load swings which the utility may expect when applying power to the smaller arc furnaces. As a result, it would appear that in order to obtain a full and complete analysis of the effect of any particular furnace on the transmission or distribution lines, some knowledge of the total reactance in the circuit should be available.

The author has indicated whether or not supplementary reactance was used, however, no information is given as to amount of reactance in the circuit. A very useful addition to this paper would be the values of total reactance in the furnace circuits including the step-down transformer, any supplementary reactance, the secondary bus bars, and the flexible leads, and electrodes. Also, information should be available on the maximum secondary voltage for each installation as noted in Table I of the author's paper. Such data would make it possible to compare the results given in this paper with the results obtained in connection with similar problems on other power systems.

I believe that the author is fully aware of the importance of having sufficient reactance in the arc-furnace circuit, since he states that the "frequency of the furnace swings and their magnitude govern the amount of stiffness required in the system at the point of service, for these are the criteria of the flicker which will result in voltage complaints if any are incurred." Where "large" furnace installations are involved, it is reasonable not to give any consideration to the problem of reactance, since sufficient reactance is usually inherent in the system. As pointed out, however, the matter of whether or not supplementary reactance is available in the smaller furnace installations is quite important, since the inherent reactance in the system is usually small compared to the total reactance required.

The author, in Table I, has indicated that the voltage swings at the nearest critical substation bus for the ten cases cited have been in general within tolerable limits. In connection with this, I would venture the comment that either one of two things has happened in order to obtain such values. The two points are that either the utility has been required to build an extra good system up to the nearest critical substation bus, or that values of supplementary reactance approaching those shown in my Table A have been used, particularly for the smaller sizes of furnaces. If the latter is the case, the utility probably can feel quite fortunate.

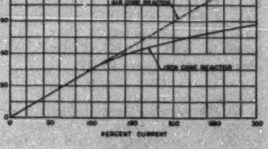

Figure 1

The statement was made previously that with the values of reactance proposed, the current swings would not exceed approximately 2.5 times the rated current of the arc-furnace circuit if the reactance was all air-core reactance. In any given installation, however, there will be some iron-core reactance as well as some air-core reactance. In the type of circuits being considered, air-core reactance is that attributable to the transmission lines and the secondary bus and electrode circuits, and possibly that of any supplementary reactance. Transformers may be considered also as being air-core reactance. Supplementary reactors may be either iron core or air core. Again looking at Table A, we can draw the general conclusion that for the larger furnace installations, the reactance in the circuit can be considered as being almost entirely air-core reactance. For the smaller furnace transformer installations, however, if we consider that the supplementary reactance is iron core, the total reactance in the circuit will be predominately iron-core reactance. On the other hand, if we consider that the supplementary reactors are of the air-core type, the total reactance will be predominately air-core reactance. Since the type of iron-core reactors usually used with arc furnaces will have straight-line reactance only up to approximately 150 per cent, it would be interesting to investigate the problem of which type of supplementary reactance should be used if it is necessary to use it at all. Since iron-core reactors may be subject to saturation above approximately 150 per cent of their normal rating, there is always the possibility of the reactor becoming saturated to such an extent that the total *ohms* in the circuit will be reduced below a value which would limit the current swings and the frequency of swings above a given magnitude to tolerable values.

In explanation of this, I would refer to Figure 1 of this discussion, which shows the volt-ampere characteristics of a 30 per cent air-core reactor and a similarly rated iron-core reactor with a straight-line characteristic up to approximately 150 per cent of rated current. Actually, at 150 per cent of rated current this reactor shows a decrease in reactance of approximately ten per cent of the rated value. Let us assume, for an example, an installation having approximately 50 per cent total reactance in the arc-furnace circuit. Of this 50 per cent, 20 per cent is air-core reactance, and 30 per cent is in the supplementary reactor. For a three-phase short circuit, the current will be approximately twice normal if the reactor is of the air-core design and does not saturate. If, however, the reactor is of the iron-core type, and we assume a three-phase short circuit, the reactance of the reactor will decrease to approximately 20 per cent, giving a total of 40 per cent for the total circuit, which results in a load swing of approximately 2.5 times normal; therefore, the result is that the maximum possible magnitude of the swings has increased approximately 25 per cent. The actual magnitude of swing will not be, of course, two times normal, or 2.5 times normal since the resistance of the circuit will tend to reduce these values. The increase in magnitude of load swings caused by saturation of the iron-core reactor will approach 20 to 25 per cent on the basis of using an iron-core reactor having a straight-line characteristic up to approximately 150 per cent rather than an air-core reactor.

B. M. Jones: I would like to take this opportunity to say that I take great pride in the fact that the picture on the front of the program for the technical meetings in January 1943, sent to all members by mail, was taken from my article.

A representative of a very large furnace manufacturer told me just before this meeting that they had built in 1941 and 1942, and had on order for 1943, more furnace productive capacity than they had built in their entire previous history. These furnace tonnage capacities were approximately 800,000 tons in 1941, 800,000 tons in 1942, and 1,200,000 tons on order for 1943.

Regarding Mr. Falls' comments, the writer most heartily agrees that the operating problems involved in serving "small" electric furnaces are much different than those in serving large furnaces. In the latter case, generally speaking, the furnace swings in proportion to the rating of the furnace are smaller, and the line to supply the big furnaces would probably be inherently larger, whereas for the smaller furnaces, the swings are larger on a percentage basis with a smaller line and probably connected to a weaker spot in the system. Thus, for the smaller furnaces a more serious problem is at hand. Our operating experience has shown that present over-all performance is obtained on the medium and small size furnaces with an appreciable amount of reactance, and we feel that something of the order of 40 per cent range to a higher figure for the smaller furnaces is a good starting point, with, of course, adjustable taps on the reactance to obtain the optimum operating condition for each installation.

Relative to Mr. Falls' question as to the amount of reactance, supplementary, secondary bus bars, flexible leads, electrodes, and so forth, this information was not available. Admittedly, it would be desirable to have this, but we just did not get it. It can be determined from measurements of the furnace in operation in a steady-state condition, together with the known electrical data on certain parts of the circuit. We do realize the importance of having sufficient reactance in the circuit, a supplementary reactance in the smaller furnaces. Right here I would like to point out to all power companies supplying furnaces or about to supply furnaces, that this is a very important thing for the medium and smaller size furnaces, and it may and probably will be very important on even very small furnaces on a small system or on a weak spot in a big system. Our 22-kv transmission system is relatively stiff at points of connection to some of these furnaces, and undoubtedly that contributes to the lack of complaints.

After all is said and done, if you have an adjustable device, taps on the reactors in this case, the operators are going to juggle it around to what in their opinion is the best operating condition. In the case of electric furnaces, this varies among different plants, and some melting superintendents think that an intermediate position on the reactor is the best, while someone else feels that it should be adjusted to the absolute minimum reactance. We have determined that certain furnace installations can actually consume more kilowatt-hours by operating on an intermediate reactance tap and maintain a steady arc, thus producing more heat in the melting pot, than by operating without the reactance with higher currents being frequently interrupted. We have actual measurements of this, using kilowatt-hour meters to prove it, but it fell on deaf ears. I have also verified this with engineers in several steel plants and their experience has been the same. So, I believe any power company taking on electric furnaces should recognize this point and be in a position to handle the loads in whatever manner the melting superintendent may operate.

Relative to the use of series capacitors for handling furnace loads, I feel that the proper application of the capacitor will go a long way toward smoothing out the furnace swings before they get back into the system, but the design and setting must be co-ordinated very carefully with the electrical characteristics and operation of the furnace. For instance, it might not be so satisfactory to install a series capacitor for a certain range of operation of the furnace and then have the furnace performance increased very radically, for the condenser might not be able to cover such a wide range. I recall that this was discussed somewhat between Mr. Falls and the writer at the Association of Iron and Steel Engineers meeting, Canton, Ohio, May 1941, and at that time it was pointed out that there were one or two series capacitors in satisfactory operation on long lines supplying small electric-arc furnaces somewhere in South America.

I do not want to leave the impression that the electric-power companies are afraid of electric-arc furnaces or that they do not want this type of load, for such is farthest from my thoughts. I merely want to point out some of the problems that have to be solved to provide adequate service for such loads and still carry the rest of the load in a satisfactory manner.

We are stretching our former limits (and will stretch them more if necessary) and are putting up with conditions in wartime that probably will have to be improved in peacetime—all to minimize the use of a critical material, copper, and we are in full accord with the War Production Board in its sincere efforts to make the copper go around!

Dielectric-Recovery Characteristics of Large Air Gaps

Discussion and authors' closure of paper 43-10 by G. D. McCann and J. J. Clark, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, January section, pages 45–52.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The paper describes an interesting method for testing recovery rates of air caused by successive impulses. On studying the test results there are several questions which arise as to the interpretation of the results.

In Figure 2 of the paper are shown a number of initial current waves which have so-called initial crests followed by either a low current steady crest (waves *A*, *B*, *D*, *H*, *G*, and *J*), or by a crest of approximately the same amplitude (waves *C*, *E*, *F*, and *I*). The high initial crest is explained as the result of the discharge of the charges in the gap after the intense "return streamers" are formed which propagate back to the electrodes.

Such a phenomenon undoubtedly occurs; however, I cannot quite understand the high initial current amplitudes which the authors obtained, ranging from 1,390 amperes to 23,000 amperes. The principal constants of the circuit producing this current consist of the capacitance *C* of the gap, the inductance in series with the discharge, and the impedance *R* of the breakdown path between the electrodes. The maximum current crest that can be produced in such a circuit is

$$I_m = E\sqrt{\frac{C}{L}}\times F \qquad \sqrt{\frac{C}{L}} = \frac{1}{Z}$$

where E is the voltage applied to the gap at the time of sparkover, and F is a reduction factor, the value of which depends on the ratio of R/Z. $F=1$ for $R/Z=0$. $F=0.37$ for $R/Z=2$. The dimensions of the rod-gap circuit and the wave shape of the initial voltage waves causing breakdown are not given in the paper, and therefore only approximate calculations can be made as to the expected initial current maximum.

The critical sparkover of a six-inch rod gap as given by Figures 4 and 5a is approximately 135 kilovolts. The usual capacitance of a six-inch rod-gap installation set up according to rules of the Joint Insulation Co-ordination Committee[6] of the National Electric Manufacturers' Association and the Edison Electric Institute is approximately 20 micromicrofarads. Unless additional capacitance, not mentioned in the paper, was connected, the test capacitance should be of this order. The inductance would be of the order of 0.75 microhenry. The impedance R is, of course, variable but should be relatively high, of the order of thousands of ohms, for discharges of such short time. Therefore, the factor F should be of the order of 0.7 or less. With a sparkover voltage of 135,000 volts this would give a current crest of $135{,}000\times 5.18\times 10^{-3}\times 0.7 = 490$ amperes or less.

This current value should be the same for all tests where high series resistance was used in the main discharge circuit. For waves *D*, *E*, and *F* where low series resistance was connected, the gap may have been overvolted before sparkover occurred and therefore the initial current peak may have been higher, say 2 to 3×490 amperes. All these values are considerably below those given in the paper.

In order to obtain an initial current peak of 23,000 amperes for wave *F*, the constants of the initial spark circuit would have to differ as shown in Table I of this discussion.

Other variations are possible, but all these figures indicate that such a high initial current peak for the test condition is improbable.

Since this initial current discharge occurs in a small fraction of a microsecond (0.1

Table I. For 23,000 Amperes Initial Current Peak of Wave *F*

$C = 20$ micromicrofarads	$C = 20$ micromicrofarads	$L = 0.75$ microhenry
$L = 0.75$ microhenry	$E = 135{,}000$ volts	$E = 135{,}000$ volts
$E = 6{,}350{,}000$ volts	$L = 3.12 \times 10^{-4}$ microhenrys	$C = 48{,}000$ micromicrofarad

microsecond or less as shown in Figure 2 of the paper), the rate of initial current rise, assuming 490 amperes crest, would be of the order of 5,000 amperes per microsecond. A small inductance in the measuring shunt or in the ground circuits might appreciably affect the measurements and may in part account for the high initial crest currents reported.

The curves of Figure 3 are very revealing in showing the recovery voltages for the different current waves. It would be interesting to study this behavior in a region representative of the more severe lightning currents.

Measurements at the Empire State Building[1] show that 50 per cent of the current peaks had a charge of 0.17 coulomb to half value of the current, 10 per cent one coulomb, and one stroke 5.0 coulombs, compared with a total charge of 0.238 coulomb maximum used in the paper. In Figure 4, 50 per cent of the long duration lower amplitude currents had a charge of 25 coulombs or more. The current amplitudes at the same station show 50 per cent to be higher than 7,000 amperes, 10 per cent higher than 20,000 amperes, and one stroke higher than 58,000 amperes. Magnetic link measurements on transmission line towers, taking into account all tower currents, indicate 50 per cent between 11,000 amperes[2] and 21,000 amperes.[3] Taking into account only the currents at the tower struck or closer to the stroke, this value increases for 50 per cent of the strokes to 35,000 amperes. On transmission lines, two conditions of initial sparkover have to be considered. If the stroke contacts the tower, and flashover occurs from tower to line, then the impulse current through the spark will be of the order of $i = IR/Z$ where I is stroke current, R ground resistance, Z surge impedance of the line wire. If, however, the stroke contacts the line and flashover occurs at the adjacent tower, the current flowing through the spark will be of the same order as the tower currents because of direct strokes or strokes to the ground wire. In this case the spark will conduct to ground a considerable part of the continuing discharge of the stroke, while in the former case the continuing portion would be rather small. It would seem, therefore, of interest to obtain recovery voltage curves for current discharges of considerably higher amplitude and charge.

The data as presented do not permit drawing a conclusion on the factors which contribute to the slow recovery rate, whether current amplitude or charge is responsible. In the wave shapes selected the second current peak and the charge vary in the same manner as the recovery curves of Figure 3. Perhaps the authors have some further unpublished material where equal current amplitudes were applied, but the charge of the waves was varied over a considerable amount. It should be expected that both current amplitude and charge will be responsible for a given recovery characteristic. In a paper in preparation, J. W. Flowers shows that the impulse spark tends to establish a channel of such size that the current density is of the order of 1,000 amperes per square centimeter in the region between 100-ampere and 100,000-ampere crest, starting with initial densities of 27,000 amperes per square centimeter or more. This would indicate that the current amplitude provides only a larger volume of ionization within this volume; however, ionization should be essentially constant.

The charge flowing through a given volume, however, should be much more important in ionizing this volume to a greater extent as the charge increases. Such phenomena can be observed during the formation of long 60-cycle arcs.

A point of importance, I believe, is the manner of application of the second impulse. The authors say very little about this except that the wave has a $1^1/_2 \times 40$ shape and that the series resistance had to be fairly high, which could have produced appreciable regulation. In Figures 8 and 9, a few of the voltage and current waves of the second discharge are shown. The currents are presumably of the same order of magnitude as for the second impulse currents for the other discharges. In multiple lightning strokes the second discharge current is frequently of the same order of magnitude as the first, and in power systems the currents available at the fault during recovery in many cases should be of the order of the load current at least or several hundred amperes. The energy available for restriking should be considerably greater than that indicated by Figures 8 and 9.

It seems to me that the power arc recovery rates should be used in Figure 16 for dielectric recovery rates, since even the short initial power current flowing through the fault very likely is of the order of 100 amperes or more. Furthermore, in most cases where the stroke contacts the ground wire or tower, and arcover occurs from tower to line, the phase with the highest operating frequency voltage crest should arc over. Laboratory tests indicate that the impulse voltage required to flashover an insulator string is reduced from its critical flashover voltage by the approximate amount of the instantaneous value of the normal frequency voltage H of opposite polarity, and increased by the same amount or more if the normal frequency is of the same polarity as the impulse voltage. There is always a positive or negative voltage in excess of 50 per cent of the crest on one of the conductors of a three-phase system. The time T at which two of the phases have equal potential is about 1,400 microseconds. This time, therefore, will be the most probable minimum time between the normal 60-cycle zero voltage and the time of flashover for strokes to the tower modified by wire swing and line clearances. For strokes to the line wire direct, which should occur rarely where ground wires are used, the time T may be anywhere on the cycle.

I agree with the authors that the duration of multiple strokes[4] and the dielectric recovery from power arcs are the principal factors governing circuit reclosing times.

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6. RECOMMENDED TRANSFORMER STANDARDS, H. V. Putman, J. E. Clem, AIEE TRANSACTIONS, volume 53, 1934, December section, pages 1594–7.

G. D. McCann and J. J. Clark: Mr. Hagenguth believes that the initial high currents of very short duration occurring at the first instant of gap breakdown cannot be as large as recorded. To attempt to prove this he calculates their crest magnitude on the simplified assumption that these currents are simply the discharge through an equivalent lumped resistance and inductance of the charge stored in the gap by virtue of its capacity (the air between its electrodes acting as a perfect dielectric). He assumes that perfect conduction starts instantaneously. This, however, is not the actual mechanism of the discharge. As pointed out in the paper, the gap breakdown starts in the form of "initial leaders" propagating from each electrode. At the instant they meet, there is distributed in the gap, along the channel which they have blazed, considerable charge in addition to that which he assumes. The current flowing at the brief instant after the "return streamers" reach the electrodes depends upon a number of factors difficult to evaluate analytically and not represented by Mr. Hagenguth's calculations.

The authors agree that it would be interesting to determine dielectric recovery characteristics for higher crest currents and for discharges of longer duration involving larger amounts of charge. However, with regard to crest magnitudes, the discharge currents studied are typical of the majority of strokes, and representative values of both crest current and time to half value were studied. In considering data on crest tower currents it is thought that Mr. Hagenguth has misinterpreted the data of Waldorf (reference 2 of his discussion). The figure of 11,000 amperes applies to currents in the tower struck or close to the terminating point of the stroke instead of to all recorded tower currents. The figure of 35,000 amperes for the magnitude exceeded by 50 per cent of the maximum tower currents of each stroke is thought to be considerably too high. Data available to the authors from the investigation of Waldorf and others show that this figure ranges from 8,000 to 15,000 amperes depending upon span length and tower footing resistance.

With regard to the relative effects of crest-stroke magnitude and total charge upon the rate of dielectric recovery, the authors believe that increasing the total charge and the stroke duration should have a greater effect than increasing the crest magnitude. The data presented in the paper show that variation of crest magnitude and charge of the initial high current short duration portion of a stroke component (for the practical range of values that have been recorded) does not materially influence the rate of recovery. However, the rate of recovery undoubtedly will start to decrease considerably under certain conditions as the duration of low magnitude current flow and resulting charge is increased.

Mr. Hagenguth questions the manner of application of the second discharge, thinking that, because small currents were obtained for the subsequent discharge, the small amount of energy available for restriking would not give typical results. Whether or not restriking takes place is determined by the character of the voltage across the gap and not the current that flows after the breakdown. For the smaller time intervals the current resulting from a restrike at or near the critical restriking voltage is naturally considerably less than for longer time intervals, since less voltage is required. However, the voltage applied for the restriking study always had a $1^{1}/_{2}$ microsecond front and a time to half value of at least 40 microseconds until it was raised high enough for a restrike to take place.

Impulse and 60-Cycle Characteristics of Driven Grounds—III. Effect of Lead in Ground Installation

Discussion and authors' closure of paper 43-12 by P. L. Bellaschi and R. E. Armington, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, pages 334–45.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The principal contribution of the paper seems to lie, in my opinion, in that it brings out in numerous examples the effect of inductance of leads and towers on the total voltage developed at the stroke contact point for given stroke currents.

Of course, this has been known by many people concerned with lightning protection, and the detrimental effect of the inductive drop has been emphasized, especially to provide short connections between protective devices and the apparatus to be protected, and the inductive effects have been used to calculate the over-all protective effects. These facts perhaps have not been recognized clearly by many, and therefore the publication of a theoretical analysis of the problem is of considerable use in calling attention to the relatively high voltages that may result from the inductive drop. Figure 1 of this discussion might be useful in emphasizing the relative effects of resistance and inductance. The voltages developed are calculated for the highest current wave measured at the Empire State Building[1] which is shown in the insert, using values of inductance and resistance as indicated.

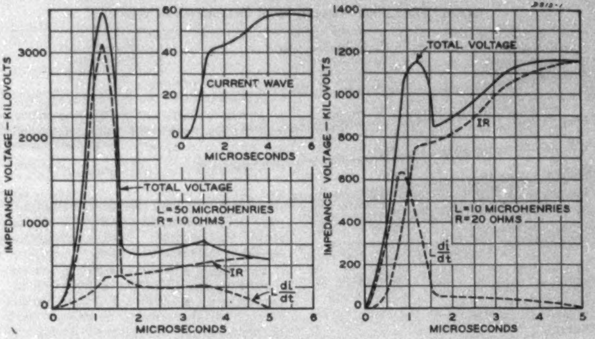

Figure 1. Relative importance of inductance and resistance in the ground connections of a lightning-rod system

Actual lightning current wave measured by oscillograph[5]

The paper uses for resistance values the impulse resistances found from test and published in two previous papers. In a previous discussion it was stated that the measurements do not apply necessarily to stroke currents at the location of lightning contact with the ground electrode, where current fronts of much shorter duration than 6 to 20 microseconds should be expected. The Empire State investigation[1] gave current fronts of from 0.1 to 6 microseconds, with 50 per cent shorter than one microsecond. The rate of current rise ranged between 0 kiloamperes per microsecond and 35 kiloamperes per microsecond, with 50 per cent of the strokes having a rate of rise of 14 kiloamperes per microsecond. Similar values of rate of current rise were reported by other investigators.[2]

It can be said therefore that current fronts at the point contacted can be frequently of the order of two microseconds or less. For fronts of such steepness, the resistance tests reported previously and used in this paper hardly apply. Inasmuch as the lowering of the resistance is a breakdown phenomenon, it should be expected that the breakdown voltage depends on time, similar to breakdown in oil, air, or other materials. It is only after resistance change at short times has been investigated that curves of the type shown in the paper can be applied. It is evident that a reduction in resistance to 50 per cent which is indicated by all of the reported measurements for the larger values of current will make a great difference in calculated potential. There is some question even whether the initial impulse resistance for steep current fronts may not even be higher than the 60-cycle resistance, disregarding the inductive and capacitive impedance components. The effective resistance of an electrode is determined, not only by the electrode shape, but also by the distribution of current between the electrode and the ground plane. It has been shown by Bewley's multivelocity theory[3] that this ground plane changes its position relative to the earth's surface as the current wave travels along a ground wire or a counterpoise. Therefore, the effective ground area carrying current should become greater with time resulting in a lower resistance at the longer time. While this phenomenon is not very clear at present, it does indicate that initial resistances may be even higher than the 60-cycle resistance, since for 60-cycle current, distribution in the ground can be of very great extent.

Some effect of wave front can even be detected from an analysis of resistance measurements of Parts I and II of the paper[4] as shown by Figure 2 of this discussion. Curve 1 is grounds *C* and *D* of Table II, Part I, with six microsecond front; curve 2—grounds *B*, *C*, and *D* of Table IV, Part I, with 12 microsecond front; curve 3—grounds *F*, *G*, and *P*; curve 4—ground *M* of Part II of the paper with 15 to 20 micro-

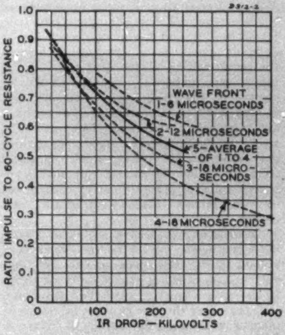

Figure 2. Effect of wave front of applied surge on the impulse resistance of grounds

Average of	R-60-Cycles
1. *C*,*D* Table II, part I	36
2. *C*,*D* Table IV, part I	39
3. *F*,*G*,*P* Table III, part II	27.5, 24.3, 13.8
4. *M* Table IV, part II	118

second wave fronts, while curve 5 is an average of all measurements. It is interesting to note that the reduction in ground resistance becomes less as the wave front becomes steeper.

The abscissa is the *IR* drop calculated from the measured current crest and the a-c resistance of the grounds as suggested by Towne.[5]

While there exists a distinct difference because of wave front, the average curve should be useful for calculations for slow front waves. The parallel connections tested and shown in Part II fall well within the range of curves 1 and 4. Thus, instead of calculating the impulse resistance for any particular installation, using the equations of Part II, the average curve can be used if the 60-cycle resistance is known by measurement or calculation.

The calculations of Figures 18 and 19 of the paper, with respect to the counterpoise voltages, seem to be too optimistic. The impedance of a 200-foot counterpoise for a one microsecond current front should be more nearly of the order of 60 to 80 ohms as compared to the 15 to 20 ohms given in the paper. This follows from tests[6] as well as independent calculations.

The calculations indicated by Figure 25 to Figure 27 in the paper show an interesting approach to explain the formation of the lightning currents in the ground at, and near the point of contact. Incidentally, the abscissa values of Figure 27 apparently should be multiplied by a factor of two to conform with the text. There are several points of argument with regard to the fundamental factors used, such as the velocity of the return stroke and the apparent change in velocity, the velocity of propagation of currents in the ground, the formation of streamers at ground terminals, and their effect on the charge distribution and others.

Many of these factors are pure speculation at the present time, and arguments therefore would be useless.

I hope that the paper will help to make it more clear to many engineers that the transient impedance of grounds has to be considered to provide intelligent protection.

While all the factors involving transient impedances are not known, sufficient data are available from this paper and others mentioned in the bibliography of this paper[6,7,8] to arrive at intelligent conclusions, with a good probability that in the majority of cases, the ground installation will be adequate.

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F. O. Wollaston (Commonwealth Edison Company, Chicago, Ill.): This very comprehensive paper sheds light on some puzzling points in connection with grounding. For example, tests by Davis and Johnston (reference 6 of the paper) showed appreciable voltage drop in towers for moderately steep-fronted waves, which was attributed to resistance as well as inductance of the structure. This paper brings out very clearly that the resistance of the tower is not an important factor, the only important resistance being that of the ground. For another example, the ineffectiveness of very deep grounds under certain conditions is satisfactorily explained. Instances are on record where flashovers occurred at towers with deep grounds, although the resistance of the grounds theoretically was low enough to prevent flashovers. One concludes that the best practical procedure for obtaining good grounds in difficult soil is first to try multiple surface grounds, including counterpoises of moderate length, and go to deep grounds or long counterpoises as a last resort.

Figure 15c of the paper is interesting in showing the transient impedance characteristic of a counterpoise. The initial impedance is much higher than the final value, which is the leakage resistance. The findings confirm those of Bewley and Hagenguth, using the conception of multivelocity wave components.

After reading this paper one grasps more clearly the importance of the wave shape of the lightning stroke in determining the total potential developed on the structure, and also the important effect of the grounding arrangement in determining what the wave shape of the stroke current going to ground will be. Some revision of ideas as to wavefronts to use in calculations on lightning protection may be in order, as indicated by the comparison of the sinusoidal and exponential waves in the paper.

P. L. Bellaschi and **R. E. Armington:** First we want to thank those who have discussed the paper for their contribution to the subject. The field experience with deep grounds Mr. Wollaston cites is interesting. This is in agreement with similar experience reported on extensive grounds and is in accord with the results of the paper.

The example on the relative importance of inductance and resistance in a lightning rod system that Mr. Hagenguth gives in Figure 1 of the discussion is particularly pertinent, for the current selected in these calculations is from a direct stroke of lightning actually recorded.

With reference to the resistance of driven grounds for current fronts of short duration, the problem was examined and analyzed in some detail in the preceding paper. The data and analysis referred to show that the variation of the true resistance rapidly follows the current with no appreciable time lag for wave fronts as fast as two or three microseconds, the limit that the data can be interpreted. The results also bear out that the resistance starts out initially at, or close to the sixty-cycle value. As stated in the preceding paper, it is quite possible that the true resistance for a very rapid rise of current to crest, in one microsecond, could result in a time lag in the decrease of the resistance from high to low value. All this would mean that for steep currents the effective resistance is sustained somewhat higher than for the longer fronts. However, considering that the inductance effect predominates during these rapid current variations, the method of analysis followed for the driven grounds hardly demands greater refinements for engineering purposes. For the extensive grounds the 60-cycle resistance was used which procedure should be on the conservative side.

The calculations in Figures 18 and 19 of the paper for the counterpoise are for grounds of fairly low resistivity. We realize that most counterpoise applications are in soils of high resistivity. These curves were intentionally drawn for soil of low resistance to accentuate for comparison purposes the inductive effect. The current waves in the illustration of Figure 27 of the paper would be of longer duration for a slower velocity of propagation than that assumed. A velocity of propagation of the current in the earth slower than one-third the velocity of light is not only possible but quite probable. This would mean that the duration of the lightning current as deduced from the method of analysis in the paper approaches closely the average duration of 40 to 50 microseconds for lightning current discharges actually recorded in the field.

Electrical Stability of Electrical Insulating Oils Under Limited Oxidation

Discussion and author's closure of paper 43-23 by J. C. Balsbaugh and A. G. Assaf, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, pages 311–22.

G. B. Shanklin (General Electric Company, Schenectady, N. Y.): The paper by Balsbaugh and Assaf describes a very fine research job on the oxidation of insulating oils. Their precision methods of test and the quantitative results obtained are above criticism. It is for this reason that I approach a discussion of their paper with some hesitation, because I have a very definite quarrel with the practical conclusions they derive from these test results. I quote briefly from their paper:

> "Operating performance of oil-paper cables in service shows, in general, that the power factor of the insulation increases with time, even in certain cases to the extent of seriously impairing the investment. Hitherto the changes have been unexplainable."

The paper then goes on to show that the "unexplainable" changes have been due to limited oxidation which, under certain conditions, is different and more severe in its action than continuous, unlimited oxidation. This is correct, but the authors fail to explain why it is more severe in practical cases and, in fact, draw the opposite conclusion in their statement:

> "Apparently the oxidation products or other compounds formed during limited oxidation are not of the type to be selectively absorbed by the paper."

Just the opposite is true, as far as cable in service is concerned. The products formed during the early stages of limited oxidation are readily absorbed by the paper, consisting as they do largely of metal soaps in colloidal solution. Perhaps their method of setting up the test samples obscured this. At any rate, in an actual cable structure this absorption of conducting products accounts for pronounced increase in dielectric power factor of the cable as a whole, when and if it occurs.

Now that this alarming statement has been made let us see just how it affects practical cable design and how well it is under control. There is no denying that a paper-insulated cable is a very efficient oxidation machine. The copper-oil and lead-oil ratios are high, and the cable is designed to operate at relatively high temperatures. It can be expected, then, that the "oxidation cycle" will eventually be carried out to its fullest extent, all oxygen in free and combined form being consumed in the ultimate end products of this complicated chemical action.

The criterion of cable deterioration from this cause is a measure of dielectric power factor as a function of temperature. If this oxidation action does not materially increase the dielectric loss of the cable as a whole, it does no real harm. In this respect, here is an interesting and important observation. Service failures in modern high-voltage paper-insulated cable from this cause are rare and, without exception, are fully accounted for by abnormal conditions leading to abnormal amounts of oxygen and oxidation products in the cable system.

Carefully controlled tests and operating experience have demonstrated time and again that the degree of cable deterioration is proportional to the amounts of oxygen and oxidation products allowed to intrude. Obviously then, the remedy is to exclude abnormal amounts of such corrosive impurities from the cable system. This fundamental principle has been recognized and followed in cable design for so many years it is hard to remember just when it was first established. That, after a period of gradual improvement, it is today under full control by all responsible cable manufacturers, there can be no doubt. It is only through accident or mistake that service trouble from this cause occurs, and this is rare and isolated.

I, accordingly, must take exception to the authors' conclusion that limited oxidation has been unexplainable in the past and is a significant and vital factor in high-voltage cable design. This implies that it is not understood and not under control, which is not correct.

I believe there is a reasonable explanation for their conclusion. Under controlled laboratory conditions they introduced different amounts of oxygen into sealed test samples and measured the degree of deterioration caused thereby. They found that limited amounts of oxygen caused more pronounced deterioration than unlimited amounts and assumed that the limited amounts represented normal cable practice. The minimum amounts of oxygen for which they show end results of aging tests were 50 and 100 cubic centimeters per kilogram of oil. These amounts correspond to 5.5 and 11.0 per cent of absorbed oxygen by volume at atmospheric pressure, the latter representing complete saturation of the oil at this pressure.

The catch in the whole thing is that their minimum oxygen (5.5 per cent) is more than 100 times the amount allowed in properly made cable and could only represent an abnormal condition of extreme degree. Modern paper-insulated cable has no more than 1.0 per cent absorbed gas in the oil at atmospheric pressure. Of this, only a very small part is residual oxygen, and after it is consumed in the oxidation cycle there is only a minor increase in power factor of the first and last few layers of paper tape in near proximity to the conductor and sheath. This has no bearing at all on troubles in service. To sum the matter up, if the old and well-established principle of reducing impurities to a minimum, initially, and keeping them out of the cable system thereafter is followed, the troubles described by the authors will be nonexistent.

These remarks, I feel, are necessary for the purpose of keeping the practical problem involved in its right proportions. I hope that I am not detracting in any way from an exceptionally fine and useful quantitative research job.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): This research project has been planned to cover semifundamental problems relating to oxidation of insulating oils in transformers, cables, and so forth. The paper indicates a valuable addition to our background of knowledge on oxidation problems, both as to equipment in service and as to materials and equipment to be made in the years to come.

In this research the deterioration of the insulating oil is carried on with no voltage stress on it. It has been found, however, in tests on oil-filled cable in Chicago that, if the sample happens to have conditions leading to deterioration when subjected to high temperatures, this deterioration proceeds whether a high voltage or no voltage is impressed across the insulation.

According to Figure 5 of the paper, the maximum amount of electrical deterioration was produced by 100 cubic centimeters of oxygen per kilogram of oil. For a specific gravity of 0.84, this ratio corresponds to about $8^1/_2$ per cent of oxygen by volume. The solubility of oxygen in oil at atmospheric pressure is about 17 per cent, and the solubility of air is about 10 per cent, giving a solubility in the oil of oxygen when in air of $3^1/_2$ per cent. Apparently, the tests in this project with 50 or 100 cubic centimeters of oxygen per kilogram of oil correspond so far as concentration of oxygen in oil is concerned to the condition in sealed transformers, some of which have air above the oil.

Data derived from Figure 7 and Table I of the paper (see Table I of this discussion) show that an oil previously subjected to limited oxidation absorbs more oxygen and develops higher power factors under continuous oxidation than an oil subjected only to continuous oxidation. These data are of practical interest because they show that oil stored in contact with a limited amount of air and copper may under certain circumstances deteriorate faster than previously unexposed oil.

Table I

	Continuous Oxidation With 1.6 Square Centimeters Copper Per Gram After Limited Oxidation		Continuous Oxidation With 2.3 Square Centimeters Copper Per Gram Without Previous Limited Oxidation	
Time of Continuous Oxidation (Hours)	Oxygen Consumed (Cubic Centimeters Per Kilogram)	$\epsilon''f$	Oxygen Consumed (Cubic Centimeters Per Kilogram)	$\epsilon''f$
8	1,700	12.1	200	0.02
26	2,900	3.5	1,000	0.035
50	6,500	1.31	2,500	0.08
74	10,000	1.65	5,000	0.1

With regard to radial power-factor tests of cable insulation, the statement is made that the power factor of deteriorated insulation in a sealed cable may be higher than the power factor measured on the removed tapes because of instability of oxidation products upon exposure to air. As this problem was recognized, the power-factor measurements of tapes are made by the Commonwealth Edison Company with the tape to be measured remaining covered by the next outer tape until the instant of measurement. Then the exposure of the tape during transfer to the cell is a matter of a few seconds, and the measurement is completed in 20 to 60 seconds. During most of this time the tape is protected from the air by the electrode surfaces. In general, we have found excellent agreement between the power factors of the completed cable and the average of the radial power factors.

Under the heading Effect of Metal Surface Areas in the paper, it is pointed out that the copper-oil ratio in the investigations was less than in cable. This difference is partly compensated by the fact that the accessibility of the oil to the copper is less in cables than in the tests. Also, the diffusion of copper compounds in a cable is much slower than with the forced circulation in the tests. These points are demonstrated by the usual U shape of the radial power-factor curves.

It is of interest to note that the optimum aromaticity, corresponding to a specific dispersion of 110–120 according to the paper agrees closely with the figures of 115–117 given in the recent paper by Clark and Raab in Industrial and Engineering Chemistry.

Further researches will concentrate on learning the effects of higher temperatures. This work includes investigations up to 145 degrees centigrade on various oils.

F. M. Clark (General Electric Company, Pittsfield, Mass.): I have followed the work of Professor Balsbaugh and his associates at Massachusetts Institute of Technology for several years, and I wish to congratulate them for the vigor with which they have investigated the characteristics of mineral oil under various types of oxidation. They have developed ingenious test methods which in their hands and in the hands of others can be expected to yield valuable information during the years to come. In this paper, however, the authors are reaching out in an effort to apply the results of their academic researches and have made reference to the application of mineral oil in cables and transformers. To do this from the data available is difficult, and I fear that unless the manufacturer and user of electrical apparatus appreciates the difficulties the very excellent academic results obtained by the investigators are going to be subject to much misapplication and possible discredit.

In the commercial application of mineral oils there are certain fundamental properties on which the present commercial structure has been built. In the cable field, one of the requirements is that the dielectric loss of the oil-treated dielectric must be controlled to the point that heat accumulation is avoided. In the transformer field, one of the requirements is that the oil must possess a satisfactory resistance to sludge formation in order to secure satisfactory heat dissipation. To this latter requirement some may now imply another means of gauging a transformer oil, that is by studying the power-factor characteristics under oxidation. To this there can be no objection if greater power-factor stability can be obtained without sacrificing the basic requirement which demands freedom from sludge formation.

In its broad aspects the present paper calls attention to the behavior of mineral oil under unlimited oxygen supply and under a limited oxygen supply. In this they support the conclusions of previous investigators and the practical manufacturer. While it is still undoubtedly true that many investigators still test oil in an open beaker, it is nevertheless generally recognized that such tests are not used by the manufacturer of electrical cables as a criterion of the satisfactory oil for cable use. The final criterion is the behavior of the treated insulation, generally the behavior of the cable itself. Oils which possess good or bad characteristics in an open beaker with free oxygen supply are not necessarily correspondingly good or bad in the treated insulation test, even when the test on the treated insulation is carried out under oil, the surface of which is exposed to air. The presence of the cellulose insulation and the effect of voltage application play a part which cannot be ignored. The authors appear to recognize this fact in their presentation of the limited oxidation test. Whether there is a valid relation between the results of the limited oxidation test on an oil and its behavior under voltage in a commercial cable or transformer still remains to be determined. Until this has been studied the importance of the interesting behavior cited cannot be evaluated.

We in our laboratory have carried on an intensive study of insulating liquids under restricted oxygen supply but as yet have not found that a mineral oil or other liquid dielectric can be clearly evaluated in terms of its commercial suitability from its behavior when tested alone.

The difficulty of gauging the suitability of an oil for commercial use on the basis of a simple oil test is illustrated in the data presented for the series of number 60 oils. These oils include samples most nearly identical to those oils in present successful commercial use. The other experimental oils studied are to be classed as technical white oils which, irrespective of their academic behavior, in commercial use have been found unsuited for cable application. The series number 60 oils have been refined to produce different degrees of aromaticity. Figure 11 shows that irrespective of the aromaticity all the oils show similar behavior on limited oxidation using 100 cubic centimeters of oxygen per kilogram. Figure 12 on oil number 60–2 which most closely approaches the present American transformer oil shows that the greatest power-factor instability is obtained in the entire *absence* of oxygen. Furthermore, the more highly refined, substantially water white oil number 60–3 described in Figure 13 shows increased limited oxidation stability as a result of the greater degree of refining. Commercial experience would be more in agreement with the continuous oxidation test results of Figures 12 and 13 which indicate greater instability for the more highly refined oil.

With regard to many of the specific conclusions drawn, our work is in agreement. We cannot agree entirely, however, that under limited oxidation as it results in commercial cable use, the products of the oxidation are not absorbed by the paper. While it is true that many of the products of oil oxidation may not be absorbed by the paper, it is general experience that many of the most destructive products are absorbed. Among these are the copper compounds mentioned by the authors.

There are a number of features presented by the authors which will demand careful study before an investigation of this type can be translated into practical terms. One of these is represented in the data of Figure 22 which describes the behavior of new and reconditioned transformer oil. The authors have stated that "mineral oils may change their limited oxidation characteristics with time in standing in a container." Yet in Figure 22 the general behavior of the new and reconditioned oils is identical in type. Each reaches its maximum power factor at the same time. Each falls to a minimum value at the same time. Each then rises in power factor at about the same rate. Yet it is my understanding that the oils represent widely different degrees of oxidation. The new oil was substantially unoxidized. The reconditioned oil had seen commercial service in transformers and had been merely paper-filtered before the test described by the authors. It was not truly a "reconditioned oil" in the accepted sense, if by that term is meant an oil which has been chemically treated or fuller's-earth-treated in order to remove the products of previous oxidation. Just why the peak value in power factor should be obtained at the same time and the general behavior of two so widely different oils (on the basis of previous oxidation) be so similar is not understandable unless there be a factor such as an electrode effect which has escaped evaluation.

There is one other important feature which must be borne in mind in any attempt to apply the academic data presented. The authors relate the results of their limited oxidation studies to the conditions which exist in sealed transformers and cables. In general, the authors prefer to use about 100 cubic centimeters of oxygen per kilogram of oil. This corresponds to about 10 per cent oxygen by volume of the oil, or in terms of air, with which the practical engineer is concerned, to about 50 per cent air per volume of the oil. It is obvious that in a properly prepared cable or sealed transformer this quantity of air is far greater than can possibly be present. Even the 50 cubic centimeters of oxygen per kilogram of oil does not constitute a "restricted supply" of oxygen in the practical viewpoint. To get an oxygen supply corresponding to the maximum present in good commercial cable practice the total oxygen must be more severely restricted. When tested under conditions approaching commercial practice the data presented appear to indicate that the present commercial grades of transformer and cable oils possess an equal if not higher degree of electrical stability than that possessed by the various experimental oils described.

It is my understanding that the power factor measurements were made at a stress of but two or three volts per mil. If this be true, I suggest that it would be of interest and value to check the results at higher stresses covering the range from 25 to 100 volts per mil.

I hope that Professor Balsbaugh will be able to continue his interesting researches on mineral oil. Information of this type is of value in the continued improvement of oil-treated insulations, although at present its translation into terms of commercial practice is difficult and frequently misleading.

J. C. Balsbaugh: Both Mr. Clark and Mr. Shanklin question the statement that oxidation products formed during limited oxidations are not selectively absorbed by the paper. I think the difficulty is in the use of the term "selectively absorbed" and in what is meant by "absorption". In many cases, the term "absorption" is used to represent capillary absorption. However, in the present instance "selective absorption" occurs by the hydroxyl groups in the paper or the cellulose. Thus, a pure mineral oil itself is absorbed by paper by capillary forces. However, in this case there is certainly no selective absorption. Thus, when the term "selective absorption" is used in connection with the limited-oxidation work, it is meant that the hydroxyl groups of the paper do not selectively absorb the type of oxidation products that are formed under limited oxidation. These results from limited-oxidation studies are substantiated by quite a number of test results which have been given in both this paper and previous papers.

There seems to be some criticism on the use of 100 cubic centimeters of oxygen per kilogram of oil in a limited-oxidation test. This represents, roughly, a volume of oxygen equal to approximately ten per cent of the oil volume. In general, limited-oxidation tests are made from zero available oxygen—that is, with the oil in contact with the paper and copper—up to values of 1,000 cubic centimeter or more of oxygen per kilogram of oil. The purpose of this wide variation in oxygen availability is to determine the degree of influence of small amounts of available oxygen on the electrical properties. In many cases, contact of the oil with copper even in the absence of any oxygen is sufficient to give what would certainly be termed very significant losses. Other things being equal, it would certainly be desired to have an oil that is stable over a reasonably wide range of limited-oxidation conditions. Since such tests must, of necessity, be of an accelerated nature, it is perhaps desirable to use a somewhat larger value than would normally be expected. Corresponding procedures are used in other tests, for example, accelerated sludge tests in which the temperatures are used in the range well above operating temperatures.

It is to be regretted that definite information is not available as to the amount of oxygen which is available to the oil in an oil-paper cable. By this I do *not* mean the amount of air that may get into the oil during manufacture or installation. However, there is no denying the fact that there are residual oxidation products present in a commercially refined oil, residual oxygen in the paper even after vacuum treatment, and impurities in the paper that can possibly account for certain of the available oxygen. This is not in any sense a criticism of present manufacturing methods in the fabrication of oil-paper cable or the component parts such as the oil and paper. Rather than to attempt to improve any of these procedures, which certainly would be very difficult, it would seem to be more promising to study what components in an oil are responsible for high losses under limited oxidation conditions, then either to eliminate them or to prevent their effect through the use of additives.

It is true certainly that mineral oil should not be commercially applied on the basis of its stability to either limited or continuous oxidation. The electrical stability is only one factor among many which must be considered in determining the best oil from all viewpoints. The paper itself makes no attempt to evaluate any other properties than the stability under limited oxidation. The purpose of the work is, of course, to determine what factors are responsible for electrical losses under such oxidation conditions, so that proper steps can be taken to correct them. There is no reason why this cannot be done, and still maintain the other required characteristics.

Mr. Halperin's discussion makes a number of important contributions to the paper.

Harmonics and Load Balance of Multiphase Rectifiers

Discussion and author's closure of paper 43-26 by R. D. Evans, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, April section, pages 182–7.

C. W. Frick (General Electric Company, Schenectady, N. Y.): During the past few years I have come in contact with a number of rectifier situations, including both actual and proposed installations, in which coordination with telephone systems was given consideration. In a majority of these situations it developed that the factor of most significance was the rectifier's contribution to the voltage *TIF* at the power supply point. This is the case where a steam plant or large substation feeds a nearby rectifier load and a number of other loads which are spread out. Whatever wave-shape distortion the rectifier produces at the power station bus spreads over the transmission or distribution lines connecting the various loads to the station. Paralleling these power lines are circuits associated with the telephone plant in the territory and under some conditions there may be appreciable induction because of harmonic voltages. Thus the effect of a given rectifier layout on voltage *TIF* is useful information, and a simple method of estimating it would be very helpful.

Methods of calculating harmonic voltages produced by rectifiers and hence the voltage *TIF* are already available in the literature of the subject, and they may be applied to the various multiphase arrangements. However, the calculations are somewhat involved, require specialized information, and are seldom attempted by others than those who are familiar with the details. In looking for a short-cut method Figure 3 of the paper was found. Relative *TIF* is taken from the curve for the given number of rectifier phases and substituted in a formula to obtain actual *TIF*. But the formula involves some unfamiliar factors pertaining to rectifier circuits which would have to be made readily available to be of any value to the average user. Possibly this information could be supplied in the form of brief tabulations, supplemented by examples.

The paper calls attention to a "Proposed New *TIF* Weighting Curve." It seems inadvisable to call this the "1941 Curve" because it is as yet only a proposal. The 1935 curve went through similar stages during the several years preceding its adoption. Reference to the new curve emphasizes the fact that rectifier situations should be included in the study of the new proposal.

H. Winograd (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): I should like to supplement Mr. Evans' paper in regard to the effect of the harmonic components of the a-c voltage on the output d-c voltage of a rectifier.

In 1938, when the first rectifier installation was made in an aluminum reduction plant, the unbalance among the various six-phase units operating in parallel on the d-c bus was observed. This installation comprised ten 5,500-kw six-phase units, provided with phase shifters to form a 60-phase system.[1] Tests and an analysis showed that this unbalance was caused by the harmonic components in the a-c voltage, particularly the fifth and seventh, and their phase position in relation to the fundamental in the secondary phase voltage of the rectifier transformer. The phase position of the harmonics differed for the various six-phase units on account of their phase displacement from each other in the 60-phase system.

The maximum deviation from the average load current observed on any unit was about 7 per cent, which corresponds to a deviation of about 0.4 per cent in the d-c voltage. In regular operation, the load is balanced by means of grid control, and the unbalance is evidenced only by the difference in the amount of grid control for the various units.

The general expression for the effect of any harmonic components on the d-c voltage at no load (disregarding the angle of overlap) is:

$$\frac{E_{dn}}{E_{d1}}=\frac{\sin n\left(\frac{\pi}{p}\right)}{\sin\left(\frac{\pi}{p}\right)}\cdot\frac{1}{n}\cdot\cos n\beta\cdot\frac{A_n}{A_1}$$

in which n is the order of the harmonic, p is the number of phases in a commutating group, E_{dn} is the d-c voltage caused by the harmonic, E_{d1} is the d-c voltage caused by the fundamental, β is the angle of displacement between the harmonic and fundamental components and A_n, A_1 their respective amplitudes (see Figure 1 of this discussion). For the double three-phase connection with interphase transformer ($p=3$), which is most generally used, and for the nontriple odd harmonics, this expression is reduced to $E_{dn}/E_{d1}=\pm 1/n\times \cos n\beta\cdot A_n/A_1$.

The effect of the harmonics on the d-c voltage of a unit does not remain constant under all operating conditions. It would be affected by a change in the system connections which might change the magnitude or phase position of the harmonic components. The magnitude of the load would have influence, as the conducting period of each phase is prolonged by the commutating angle.

Regulation of the d-c voltage by phase control of the firing of the anodes would alter the effect of the harmonics on the d-c voltage, because of the phase shift of the conducting period in relation to the fundamental and harmonic components. The practical effect of this is that it is not possible to use wide-range group control of the d-c voltage by phase control of a number of parallel units which are displaced in phase from each other. If such group control is used, it is necessary to balance up the load among the units at intervals, while operating the group control.

The preceding expression may be modified to take into account phase control, by substituting E_{d1} cosine θ for E_{d1} and $(\beta-\theta)$ for β, in which θ is the angle of retardation of the firing.

The effect of the harmonic components on the d-c voltage may also have a bearing on the choice of rectifier connections. For

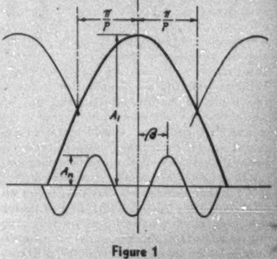

Figure 1

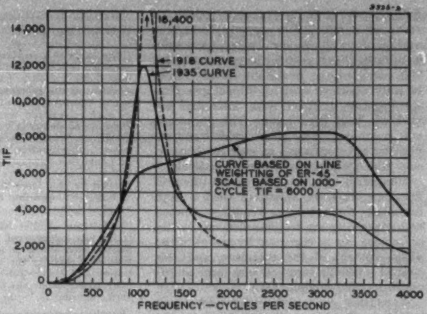

Figure 2. 1941 tentative revised *TIF* weighting

example, if two rectifiers are connected to a 12-phase transformer, it is desirable to connect each rectifier to a six-phase winding of the transformer, so that the load on each rectifier may be observed and adjusted.

In the majority of rectifier applications, the effect of the harmonic components in the a-c voltage is of little consequence. However, there may be special cases, such as a parallel resonance condition in the a-c supply for a frequency close to the fifth or seventh harmonic, where it may become an important factor.

Reference

1. Alcoa Rectifier Installation, J. E. Housley H. Winograd. AIEE Transactions, volume 60, 1941, pages 1266–74.

J. H. Cox (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): In the application of any kind of apparatus it is always comforting to know the limitations. Mr. Evans' paper is a comfortable contribution to our knowledge of power rectifier application. In the past the question of the harmonics generated by rectifiers caused some concern from the point of view of influence on both communication circuits and generator heating. Now, through the combination of analytical work, such as here reported by Mr. Evans, and field experience, the factors are much better known, the concern has largely vanished, and applications are made freely as desired.

Notwithstanding the large number of rectifiers that have been installed, the cases of even original communication difficulty have been negligible and the few cases that have been encountered have been corrected by relatively simple means where corrective measures in the power system were indicated. For the case of small stations, a few have required filters and for the case of large stations the corrective has been the use of a suitable number of phases. Experience has shown that modern generators can be loaded exclusively with rectifier load without important sacrifice.

Mr. Evans has discussed for the first time the relationship of rectifier harmonics to load division between rectifier groups and to connected capacitors. Here again a great amount of experience shows that the situations where the influence is enough to cause difficulty are rare, and where difficulty is encountered simple correctives are known. Some method of phase control can always be used to achieve load balance where needed, and Mr. Evans has mentioned the simple method of throwing a capacitor bank off resonance, should resonance be encountered. Incidentally, if a capacitor bank should resonate to a particular harmonic on a system it may be objectionable for other reasons than the presence of a rectifier.

H. E. Kent (Edison Electric Institute, New York, N. Y.): Mr. Evans' excellent paper deals with a timely subject from the standpoint of inductive co-ordination of power and telephone circuits. The war effort has resulted in a number of large capacity rectifier installations which, had it not been for the multiphase method of operation, would undoubtedly have given rise to widespread co-ordination problems. As mentioned in the paper, the Joint Committee on Research and Development of the Edison Electric Institute and Bell Telephone System has studied extensively the inductive co-ordination aspects of rectifiers. The subcommittee's latest report on this subject appears in a volume of engineering reports published recently. This volume constitutes the fifth in a series, which taken as a whole provide quantitative information for the use of the power and telephone industries in the engineering, construction, and operation of their plants to secure adequate co-ordination.

In his paper, Mr. Evans refers to various *TIF* weightings which have resulted from the work of the joint subcommittee. I should like to comment on these weightings and on their present status. The three weightings labeled, according to date, as 1918, 1935, and 1941, are shown in Figure 2 of this discussion. As previous papers before the Institute have described, the major factors involved in *TIF* weighting are the relative interfering effects of different single frequencies in the telephone plant and the coupling between the power and telephone circuits. It has been the changes which have been made since 1918 in the characteristics of the telephone message circuit and equipment toward a more uniform frequency response over a wider band of frequencies, which gave rise first to the 1935, and now the tentative 1941, revisions in the *TIF* weighting curve. It is believed that the shape of the tentative 1941 curve takes into account probable future developments so that further changes will not be necessary for an extended time. While the *relative* weighting of the various frequencies for the tentative 1941 curve has been determined, the absolute value of the ordinates has not been fixed yet. The ordinates of the tentative 1941 curve shown in Figure 2 were selected on a judgment basis to provide a means for securing data comparative with the 1935 curve. There has been devised a relatively simple modification of the Western Electric Type 2 noise meters which makes possible *TIF* measurements in approximate accord with the tentative 1941 weighting as well as by the usual 1935 weighting. This modification is of such nature that it can be made by the owner of the set without the necessity for returning the instrument to the manufacturer. It is planned to collect a considerable amount of these comparative data in connection with day to day field problems, which will be used in arriving at a decision as to what the new ordinate values should be.

In a footnote of his paper Mr. Evans states that the 1919 frequency weighting curve of telephone interference factor (*TIF*) remains in use for machine wave-shape guarantees. An explanation of the reasons for the use of the 1918 instead of the 1935 weighting for this purpose may be helpful. The question of wave-shape guarantees for machines of 1,000 kilovolt-amperes and larger was studied by representatives of the manufacturers and electrical utility industry in 1932. Out of this study came certain recommendations which were presented in a National Electric Light Association committee report entitled "Generator Wave Shape," published in August of that year. These were based on the 1918 *TIF* weighting curve, which was in use at that time. Following the adoption of the revision in *TIF* weighting in 1935, the new weighting was not immediately applied to the matter of machine guarantees, as a trial period for further accumulation of comparative data on the two bases seemed desirable. Later on it became apparent that additional developments in the telephone plant and equipment would require a further revision in *TIF* weighting. It was decided therefore not to make any change until this further revision was complete and to continue to use the 1918 *TIF* weighting in the meantime.

To summarize, the 1918 *TIF* (Telephone Interference Factor) at present is used only in connection with the specification of machine wave shape. The 1935 *TIF* (Telephone Influence Factor) is still used in all field studies of noise frequency inductive co-ordination problems. However, in connection with such field studies an attempt will be made to secure comparative data with the 1941 tentative weighting, looking toward the adoption of a new curve to supplant completely both the 1918 and 1935 versions.

References

1. Review of Work of the Subcommittee on Wave-Shape Standard of the Standards Committee, H. S. Osborne. AIEE Transactions, volume 38, 1919, pages 261–88.

2. Measurement of Telephone Noise and Power Wave Shape, J. M. Barstow, P. W. Blye, H. E. Kent. AIEE Transactions, volume 54, 1935, December section, pages 1307–15.

J. W. Butler, L. W. Morton, and **C. C. Herskind** (General Electric Company, Schenectady, N. Y.): This paper presents a thorough qualitative analysis exploring the various possibilities concerning harmonics as related to large rectifier installations. We feel many of the questions raised cannot be fully answered until additional quantitative studies, coupled with tests and experience, have been made. This discussion will point out those effects which our experience substantiates, as well as those issues which we feel need further analysis.

The effect on the performance of rectifiers operating on a system with a high harmonic content is a tendency toward unbalance between rectifier elements. Any unbalance can be corrected by means of the phase control of the rectifiers, and this control is usually supplied with large multiphase rectifier equipments.

A rectifier connected to an infinite a-c bus normally draws all the nontriple odd harmonics. However, it is not essential that all of these harmonics flow. For example, if a harmonic trap having high fifth harmonic impedance is connected in series with each of the a-c lines feeding the rectifier substantially no fifth harmonic current will flow to the rectifier. Inasmuch as the rectifier commutation period is not fixed but can adjust itself to load and circuit conditions, it is questionable whether the presence of a shunt capacitor of such value as to operate with the line reactance as a fifth harmonic trap, will cause an increase in fifth harmonic current in the line. This phase of the problem should be given more study than that indicated in the paper before it can be definitely stated that it is analogous to the transformer magnetizing current problem.

Under the section "Heating of Capacitors," Mr. Evans has clearly presented the capabilities of the capacitor to withstand harmonic loading, but we feel unnecessarily strong emphasis has been placed on the rectifier-capacitor question, namely, "Consequently, special attention should be paid to the harmonic problem for installations involving multiphase rectifiers and capacitors." And "Where large capacitors are connected in parallel with large multiphase rectifiers supplied by transmission systems, it is particularly desirable to give careful consideration to the harmonic problem. The effects for the lower frequencies should be studied analytically, and by field tests which should give, if possible, the magnitude of the fifth-harmonic voltage in the supply and the harmonic-frequency impedances. With this information available the capacitor application should be studied to determine what characteristics at harmonic frequency should be employed. It appears possible but unlikely that in a few installations the subdividing of the capacitor into sections tuned for various frequencies will be found desirable."

It is felt that statements of this nature generally leave the wrong impression. It leads one to believe that in a proposed installation, extreme care should be taken in applying capacitors and in choosing the number of rectifier phases.

Our experience does not seem to bear out the contention that detailed analytical studies and field tests need be made in advance to insure successful operation.

An installation of 57,000 kw of General Electric rectifiers and 20,000 kva of Cornell-Dubilier capacitors in the northwest, is reported by J. R. Curtin in the January 9 *Electrical World*[1], from which the following excerpts are taken:

> "The loads at Longview consist of 31,000 kw to a 48-phase rectifier, 26,000 kw to a 24-phase rectifier and 3,000 kw to a local public utility district. Ordinarily, the problem of supplying leading reactive kilovolt-amperes in conjunction with a load of such magnitude would require either a synchronous condenser or static capacitors with enough oil circuit breakers to approximate continuity of control. Fortunately, however, the rectifier load at Longfellow is practically constant at all times, and two static capacitor banks with one oil circuit breaker for each bank have been found to be satisfactory. No operating troubles have been experienced to date.
>
> "The danger of difficulties with harmonics in this region was well realized, and so, as soon as the capacitors were almost all installed (June, 1942), joint tests were made by the Bonneville Power Administration and Pacific Telephone and Telegraph Company to determine the effect of the capacitors on harmonics originating in the rectifiers.
>
> "Results of extensive testing with various amounts of capacitance and with both balanced and unbalanced rectifier operation showed that a near resonant condition, as anticipated, did exist for the eleventh harmonic with 3,330 kva of capacitors on the 48-phase rectifier bus, but this condition disappeared with increased capacitance. The tests also showed that the telephone interference factor was reduced to about one-tenth its former value when the capacitors were connected, other conditions remaining fixed."

The operation of rectifiers is considered good when an operating record of one arc-back per rectifier per month is obtained, and the rectifiers reported in this article during 1942 averaged less than 0.75 arc-backs per rectifier per month, which is better than good.

Another installation with which we have had experience comprises 118,000 kw of rectifiers and 22,500 kva of capacitors, all General Electric. It has been established here that factors other than the shunt capacitors are responsible for such operating difficulties as have been encountered.

Similar to the northwest installation, an extensive test program was conducted in this region by the Bell Telephone Laboratories and interested companies, in which we believe Mr. Evans participated, and somewhat similar reductions of *TIF* were obtained with the capacitors in service, under various operating conditions, indicating the acceptability of the performance in this respect.

We are acquainted with one other installation in the south comprising Westinghouse rectifiers and General Electric and Cornell-Dubilier capacitors, of which we have little information on operating experience. Possibly Mr. Evans has had experience with this installation or otherwise which he could describe bearing out his conclusions, and thus aid us in ventilating the matter.

Further on we find, "If the capacitor is supplied through a transformer (or autotransformer) and connected to the bus which supplies rectifier transformers, the effect is equivalent to putting a reactor in series with the capacitor and thus to tune it for some (not necessarily integral) harmonic frequency of the supply system." It is one of nature's laws that any combination of inductance and capacitance has a resonant frequency, so the qualitative picture is unchanged whether the capacitor *is* supplied through a transformer or not. If a capacitor bank is supplied through an autotransformer, the autotransformer will have such a low reactance in comparison to the supply system it seems useless to consider the combination alone in any quantitative analysis. And, if the capacitor is supplied through a two-winding transformer, the transformer no doubt will be much larger in rating than the capacitor, presenting the same relative case as with the autotransformer. The transformer would be larger, for otherwise the transformer would have been put in only to accommodate the capacitor which, in general, would be uneconomical and unnecessary, for high-voltage capacitor equipment can be made by series-parallel connection of lower voltage units at very little additional cost. If, on the other hand, the transformer were equal in rating to the capacitor, it would—or could—be made to have about four or five per cent reactance which would make the combination tuned to present a low impedance to the fifth harmonic and thus obviate the necessity of utilizing a separate reactor as discussed by Mr. Evans. So on this point we also ask if we can have the benefit of Mr. Evans' experience. We have had quite a bit of experience and have in effect passed the worrying stage on the points presented in this discussion, so we cannot appreciate Mr. Evans' points of view.

REFERENCE

1. CAPACITOR BANK 20,000 KVA ON LONGVIEW SUBSTATION, J. R. Curtin. *Electrical World*, January 9, 1943, pages 40–2.

R. D. Evans: C. W. Frick has rightly called attention to the application of voltage *TIF* in co-ordination studies and to the usefulness of the short-cut method presented in connection with Figure 3 of the paper. The application of Figure 3 involves the use of supply-circuit reactance and data that can readily be supplied by the rectifier manufacturer. However, it is possible to simplify the formulas for the primary-circuit values of the $I \cdot T$ product and the voltage *TIF* as follows:

$$\text{Actual } I\cdot T \text{ product} = \frac{I}{R}(\text{relative current } TIF \text{ from Figures 1 or 5})$$

$$= 0.78\left(\frac{\text{watts d-c}}{PCR}\right)\times \left\{\frac{\text{relative current } TIF \text{ from Figures 1 or 5}}{\text{primary voltage, line-to-line}}\right\} \quad \ldots\ (\text{approximately}) \text{ and}$$

$$\text{actual voltage } TIF = 1.34\left(\frac{\text{per cent } X}{PCR}\right)\times (\text{relative voltage } TIF \text{ from Figure 3}) \quad \ldots\ (\text{approximately})$$

The two approximate equations are given in terms of primary voltage, d-c watts, and phase control ratios (*PCR*). These two equations apply to the two cases with phase control ratios equal to one and to 0.85, respectively; they assume a total transfer reactance corresponding to an angle of overlap of 25 degrees as a plain rectifier. The value of the supply reactance given as (% X) is based on primary voltage and given d-c watts. In connection with the caption of Figure 3, it should have been pointed out that I_{ac} for this case is based on d-c watts and primary voltage and differs from the values used in Figure 1 and elsewhere in the paper.

H. O. Winograd has presented analytical expressions for the effect of a harmonic on the average d-c voltage of the two six-phase groups of a 12-phase rectifier. In order to use these expressions to determine the amount of load unbalance, it is necessary to consider the regulation of each phase group. When this is done, the formula will reduce to that shown in equation 1 of the paper. Mr. Winograd has also emphasized the importance of load balance in minimizing harmonics in multiphase rectifiers.

H. E. Kent has presented a valuable discussion which clarifies the relation between the 1919, the 1935, and the 1941 frequency-weighting curves and the circumstances that control their applications. I might add that the 1919 frequency-weighting or *TIF* curve is not used for rectifier co-ordination problems. Consequently, the 1935 curve is the one in common use. Since the shape of the 1941 frequency-weighting curve has been established, it was possible to include as part of the present paper the results of studies, such as given in Figure 2. There is no reason why such studies should not be made at this time as they will indicate the trend of future developments in regard to the effect of the number of rectifier phases on the telephone-noise problem. The captions of Figures 1 and 3 give formulas for obtaining, respectively, the actual current *TIF* and the actual voltage *TIF* from the curves which are based on the 1935 frequency-weighting curve. Similar curves, based on Figures 2 and 4, are not given because the actual value of the 1941 *TIF* curve has not been determined. While Mr. Frick is technically correct in questioning the terminology of the 1941 *TIF* curve, it is, nevertheless, convenient to refer to the 1919, 1935, and 1941 frequency-weighting and *TIF* curves.

R. M. Hawekotte and Mr. Kent have commented on the extensive co-ordination studies carried on by the Joint Subcommittee on Development and Research of the Edison Electric Institute and the Bell Telephone System. This work includes not only Engineering Report number 49, "Multi-Phase Rectifiers," published in Volume 5 of *Engineering Reports*, but also the forthcoming report on multiphase rectifiers and large capacitors in electrochemical installations. As Mr. Hawekotte and Mr. Kent have pointed out, the recent large installations of rectifiers in the electrochemical industry would have given rise to important co-ordination problems, except for the extensive use of multiphase connections.

The possibility of resonance at the lower harmonic frequencies with large shunt capacitors, particularly with a rectifier unit out of service, is a problem that was considered at some length in the paper. Mention has already been made of the fact that the Joint Development and Research Subcommittee is preparing a report on this problem. The views expressed by Cox, Hawekotte, Kent, and Winograd are in close agreement with those of the author and developed in the paper.

The discussion by Butler, Morton, and Herskind indicates that they are in agreement with the technical presentation made by the author, but that they are critical of a minor part of the author's paper concerning the desirability of special studies of harmonic problems where large shunt capacitors and rectifiers are to be operated, particularly under conditions with one or more rectifier units out of service. Certain statements from the author's presentation are selected as magnifying the need for such studies and tests. Careful reading of the entire section from which the selections were taken will show that the criticism is not justified. Briefly the author described the phenomena encountered, pointed out the possibility of certain resonance effects, and mentioned the remedial measure that consists of subdividing the capacitor into sections that are tuned for particular frequency. This part of the paper concluded with the observation that the use of such a remedial measure appeared "possible but unlikely."

As an alternative to ignoring the problem, it would seem preferable

1. To recognize the possibility of certain unusual resonance effects.
2. To show for the particular case by means of tests and analytical work that the effects need not require further consideration.

Wave-shape tests showing magnitude of lower harmonic voltages are frequently available and in any event are easily made. The possibility of resonant conditions being encountered for the lower harmonics is supported by the third paragraph from the excerpt of J. R. Curtin's article. Relatively simple calculations will frequently show for particular cases that resonance effect should not be important. In general, these procedures are desirable for capacitor installations whether made separately or in combination with rectifiers.

We do not understand the basis for much of the discussion by Butler, Morton, and Herskind, since the use of capacitors would be but little affected by the addition of measures to avoid resonance. There is an implication in their remarks that discussion of unusual operating features of rectifier and capacitor installations is an undesirable or dangerous thing. We see no reason for not presenting a straightforward technical discussion of the unusual operating features of such installations. We do not believe that technical information, such as given in the paper, will be misused by operating engineers. As to charge of causing unnecessary worry, it is our experience that recognizing a problem and making a logical analysis of it is the surest way of dissipating any worry that may be connected with it.

Pulling Loads on Single- and Multiple-Conductor Impregnated-Paper Lead-Encased Cable, Solid Type

Discussion and author's closure of paper 43-30 by A. P. S. Bellis, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, March section, pages 138–48.

William A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): A surprising thing brought out by Mr. Bellis is that damaging pulling stresses are so low that a considerable proportion of cables now installed must have been in this damaged condition when pulled in and ready to be spliced. Yet they have continued to operate in spite of the presence of air which undoubtedly entered the voids created by the lead stretching during installation.

Some 12 years ago Electrical Testing Laboratories made a study of the distribution of failures in service, in the year 1930, with respect to distance from the nearest joint. On the assumption of uniform distribution, the number of failures per foot of cable lengths in which failures occurred would have been 0.6 per cent. However, the actual distribution was from nearly nine per cent of the total failures per foot in the first foot to ${}^1/_{10}$ of that number between 10 and 15 feet and to ${}^1/$[illegible] beyond 15 feet.

This might be seized upon to prove that end damage by pulling with cable grips has a decidedly bad influence upon cable life. However ETL also made a similar study of the distribution of cable failures on high-voltage time test samples which, of course, had never been subjected to strains due to pulling grips. The distribution of failures was of exactly the same character and, taken over the same period, even more pronounced in the piling of failures near the cable ends.

The weakness of cable ends is, undoubtedly, due to voids, but we see from these ETL tests that the voids are not necessarily created by pulling grips.

We, therefore, must conclude that neither lead stretch nor void formations at cable ends are reliable criteria of serious cable injury, and that, while cable grips are likely to cause lead stretch and voids, their use can be justified for almost any size or type of cable, if ducts are of adequate size, and pulling lengths are not excessive.

Mr. Bellis' data are conclusive as far as they go, but, before they can be used safely to establish rules for pulling stresses, they should be co-ordinated with such practical data as may be obtainable from the records of operating engineers.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): This paper is based on an impressively large number of laboratory tests, and the results constitute a valuable addition to the knowledge on this subject. Although large quantities of lead-sheathed cable have been pulled into underground ducts over a period of some 50 years, no limits for the pulling forces have been established. It has been common practice to assume that, if the sheath was not pulled apart at the leading end or pulled out to such an extent that the insulation was disarranged and if the sheath was not badly scored, then the cable was not injured seriously in pulling.

Apparently this method of judging the pulling has had no serious consequences. No cable failure in Chicago has been ascribed to damage due to pulling other than damages caused by stones in the ducts or uneven ducts, and there are very few failures for which other causes are not found. Even in the rare cases where the leading end of the cable is damaged in pulling, it has been possible to cut off a few feet at the end and to operate the remainder of the cable.

Recommended limits for the pulling forces are obviously desirable for design purposes on which to base such features as the maximum length of cable that can be pulled, size of ducts, minimum radius of curves, number of curves in a run; and to determine the extent to which it is necessary to clean the duct and lubricate the surface of the cable. It should be recognized at the outset that there will be cases where exceptions must be made to any limits that may be evolved when there might be some risk of injuring the cable. Such risks might have to be assumed in order to permit the job to go ahead.

The conclusions in the paper are based on tensile tests of eight-foot samples of cable with a wire mesh grip at each end. The behavior of long lengths of cable in pulling would be somewhat different from that of the short samples. Also the results are not entirely applicable to pulling with pulling eyes attached to the cable ends. This is common practice for heavy cables and long lengths.

About ten years ago some tests were made by H. F. Moore, professor at the University of Illinois, on lead sheaths that had been subjected to very heavy pulling forces (10,000 pounds or more) and also on samples of sheath from the same cable at the trailing end where the pulling forces were very low. No appreciable difference was found in long-time creep tests and in long-time tests to fracture under steady loads. It was concluded that the properties of the lead were not changed appreciably.

One of the author's conclusions is that damage to the cable caused by excessive pulling forces can be detected by changes in the outside diameter of the cable. A more certain method is to remove some of the sheath and examine the cable insulation. This is done in building the joint or terminal.

Three of the conclusions suggest that distension of the cable at the end from pulling causes a concentration of heating and failures at the manhole ends. Probably it does increase the voids in the insulation of solid-type cable which increases the ionization in high-voltage cable. However, where hot spots have been found in manhole ends, they have usually been due mainly to infiltration of oil or viscous compounds from joints. There has been no concentration in manholes of service failures because of deficiencies of the insulation itself.

Howard S. Phelps (Philadelphia Electric Company, Philadelphia, Pa.): Mr. Bellis has investigated, in a practical manner, a problem of major importance to those engineers responsible for the installation, operation, and maintenance of underground cable systems.

The problem which he considered differs materially from the investigations of "The Creep of Lead and Lead Alloys Used for Cable Sheathing" reported by H. F. Moore and M. J. Alleman in Bulletin 243 of the Engineering Experiment Station of the University of Illinois, as well as other reports on this general subject from the Engineering Experiment Station by Professor Moore, and the investigation of "Internal Hydraulic Bursting Tests of Lead Cable Sheathing," carried on by Phelps, Gates, and Kahn of the Philadelphia Electric Company. The latter was reported at the Annual Convention of the American Society for Testing Materials in June 1940.

The present investigation was for the purpose, at least in part, of determining the strength characteristics of lead cable sheathing under stresses closely approaching the ultimate for relatively short-time intervals, of the order of minutes. The other two investigations were primarily for the purpose of determining mechanical characteristics of lead sheathing under lower stresses applied for time intervals extending into years. Without recognizing these fundamental differences between the respective investigations one might readily become confused in reviewing the different reports and possibly conclude the results were conflicting.

Referring now to the paper by Mr. Bellis, particular attention should be given to the conditions under which he observed the occurrence of substantial cold flow of commercially pure lead cable sheathing at stresses of approximately 1,500 pounds per square inch. This occurred under short-time loading. Since Mr. Bellis was investigating the characteristics of lead cable sheathing during installation, the time intervals during which the test loads were applied were of the same order, as those during which cable is being pulled in, a few minutes in each case.

However, it should be pointed out that, under conditions which differ substantially from the foregoing, the stresses at which cold flow may occur in commercially pure lead cable sheathing may be entirely different. The "Internal Hydraulic Bursting Tests of Lead Cable Sheathing," referred to previously, indicated that substantial cold flow can be expected at stresses considerably lower than 1,500 pounds per square inch, when applied for only a few hours or days. However, those conditions are outside of the scope of the present investigation.

The binding or gripping of the core by the sheath, that was observed by Mr. Bellis, apparently increases the ultimate tensile strength of the lead from the point of view of cable pulling strains. Accordingly, it would seem preferable to use the values reported by him for determining safe pulling strains for installing cable, and the ultimate tensile strength values reported by Professor Moore and other similar investigations for determining if stresses resulting from internal pressures developing in oil-filled cable, cable risers, or cables of considerable length installed on heavy gradients can be safely withstood.

Direct comparison of the ultimate tensile strengths, reported by Mr. Bellis and Professor Moore, is difficult without more definite knowledge concerning the temperatures at which the determinations were made. In order to emphasize this point, reference is again made to the investigation of internal bursting strength by the Philadelphia Electric Company. There it was disclosed that under similar loading conditions the internal bursting strength of cable sheathing, as measured by the time for failure of the specimen to occur, at 150 degrees Fahrenheit might be as little as three per cent of the strength at 50 degrees Fahrenheit. In some cases, the decrease in strength was even greater.

This paper and the discussion of it may raise some questions concerning the desirability of using alloy sheathing, particularly some of the calcium alloys. In general, the tests carried on by Philadelphia Electric Company indicate that while alloy sheathing has greater short-time strength than commercially pure lead sheathing, the apparent advantage in favor of the alloys disappears rapidly as stresses are reduced, and the period during which the loading is applied is extended. This characteristic appears to be particularly true in the case of calcium alloys. Apparently, the mechanical strength characteristics of the various types or grades of lead cable sheathing do not differ materially at the stresses occurring in practical operation.

C. J. Snyder and **L. F. Roehmann** (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): One of the conclusions reached by this investigation is that the pulling force on a cable when using woven wire grips can be such that the stress in the lead sheath reaches 1,500 pounds per square inch. This conclusion is extremely interesting because it shows that the lead sheath is reinforced by the cable core. An empty lead sheath of average commercial lead has been found in our laboratory to stretch rapidly at a stress above 1,000 pounds per square inch and to fail in approximately 100 minutes under a sustained load of 1,500 pounds per square inch. The calculated tensile strengths of the lead listed in Tables I, II, and III, of the paper, can be considered therefore only as the apparent tensile strength of the lead under the conditions of the test.

The same conclusion is reached if one contrasts the average tensile strength of the lead proper, given as about 1,800 pounds per square inch in Appendix II, with the (apparent) strength of about 3,000 pounds per square inch as derived from the pulling tests of cables.

This shows that it is not the sheath which takes the entire load, but part of the load is taken by the core also: the total pulling force is equal to a sheath force plus core force.

If this result is accepted, the next problem is to correlate the core force with conductor size and insulation wall thickness. It seems reasonable to assume that the larger the conductor cross section for a given insulation thickness, the larger the core force, and that the larger the insulation thickness, for a given conductor size, the smaller the core force (because of slippage of the oil-impregnated paper).

If these ideas will be substantiated, perhaps by some appropriate additional tests, then it will be possible to reanalyze the test data presented in this report, not on the basis of lead sheath cross section alone but considering conductor size and insulation thickness also. In this way it might be possible to arrive, for each cable design, at a maximum pulling force, to be used, as a guiding figure, for the installation.

We wonder if the author, or anybody else working in this field, has already analyzed the data from this viewpoint.

A. P. S. Bellis: The presentation of the cable installation problem in a clear and concise manner as given by Mr. Halperin makes apparent the contrast of premises in the case of two cable pulling problems.

In pulling with grips any damage as the result of overstressing the cable sheath can be cleared by cutting off a few feet, providing there is sufficient manhole slack allowance.

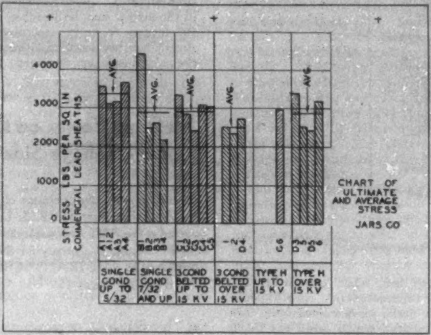

Figure 1. Chart of ultimate and average stress

In pulling by pulling-eye attachment any damage to the cable will be effective on the sheath only in the form of gouges or tears in the lead. There can be little or no disturbance to the mechanical uniformity of the insulation unless the stress on the conductors to which the pulling eye is attached is excessive. Under this condition the limiting load serves to protect the sheath not from excessive stress but damage from abrasion, gouging, forced inclusions, and high coefficients of friction. These factors can be minimized to a great extent by proper preparation of the conduit and adequate lubrication.

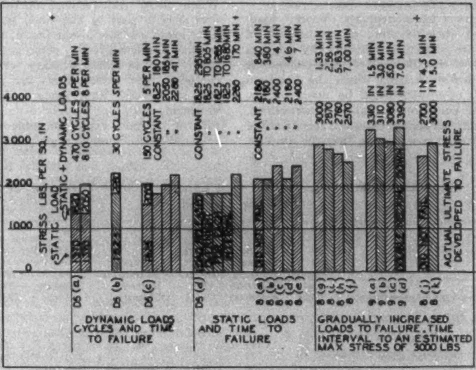

Figure 2. Actual ultimate stress developed to failure

The discussion, in addition to presenting the practical cable installation considerations in judging whether cable may have been damaged, accentuates the potential value of these reported laboratory tests. It was expected that the results of these tests would enable one to evaluate better the pulling load that a lead sheath cable could be subjected to without serious resulting damage. No rigid rules are proposed. The responsibility of experienced judgment in the cable pulling art will rest where it has always been, with the men handling the job. It is not expected that the recommended maximum apparent stresses or maximum recommended pulling length values be observed unless the conditions upon which they are predicated render them applicable.

The substance of the report is factual. It casts no reflections on the art as it has been pursued. The observations and conclusions record matters believed to be important in minimizing cable damage that heretofore may not have been realized was being done and to eliminate as far as possible those exceptional cases which make it necessary to cut back a few feet to eliminate damaged ends. It is important not only to eliminate the borderline cases where damage to the sheath is apparent but also to limit as far as possible appreciable weakening of the original quality of the insulation by minimizing the disturbance to its original mechanical uniformity.

Evidence of excessive stretching of the lead sheath best can be realized by graduating the pulling end of the cable sheath with circumferential marks ten inches apart. A good idea of potential damage to the insulation may be had by observing the extent of the coning of the insulation off the end of the conductors.

It is believed that observations of elongation of sheath and an examination of the underlying insulation as to the change from its normal mechanical uniformity will be very informative and will permit some correlation of these laboratory results sheath with field experience. This information may ultimately enable one to set values of maximum elongation of the sheath in ten inches, beyond which damage to the insulation may be considered as having occurred and whether it is necessary to cut back the end to clear the damage done. If excess manhole slack is allowed for the section length in border-line cases, there is no serious result except the economic loss of three or four feet of cable. This should be balanced against the cost of attaching pulling eyes in these few instances.

From an examination of the serious effect on the mechanical uniformity of the insulation under the point of excessive sheath stretching there is evidence that rather large spaces are created between the convolutions of tape and that the number of registrations may be increased. It may be this makes provision for the infiltration of an unusual amount of joint compound which is recognized as one of the main reasons for cable heating adjacent to the joints.

Mr. Del Mar states that "before they (the data) can be used safely to establish rules for pulling stresses they should be coordinated with practical data obtainable only from the records of operating engineers." It is because of a dearth of factual data pertaining to pulling cable with woven wire grips upon which rules could be based that the need for these tests arose. It is hoped that all practical data now available and additional information as it becomes available will be presented through the technical press.

Work to obtain these data should not be too burdensome as only the pulling end of the cable presenting borderline cases should be considered.

There is no implication in the report that the use of pulling grips is not justified. In borderline cases it is merely a matter of choice of taking a chance on damaging the cable insulation, making it advisable to cut off a few feet of cable to scrap or specifying the attachment of pulling eyes.

In the interest of improved design and proper application and use of pulling grips the Kellems Company contributed greatly to the value of this work. They have expressed a keen interest in the successful use of their product and would not want woven wire grips used at the expense of the cable they are designed to handle.

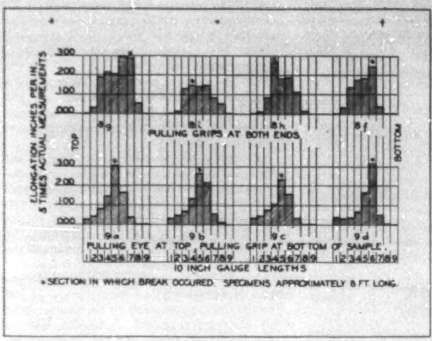

Figure 3. Elongation inches per inch—five times actual measurements

It is surprising to read Mr. Del Mar's conclusion which incorporates the statement that "neither lead stretch nor void formations (at cable ends) are reliable criteria of serious cable injury." It would be interesting to know what criteria should be judged adequate. So much has been said and recorded regarding the effect of voids and lack of mechanical uniformity in cable insulation it is difficult to accept unquestionably this conclusion.

In the future it may be necessary to differentiate between voids in cable insulation resulting during application of the taped insulation and as a result of handling of the cable in production or under normal installation conditions and those supervoids that are introduced by way of major disturbances to the mechanical uniformity that result from stretching the lead excessively when pulling the cable in conduit.

There are one or two qualifying factors which should be given weight when considering the comparison which Mr. Del Mar draws from data in the cited Electrical Testing Laboratories' report.

The report records examination of 696 failures, 570 of which were attributed to external causes. The difference of 126 were located with respect to the nearest joint. It is not stated that the distance measured involved the pulling end of the section. Of these failures, 87, or 69 per cent, were classified by the examiner as inherent. Involved within four feet from the joint is 18.4 per cent; within ten feet, 30 per cent; and 25 feet, 50 per cent. These are formidable proportions of the total and a serious reflection upon initial cable quality, if inherent. Failures attributed to inherent faults in the cable would seem to bear no relation to failures associated with high-voltage tests on cable samples. Such failures are attributed to end effects which are quite controllable in the preparation of the samples for test. The parallel as drawn is of interest as are the percentage failures per total length of cable involved. However, it should be realized that the problem which we are considering in connection with the present report involves the relatively small portion of the total cable footage that is included at the pulling end of the section lengths.

Mr. Phelp's discussion does a great deal toward clearing the muddied waters one is likely to get into when translating lead sheath test characteristics into expected performance under service conditions. Material is presented which could not be included properly in the report. I am heartily in accord with the distinctions drawn and feel they add materially to a better understanding, scope, and interpretation of the results presented in this report.

Snyder and Roehmann are correct in interpreting the tensile stress values for lead sheath as "apparent." Other important factors in estimating the permissible pulling load are the grade of commercial lead or lead alloy that is to be used, the time of application of the load, and whether it is a steady pull or involves superimposed dynamic surges as a result of inherent elongation characteristics of the pulling rope, slippage of the pulling rope on the winch drum, or snubbing action on the cable at the entrance end.

Results of tests involving dynamic loading and variable time of application of stress are shown graphically by Figures 1 and 2 of this closure. It may be possible to develop data from which the proposed analysis of results could be made on the basis of variation in cable constructions, but if only borderline cases are considered of importance it would seem that the simplest practical procedure would be to specify the attachment of pulling eyes in these cases.

A good idea of the variation in the extent of the elongation throughout the entire length of two sets of specimens may be had from Figure 3. From similar data obtained in the field it may be possible, sometime in the future, to judge accurately when the sheath has been stretched excessively and the cable insulation damaged.

Metering Power on the Low-Voltage Side

Discussion and author's closure of paper 43-34 by A. J. Petzinger and B. E. Lenehan, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, April section, pages 159–63.

G. B. Schleicher (Philadelphia Electric Company, Philadelphia, Pa.): In presenting this paper the authors have contributed a valuable outline of possible methods pertaining to a branch of electric metering on which the literature is relatively scattered, hence difficult to locate. Some of the methods would have greater use if their advantages and economies were more generally recognized. This review of the various methods in a single paper is therefore of value to the industry.

Experience with compensating metering has made available some interesting data that has a direct bearing on the conclusions that may be drawn from this paper.

As engineers we are accustomed to thinking of transformer losses as two per cent, or in larger sizes perhaps only one per cent. When these transformers operate under load only a part of the time, perhaps at light loads and low power factor, the losses become a much greater percentage of the load. Also, in selling primary service to a customer, the transformers are generally customer-owned, and, being a major investment, they are bought often to take care of business expansion that may not materialize for many years. Table I of

Table I. Range of Monthly Measured Per Cent Losses in Kilowatt-Hours and at Time of Maximum Demand

Customer	Business Class	Months of Test	Transformer Capacity (Kilovolt-Amperes)	Billing Power Factor (Per Cent)	Range of Maximum Demand (Kilowatts)	Range of Monthly Per Cent Loss	
						Kilowatt-Hours	Maximum Demand (Kilowatts)
A	Abattoir	15 22	50 112.5	83 83	46– 82 50– 76	3.0– 3.4 2.6– 3.8	3.1–5.8 2.8–3.8
B	Machine shop	31	75	67	53– 76	2.4– 6.4	0.8–4.6
C	Silk mill	4 21	50 112.5	68 78	82– 88 36– 103	4.3– 4.6 3.3– 5.6	3.8–4.5 1.4–5.5
D	Street railway	24	300	62	64– 170	3.1– 5.0	2.0–4.8
E	Dairy	19	75	80	36– 60	3.9– 5.6	3.8–5.2
F	Steel mill	18	1,500	95	264–1,116	3.3– 7.9	1.6–4.0
G	Steel mill	17	500		156– 216	7.8–11.5	3.3–5.5
H	Steel mill	17	10,000	99	1,800–4,200	1.6– 4.8	1.2–2.5
I	Penitentiary	15	1,000	100	176– 408	2.9–32.9	1.7–3.5
J	Construction	14	300	81	40– 207	2.6–13.8	1.6–4.3
K	Paper manufacturer	12	450	92	199– 300	2.5– 6.6	2.0–2.5
L	Hosiery mill	12	50	88	42– 47	2.8– 3.0	1.0–1.4
M	Pipe line	3	600		332– 440	2.4– 4.5	1.4–2.0

this discussion shows the actual range of percentages of monthly loss in terms of load in kilowatt-hours and in terms of kilowatts at the time of the monthly 30-minute maximum demand. In 13 installations, monthly per cent losses in kilowatt-hours varied from 1.6 per cent to 32.9 per cent, at the time of maximum demand from 0.8 per cent to 5.8 per cent. Large variations are general not only between customers but for the same customer in different billing periods. The table includes *all* compensating meter installations made during the first three years of its use; hence the results must be considered typical.

If we take the example shown in the paper the lowest possible percentage of losses is about 1.9, the "highest probable" 2.5 per cent, but this assumes unity-power-factor load. If we assume a power factor of 70 per cent, the highest probable loss becomes 3.6 per cent, without even considering that the transformer bank will probably remain in service even though the monthly kilowatt-hours drop to 50,000 or less, resulting of course in a still greater percentage of losses.

It must be concluded that the losses are sufficiently important to be measured. Throughout the paper the conception of their magnitude as related to load is definitely low. The service test results show much higher percentages of losses in terms of load than those assumed. It follows that method 1, in view of the variations shown in Table 1, is impractical for general application. Losses cannot be predicted sufficiently closely, nor is it practical to change a customer's rate every billing period.

Method 6, the use of the meter adjustments for including the losses, is limited in application to locations where the load conditions are known definitely. If the meter rating and the load exceed the point of maximum efficiency of the load transformers, namely about half load in modern transformers, appreciable errors in registration may result. A characteristic curve for this method of adjustment is shown in Figure 1 of this discussion. In this figure the meter rating is 75 per cent of the transformer rating. The error is the difference between the curves of desired and actual registration. Both are shown for unity and 50 per cent lagging power factor. Note that the meters read high between the light-load and full-load test points. This could be improved by reducing the meter rating, but only at the expense of poorer performance at higher loads. Since customer's load conditions cannot be predicted closely (Table 1), this method should be confined to installations where the load conditions and the meter rating are within the limitations of the method.

Method 2, using compensating meters, has been in use for the last ten years. These meters, after calibration for an installation, give the loss kilowatt-hours and the loss demand directly without the need for voltage and power-factor information throughout the billing period. Their application, testing, and use is a matter of routine on the Philadelphia Electric Company system. There has been no difficulty in obtaining transformer-loss data. The manufacturers make loss tests on each transformer of about 100 kilovoltamperes and above, while for smaller sizes test data on representative specimens from production are on file. There is thus no need for "estimating" transformer losses. The compensating meter in the form where the total loss is given by one meter is the most desirable not only for the simplification of billing but also to facilitate the measurement of the loss demand. Where vars and var hours of the loss are required for billing, a separate compensating meter can be calibrated to read these quantities directly. This is an alternative method to that pointed out in the paper in which the compensating meters are in the form of separate volt-square and ampere-square-hour meters with different multipliers for watts and vars. The latter method introduces complications in the measurement of the loss demand.

The line-drop compensator method of including copper loss is sound in principle and, when combined with the use of either the light-load adjustment or the dummy-load method of including core loss, can be made practical for routine metering installations on customer's premises. While technically there is no advantage in one method over the other, there is an advantage in keeping the adjustments of a billing watt hour meter entirely standard. From this point of view the dummy-load method of compensation is preferred. Tests of both methods in comparison with the regular billing metering show close agreement and a degree of accuracy well within the performance that can be maintained on primary meters. Tests on five customers over a period of 13 weeks showed a comparative registration of 100.08 per cent for the dummy-load method, and 100.15 per cent for the method using the light-load adjustment for including core loss. Both methods, therefore, are well within the requirements of the public utility commission of any state and hence are entirely acceptable for billing purposes.

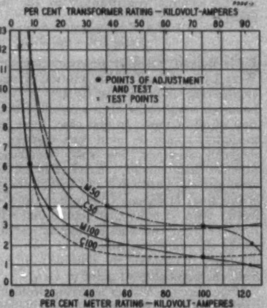

Figure 1. Performance of a 230-volt 800-ampere meter adjusted to include losses of a 250-kva power transformer

C 100—Desired registration, 100 per cent power factor

C 50—Desired registration, 50 per cent power factor

M 100—Meter registration, 100 per cent power factor

M 50—Meter registration, 50 per cent power factor

In the line-drop compensator method, it is practical to use two line-drop compensators on three-phase *three-wire* circuits with either star- or delta-connected transformer banks when two-element meters are used. This is true because the degree of unbalance that exists on customer's installations is insufficient to warrant the use of the third compensator shown in Figures 3 and 10. This has been established in connection with compensating metering (reference 2 of the paper). The comparative tests discussed in the preceding paragraph were made with two line-drop compensators on each installation.

The advantages of metering on the low-voltage side, with an acceptable method of compensating for the transformer and conductor losses, are not confined merely to the first cost of the installation. These methods result also in a simplification of the high-voltage structure; and, in territory subject to lightning disturbances, failures of the vulnerable current and potential transformers on the high-voltage side of the power transformers are eliminated by connecting the metering on the load side of the power transformers. Thus service interruptions are reduced to a minimum, which is particularly important in supplying war loads.

C. V. Morey (Consolidated Edison Company of New York, Inc., New York, N. Y.): A review of available methods of metering high-voltage service on the low-voltage side of the power transformers is particularly valuable at this time. The subject has been well covered, including, as it does, methods of billing for power-transformer losses by estimate as well as by the installation of special equipment. I could wish only that the authors had stressed even more strongly the case for estimating rather than metering transformer losses.

In any discussion of methods of metering on the low-voltage side of a power transformer, it is generally assumed that primary metering should be taken as the base from which to calculate possible errors. It must be kept in mind, however, that even under relatively good operating conditions the maximum probable error in standard primary metering is at least ±0.5 per cent so that the base of reference does not represent absolute precision measurement. As is pointed out under method 6 in the paper, the measurement of core loss by primary metering during periods of no load on the power transformer is possibly less accurate than would be any one of several of the other methods described. In fact it can be stated that for such periods of no load, if their duration can be closely approximated, no method of metering primary or secondary, will be any more accurate than the estimate, for all practical purposes.

With the limiting conditions of method 1, estimation of losses, as set up by the authors, it may be seen that the probable maximum error is less than the probable maximum error to be expected in primary metering. The authors have taken as a typical example a case where a bank of transformers is operating at about 35 per cent load factor. By taking the average between the two load conditions which would provide minimum and maximum losses, they show that the maximum error in the estimation of the losses cannot be greater than 0.32 per cent even with no knowledge of loading conditions. They arrived at these figures by assuming the power transformer to have 1.5 per cent combined core and copper loss at rated voltage and current. Any assumed increase in load factor or decrease in percentage losses would have lowered the maximum possible error.

We recently have installed meters on the low side of power transformers on a relatively large installation. Although we are not estimating losses in this case, I have calculated it on the basis as shown in this paper to determine maximum error if the losses were estimated. The core loss in this case is about 0.22 per cent at rated voltage and load and the copper losses about 0.48 per cent at rated current, making the total losses about 0.7 per cent. Using the 35 per cent load factor assumed by the authors, the limiting losses would range from about 0.8 per cent to about 1.1 per cent of the registered energy and the maximum possible error would thus be only about ±0.15 per cent.

These figures apply, of course, only to unity-power-factor loads, as is the case with the authors' example. If an average power factor of say 80 per cent is assumed, the maximum possible error would increase to about ±0.2 per cent. For power-factor variation within ±10 per cent of the assumed figure, the possible error would further increase to about 0.4 per cent. With even this latter figure, however, a very strong case is made for estimating rather than attempting to meter losses.

There are many factors in the rate setting and in the selection of the point at which the metering is to be done that outweigh the small additional errors that may be introduced by estimating rather than metering transformer losses. I therefore find it difficult to justify the use of special equipment of any kind for the purpose. This is particularly true in view of the fact that the per cent losses on large power transformers operating at normal load points are comparable to and in some cases actually less than the error tolerances to be expected in primary metering.

There is an additional combination of methods for including the effect of transformer losses in secondary measurement which is not mentioned in the paper. It is a combination of method 4 with a part of method 6 in that a dummy load is used to correct for core loss, the copper loss being taken care of by altering the shape of the load curve of the watt-hour meter.

It is rather difficult to alter the shape of the load curve of the meter to take care of both core and copper losses but fairly simple for copper loss only, since it is not necessary to disturb the normal curve of the meter to any extent at light load, which is the meter's critical point of operation.

We have three such installations in service at the present time. A formula has been set up for the meter test in such a way that, knowing the percentage core and copper losses, and the load points at which the meter is to be adjusted, the calibration may be made in the normal way by simply adding a calculated correction to the revolutions of the rotating standard test meter to include transformer losses. For the particular installations involved no improvement in over-all accuracy of operation could be obtained with primary metering.

Walter C. Wagner (lieutenant commander, U. S. Navy, Washington, D. C.): The authors have prepared a very excellent summary of the generally known methods of using secondary metering for the measurement of primary energy. Their accuracy-load estimates are fairly representative for the conditions outlined, and their comparison between primary metering and metering the primary energy on the low-voltage side is very conservatively presented. In the field applications of some of these methods, it has been found that even better performances are assured under many service metering conditions. The authors have perhaps wisely avoided the very light load (under 10 per cent load) relative characteristics, and have not presented the relative characteristics of operation at power factors generally found in metering power customers. Comparative test and service data curves must be given serious consideration if such metering is to meet the requirements for metering accuracy demanded by customers or even for the measurements of energy distribution in a power company's system.

The writer of this discussion has, over the years, developed a method which makes use of such principles as described by the authors, but, by a different method of influencing the watt-hour-meter elements, has succeeded in the laboratory and in practice, in obtaining greatly improved accuracies over those indicated by the authors, for both 100 per cent power factor and 50 per cent power factor over a range of meter capacity from 5 per cent to 150 per cent, with a marked starting current improvement.

In these days of conservation of critical materials, the writer joins the authors in an endeavor to interest the electrical industry in this definite contribution to the war effort.

E. J. Boland (General Electric Company, West Lynn, Mass.): The solution of most engineering problems is usually a well-balanced compromise after a careful evaluation of all factors. In this case, there immediately comes to mind such matters as metering accuracy, first cost, installation expense, maintenance, relative simplicity of arrangement, possibility and magnitude of errors from erroneous connections, and so forth. Also, in order that contractual relations between two parties may be carried out to the mutual satisfaction of both, it is necessary to evaluate the possible solutions from the psychological point of view.

If we were to consider metering problems strictly on the basis of engineering considerations, it is difficult to come to any other conclusion than the practicability of estimating core and copper losses. The over-all accuracy of metering high-voltage energy on the low side and estimating the power-transformer losses would not appear to be out of line with the accuracy with which the cost of service can be determined. But it is improbable if methods involving estimations will be satisfactory to both parties concerned. The logic of percentages is not always impressive when a considerable revenue is involved. That is one practical reason for high-voltage metering, and for the substitute method of metering on the low side and measuring power-transformer losses by means of so-called transformer-loss meters. It probably also explains why reactive transformer-loss meters are used even on schedules where the unit value of kilovar demands is only a fraction of that applicable to kilowatt demands.

It should be pointed out also that transformer-loss meters have practical advantages over the use of line-drop compensators and dummy loads. Metering connections are generally simpler. The measurement of the power load is by a standard (and separate) watt-hour meter. Therefore, should there be erroneous connections in the loss-measuring equipment they will have no effect on the registration of the much more important power load. And there is also some advantage in being able to explain in simple terms to the purchaser that the power meter registers all loads behind it and the loss meter measures the losses from the service entrance to the power meter.

Over the last ten years, or so, a considerable number of these transformer-loss meters have been installed. This does not necessarily indicate a more general acceptance of such meters than in the years before. There has always been an incentive to circumvent the cost and installation expense of high-voltage instrument transformers. If transformer-loss meters have been used more in the past few years, one reason may be that there have been proportionately more applications for them. When high-voltage instrument transformers are required for relaying and protection and when there is a relatively small difference in voltage rating between the primary and the secondary of the power transformers, it is logical to employ high-voltage metering. Therefore, the use of loss-meters is generally limited to metering of high-voltage power consumers, which is another way of saying that there must be schedules in effect for such consumers before there will be requirements for schemes and meters to measure losses.

Transformer-loss meters, or equivalent methods, will not necessarily be used for all high-voltage power consumers. Disregarding entirely the limitations of the meters and methods and, as well, difficulties from tap changing (automatic load-radio control), from power flow in and out, and so forth, there is the practical consideration of testing and servicing of such special meters. A large utility can train personnel to take care of such meters and may be able to justify providing special test standards by means of which testing becomes relatively simple. Contrastingly, the utility with only one or two applications for transformer-loss meters must consider carefully the disadvantages of test and maintenance of special meters against the advantage of reduced first cost.

For the duration, another factor must be considered, namely the use of critical materials. Low-voltage instrument transformers use less steel and copper than high-voltage transformers. These savings, however, are offset in part by additional man-hours in production, installation, test and maintenance of transformer-loss meters, and man power is also a scarce item.

Finally, it should not be overlooked that with the recent advent of new small-sized and light-weight current and potential transformers the installation and maintenance of high-voltage instrument transformers has become materially simplified.

J. A. Morris, O. K. Coleman (American Gas and Electric Service Corporation, New York, N. Y.): The article by Petzinger and Lenehan is a valuable contribution to the knowledge of metering and seems especially valuable at this time when every effort is being made to conserve vital materials such as steel and copper, the major components of high-voltage metering equipment.

The technical subject matter appears to be quite complete and will undoubtedly prove to be of value to those who are primarily interested in the subject of measurement of electrical quantities. Further study and discussion would probably be of value in further development of high-voltage metering on the low-voltage side, to the point where it would be as generally acceptable and understood as the more commonly used methods of primary metering.

The paper being considered requires little or no discussion of its merits from a technical standpoint but would permit perhaps a discussion of some of the practical aspects of the seven methods of including the transformer losses in a secondary measurement.

COMPENSATING METERS AND NETWORK

The greatest application of the so-called "compensating metering" methods would probably be on customers' metering where the charges are based on the energy input to the high-voltage side of the transformer. On the other hand, "compensating metering" would probably have a minimum of application on station metering, as the greater part of such metering is usually done on the low-voltage side and high-voltage instrument transformers, essential for other purposes, are available for metering, if necessary.

The general acceptance of "compensating metering" will probably depend to some extent on the development of more or less standard, flexible, compensating devices, having circuit constants that will cover certain ranges of transformer characteristics and thus avoid the need of "homemade" networks.

The meters used for measuring losses are at the present time considered as special apparatus and require for their manufacture, material and man-hours that undoubtedly can be used more advantageously elsewhere. Thus the development of a more or less standard line of equipment for the purpose of loss measurements seems to be a subject for the future.

Compensating networks, in addition to being difficult to construct and adjust, tend to complicate a standardized system of meter connections and might, in some cases, through misapplication, introduce errors of measurement of greater magnitude than the small quantity to be measured.

CALCULATING TRANSFORMER LOSSES

Fortunately, other means, suggested by this paper, are available to accomplish the equivalent of direct measurement of transformer losses, namely calculation, and suitable adjustment of the watt-hour meter. While these methods are possibly not as accurate as the direct-measurement method under conditions of variable voltage, frequency, and transformer loading, they have certain advantages worthy of consideration, but, as pointed out in the paper, each case should be studied to determine the best method for the existing conditions.

Probably the major advantages of these two schemes lie in the fact that no additional equipment is necessary to accomplish the desired measurement, thus eliminating complicated connections and the use of vital materials.

The one objection to the use of calculations that may be worthy of consideration is the possible customer reaction to change after years of experience with only measured quantities in the purchase of power. There should, however, be no difficulty in overcoming possible objections if the method is sound and the matter is properly presented.

A minor disadvantage of calculating the losses is the necessity of making computations for every billing period. While these computations are not especially difficult, they might become cumbersome to handle if a large number of installations were billed by this method.

ADJUSTMENT OF STANDARD METERS FOR LOSSES

Compensation for losses by adjustment of the watt-hour meter to include the registration of the transformer losses appears to offer a fairly simple solution to the problem, eliminating as it does the use of special equipment and calculations. On the other hand the scheme requires the keeping of records of calculation and calibration constants which must be referred to each time the meter is tested. The objection may also be raised by some that the meter must be adjusted to measure apparently at some accuracy other than one hundred per cent. It should be pointed out that in some cases meters are required to be adjusted similarly to compensate for instrument-transformer errors. However, the accuracy of modern instrument transformers are such that special meter adjustments are generally unnecessary.

REACTIVE-VOLT-AMPERE MEASUREMENT

The discussions presented in the foregoing may, to some extent, be applied to the measurement or calculation for determining the primary reactive kilovolt-amperes from secondary metered quantities. As pointed out in the paper under discussion, the accuracy of measurement or calculation for varhours is probably not as great as similar measurements of watt-hours, but since this component usually reflects in the final billing to only a minor extent slight errors in its measurement would probably have an inappreciable effect on the final result.

It should be pointed out in connection with determining primary vars or varhours, that the standard varhour meter cannot be adjusted readily to include the transformer loss similar to the method described for watt-hour meters. Therefore it will be necessary to do one of several things, neglect var losses, calculate, or measure by one of several methods. Probably the simplest method of measurement, where the watt loss is also to be measured by the simple method of adjusting the standard watt-hour meter, would be with a standard varhour meter, having an I^2X element added, and a dummy load for excitation loss.

CONCLUSION

It seems quite evident, after a study of the methods and the advantages and disadvantages of each, that as the authors pointed out, each application of means of compensating for transformer losses should be studied individually to determine the best method, and for the present no universal method can be said to apply.

While a rule of thumb cannot be made at this time, it would obviously be desirable, in view of the situation, to avoid in so far as possible not only the use of high-voltage metering equipment but the use of special compensating meters.

L. D. Price (Public Service Electric and Gas Company, Newark, N. J.): This paper presents an excellent analysis of the available methods for the determination of transformer losses to compensate secondary metering in cases where it becomes expedient to utilize secondary instead of primary metering. However, both the introductory paragraph and the summary at the conclusion of the paper convey the impression that such metering is desirable under all circumstances. Such is not the case, and I should like to present some general comments on the limitations of application of this method of metering.

The first consideration in application requires an analysis of the power system and the relative sizes of customers' loads normally supplied at transmission or subtransmission voltages. It may be stated that, in general, the economic advantage of secondary metering increases rapidly as the voltage of the transmission system is increased. If a rather large number of customers with relatively small loads are tapped from transmission lines and are supplied with one transformer or one bank of transformers where a single meter installation on either primary or secondary side is applicable, an obvious first cost economic advantage obtains where low-voltage metering is applied. However, with increased demands by customers served at these voltages and with a corresponding decrease in the number of such customers, variations in design of customers' substations necessitate more complicated metering installations that often result in a reversal of the initial economic advantage. For example, a customer may have several transformer banks with different secondary voltages such as four kilovolts for plant distribution and 480 volts for immediate use at, or near, the service location; or a customer may distribute transmission voltage supplying unit substations located in various factory buildings with one transformer bank located at the service; or, a customer may have several banks of transformers supplied from a bus at transmission voltage. In these or similar cases, the unnecessary complication of totalization metering coupled with compensated metering should generally be avoided. A very important consideration in the application of low-voltage metering in such cases involves the design of proper metering to take care of circulating currents and reverse flow of power under load switching conditions that are not under the control of the supply company. If metering on the low-voltage side of the customer's installation is insisted upon, much of the desirable flexibility of design of the customer's substation may have to be sacrificed.

The authors mention the difficulties encountered in direct primary circuit metering. It seems to me that simplification is best accomplished by direct measurement of energy and demand as it is billed to the customer. In any event, instrument transformers will be required for metering whether on the primary or on the secondary side, and it seems obvious that an increase in potential transformer ratio and a corresponding decrease in current transformer ratio to meter service at transmission voltages will not complicate the metering system. On the other hand, the addition of voltage squared elements, current squared elements, line drop compensators, dummy loads, or the special calibration necessary for meter compensation applied to secondary meters simplifies neither the metering system nor the problem of explaining the functions of such devices to customers.

In addition to the consideration of investment cost and problems concerning metering equipment and installation methods, maintenance of secondary, compensated, metering equipment with the attendant special equipment and maintenance methods must receive careful consideration. Increased maintenance costs and increased investment in special testing equipment should not be overshadowed by initial investment savings. Furthermore, the handling of these special accounts out of routine for the bookkeeping and billing departments is an important factor.

In general, the application of one or more of these methods of compensation of secondary metering applied to primary billing involves careful analysis of the supply and utilization systems. Some of the problems involved in this analysis have been outlined in this discussion. It is not my intention to condemn this method of metering but rather to point out that it is not a universally applicable "money-saver."

H. L. Prescott (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): The paper by A. J. Petzinger and B. E. Lenehan presents a valuable contribution to the art of metering primary power, as it permits using low-voltage instrument transformers to measure power in the high-voltage circuit.

Many power transformers on which secondary metering might be desirable are supplied with tap changing under load equipment. This equipment is often for the purpose of maintaining constant secondary voltage regardless of changes in the primary voltage, although it may maintain constant voltage at a point removed from the transformer. There are many types of equipment which may be used for this purpose, but three will serve to illustrate the conditions.

1. The load tap changer may be connected in the high-voltage winding. This, in effect, changes the number of turns in the high-voltage winding so as to maintain constant induction in the iron. For this case, the core loss remains constant but the load loss varies not only with the square of the load but also with the changes in high-voltage resistance as the tap changer position changes.

2. The load tap changer may be connected in the low-voltage winding. This, in effect, changes the number of turns in the low-voltage winding in inverse ratio to the changes in induction caused by changes in the primary voltage applied to the transformer. In this case, the transformer core loss varies with the applied voltage, but the tap changer action prevents these changes being registered on the secondary side. Also the resistance of the low-voltage winding changes with the tap changer position so that the load loss does not vary exactly as the square of the current.

3. A series transformer may be used to add or subtract a voltage equal to the variation of the transformer voltage from the desired constant value so as to maintain constant output voltage. Here, again, the tap changer action prevents the voltage changes from affecting secondary meters although it does affect the loss in the transformer. Also the tap changer position determines the core loss in the series transformer, and the effective resistance varies with the tap changer position.

These cases do not follow the same easily approximated curves as a transformer without tap changing under load, and it therefore appears that secondary metering cannot be accomplished quite so simply as the paper describes. However, the writer of this discussion has studied this problem in relation to this paper and has found that in many cases a reasonably accurate approximation may be made by a slight variation of the methods described in the paper.

The variations involve estimating not only the loading to be expected, as is described in the two paragraphs preceding method 1, but also estimating the length of time which the tap changer will operate on its different positions and the loads which will be carried when the tap changer is on these positions. Generally this estimate would be sufficiently close by considering only the neutral position, the maximum buck position, and the maximum boost positions of the load tap changer. The calculations are then exactly the same as described in the paper.

While the results obtained by the modified method would in many cases be slightly less accurate than applying the methods as described to a transformer without load tap changer, it nevertheless permits an extension of the methods to a larger proportion of transformer installations. Just as is concluded in the paper, the best of the seven methods for a particular installation must be determined by a study of the individual circumstances.

A. E. Knowlton (*Electrical World*, New York, N. Y.): A much needed summary is provided in this paper on means of augmenting the load registration to such degree as to cover the losses which, by the contractual relations, are to be charged against the customer. Although the paper does not say so, there are instances where the customer is to absorb the losses in all or part of the high-voltage line as well as the losses in the step-down transformer. The line copper losses fluctuate with the load current along the same pattern as the copper losses within the transformer. The line losses therefore can be lumped with those in the transformer copper, and all of the methods of the paper are applicable to the combined value. Since the line losses may be several times the transformer losses, the deviation percentages cited in the paper change to such a degree of uncertainty with a varying load as to weaken trust in the estimating procedure and place the faith in one of the instrumental methods detailed in the paper.

Reverting to the province chosen by the authors (losses only in the transformer bank), comment can be made in particular about the psychological factors. Psychology is an element of the regulatory arena in which power metering falls. In making a decision on the form of loss metering (or estimating), an answer should be given to the question, "which procedure offers the most convincing setup and the least opportunity for differences of opinion?" This question would seem to place the estimating method in disfavor and elevate the various metering methods of ascertaining the losses which should be added to the bill. But it should not be overlooked that several of the methods, once applied, obliterate the chance to register the true load consumption independently of the losses; if argument arises about the loss adjustment, there can be just as much argument about the customer's actual consumption. In so far as there is merit in this consideration, the separate loss meter offers a less controversial potentiality than a line-drop compensator, dummy loads or a watt hour meter recalibrated to embrace the transformer losses. No matter how much it may be trusted or mistrusted or misunderstood, there is still the conventional and undisturbed watt hour meter totaling the delivered load consumption. Ninety to 98 per cent of the financial transaction would thus be in the realm of lay acceptance and of lay understanding involving the addition of the registrations of load meter and loss meter.

That, as I see it, is a likely reaction and attitude of regulatory authorities on the matter of metering and billing for delivery losses.

B. E. Lenehan: The variations in opinion expressed in the discussion add weight to the belief that there is no "best" method for every installation. The additional data supplied should help further to determine what method should be used, including, as Mr. Price suggests, primary metering and ignoring the losses in addition to the methods in the paper. The estimating methods are particularly useful in studies of this kind.

I believe Mr. Schleicher's data were taken around 1933 and represent business conditions at that time. The errors, of course, vary with conditions. The results given in the paper and by Mr. Schleicher for meter curve adjustment are typical when curves are fitted at the conventional test points of 10 and 100 per cent. Better results could be obtained by a different adjustment, but the effort required may not be worthwhile, since the remaining error is small and will be positive or negative at different loads, resulting in a very small over all registration error. Usually, it will be found desirable to correct for instrument transformer errors at the same time.

Factors Involved in the Selection of Railroad Motive Power

Discussion and author's closure of paper 43-49 by H. C. Wilcox and A. G. Oehler, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, May section, pages 235–40.

Norman Litchfield (Gibbs and Hill, Inc., New York, N. Y.): The authors have brought together some interesting data, among which are prices of the three types of locomotive considered, as follows:

Diesel locomotives	$87.50 per engine horsepower
Steam locomotives	35.00 per cylinder horsepower
Electric locomotives	65.00 per continuous horsepower

These prices are misleading because the bases of Diesel-engine horsepower, steam-engine-cylinder horsepower and electric continuous horsepower are not comparable. A more proper basis is drawbar horsepower, which works out roughly for main line passenger locomotives at 60 miles per hour to:

Diesel locomotives	$113.00 per drawbar horsepower
Steam locomotives	40.00 per drawbar horsepower
Electric locomotives	69.00 per drawbar horsepower

Furthermore, if the service is such as to require a maximum speed of 80 miles per hour or over with relatively frequent slowdowns, then a more proper comparison is on the basis of maximum drawbar horsepower available, and the prices become:

Diesel locomotives	$113.00 per maximum drawbar horsepower available
Steam locomotives	40.00 per maximum drawbar horsepower available
Electric locomotives	57.00 per maximum drawbar horsepower available

This emphasizes the fact that no set of figures can be set up which are generally applicable, which the authors recognize in their suggestion that a study be made of a specific case. Such an investigation, however, to be of any real value, would be laborious and expensive and when done would only apply to one set of conditions.

So far as electrification is concerned, it has long been recognized that the traffic must be very dense to produce operating savings sufficient to carry the fixed charges. Incidentally, the latter often cover improved types of signaling and communication and an increased over all speed.

The Diesel locomotive provides some of the features of electrification and has the obvious attraction that it can be introduced a few units at a time without the large initial capital expenditure required for electrification. But so far it has been used only in main line operation on certain runs which favor it and is supplemented by steam power for the balance of the service. The published costs of operation are based on the high mileage resulting from these special runs and do not represent costs that may be expected in an over all operation.

The problem is one which does not permit a general solution but must be considered separately for each set of operating conditions.

W. S. H. Hamilton (New York Central System, New York, N. Y.): The authors have done a good piece of work in bringing together in one place a discussion of the factors involved in this often difficult problem and in pointing out methods of evaluating them. However, I wish to point out that when it comes to the actual determination of this problem in any specific case, it is necessary to consider carefully all the conditions surrounding that particular case, before a definite conclusion can be reached. While certain general factors hold more or less true in nearly all cases, local conditions in different cases are likely to change completely the conclusions to be reached even though superficially the cases may seem to be similar. We have made three major studies of the application of Diesel-electric switching locomotives on the New York Central System in recent years. Each case proved to be a study by itself, and the factors that were determining in one location were not determining in the others. This feature should always be kept in mind in making these studies, and it should not be assumed that, since a result is secured in one location, the same result will be secured in another, even though off hand the cases might seem to be very similar.

For this reason I doubt that the suggestion made by the authors toward the end of the paper that such studies be placed in the hands of a joint committee would lead to results sufficiently worth while to justify it. Such committees are usually not inclined to make the detailed studies necessary to evaluate properly all the local conditions. Rather I think this problem should be handled jointly by representatives of the operating and motive power departments of the railroad involved. Papers such as this are helpful in calling attention to the general factors involved, but it will be necessary for the railroad representatives to give proper consideration to the local problems of each case.

There are a number of other points on which I wish to comment.

I question the statement that the Diesel-electric locomotive was developed from the "rail car." There were gasoline-electric locomotives long before the Diesel engine entered the field, and these were separate and distinct from what is ordinarily known as a "rail car." While it is true that the first Diesel-electric locomotives did have box-type cabs, that does not make them "rail cars."

In connection with the possibilities of operating one class of service with one type of motive power and another class with a different type, and so forth, this method of operation should be kept in mind and explored in considering specific cases. It is closely allied with the determination as to whether a given type of motive power is to handle the base load or the total load, which makes a great deal of difference in the total investment required. We have found in Diesel switching locomotive studies, that in general, jobs working less than 18 eight-hour tricks a week can be handled more economically by steam-switching locomotives than by Diesel-electric, but in a few instances because of special circumstances, utilization of slightly less than this amount has been found economical.

However, in considering any method of operation involving mixed motive power, it should be kept in mind that operating a number of different types of motive power at the same point requires maintenance facilities for each of the different types. This in turn eliminates the large saving that usually can be made by the complete elimination of facilities required for steam locomotives at any given location. The boiler washing facilities required for steam locomotives represent a considerable investment and operating expense, but, unless such facilities can be completely eliminated, the final result may be simply adding one type of facility on top of the other.

In connection with Table I of the paper, it would be interesting to have a valuation of the relative merits of the various items of advantage and disadvantage.

I do not feel that item 4*c* belongs in Table I at all, as all three types of motive power have definite merchandising appeal if properly handled. The interest of large groups of railroad "fans" in steam locomotives of all kinds is sufficient argument to indicate that there is plenty of merchandising appeal in the steam locomotive.

Comment is made that the electric transmission permits the Diesel engine to operate near its full load rating at all locomotive speeds. Not all types of electric transmission and control permit a close adherence to the full load rating of the Diesel engine at all speeds, and this point should be kept in mind in comparing locomotives. This same comment applies to the reference to overload capacity near the end of the paper.

It seems to me that it is not necessary or desirable to have three separate depreciation rates and that two are sufficient, one to cover the engine and generator and the other the remainder of the locomotive. Enough experience has been accumulated to indicate that the electric transmission (excluding the generator) and the mechanical portion of the locomotive will have a probable life of about 30 years. The Diesel engine and generator may have as long a physical life as the transmission and mechanical portion, but it may become economical to replace the Diesel engine by a new one before the expiration of this time, not because the engine is physically worn out, but because it is out of production and the repair parts become expensive. Also, the development of the Diesel engine has been so rapid up to now and probably will be for the next few years that a period of about 15 years brings major changes in engine design that can well be taken advantage of by the purchase of new engines instead of maintaining the old ones. Our experience with a number of Diesel engines that are now about 12 years old indicates this trend. It should be borne in mind in this connection that more parts have to be renewed in the course of maintenance of Diesel engines than in the case of electric motors and generators. The only reason for including the generator with the Diesel engine in the above statement is that if the engine is replaced the same generator probably could not be used.

It is felt therefore that basing the depreciation on a life of approximately 15 years for the Diesel engine and generator and approximately 30 years for the balance of the equipment is reasonable.

The possibility of obsolescence of the locomotive as a whole should be kept in mind in considering depreciation rates. Changes in operating conditions may make the locomotive of little value in a shorter time than those lives set forth above, and this point should at least be given consideration.

In connection with the remarks under the head of "Flexibility," while the idea of having small locomotives for small trains and large locomotives for larger trains is superficially attractive, as well as the possibility of operating locomotives in multiple unit to achieve the same result, at the same time practical operating experience usually has shown that it is more economical, particularly in road service, to have all the locomotives alike and capable of handling the maximum train. It is more economical in the long run to operate a large locomotive occasionally on a small train than to try to separate out locomotives to suit the trains encountered, or make them up in multiple-combinations to suit the same conditions.

In connection with comparisons of the performance or characteristics of different types of motive power such as in Figure 1, there is a great tendency to compare locomotives of somewhat different horsepower output at a given speed and to attempt to discredit the characteristics of one because of differences found at this speed. In order to compare any road motive power fairly, they should develop the same drawbar horsepower at the speed that is to be given primary consideration. In other words if they have to haul trains at 70 miles per hour the drawbar horsepower at 70 miles per hour should be the same before making comparisons. It is often difficult to do this because existing locomotives as built do not always have the same rating at a given speed, but this point should be kept constantly in mind.

As a matter of record in connection with the items of Appendix A, depreciation is classified by the Interstate Commerce Commission as an operating cost and should really be included in connection with the items under 4. For convenience it is frequently included with the items of fixed charges, as is done in this paper

In connection with "Locomotive Utilization," the distinction between availability and utilization must be kept constantly in mind. While Diesel-electric locomotives can have a very high availability, it may not be possible always to utilize them as much of the time as they are available. This again requires a careful study of each case and the error should never be made of dividing the total number of switching hours by the availability, and calling the result the number of locomotives required. Unfortunately for those of us in the locomotive-design field, the shipping requirements of industries often require switching service at hours which do not permit the most economical utilization of locomotives.

In connection with road locomotive studies this problem is complicated by the difficulty of finding trains that run at the proper hours to permit maximum utilization, and the problem of assigning locomotives to trains in such a manner that a reasonable adherence to schedule can be maintained even if one train is late, which does occur even on the best regulated railroads.

In connection with the subject of locomotive utilization, consideration should be given to the question of whether the existing locomotives are being utilized to the best possible advantage before those of a new type are considered. It is frequently found that a substantial portion of the savings claimed for a new type of motive power can be made by rearranging the existing operation without introducing a new type at all.

Sidney Withington (New York, New Haven and Hartford Railroad Company, New Haven, Conn.): Table I of the paper indicates that fuel at less than $4.00 a ton is a "disadvantage" in electric motive power as compared with steam. The cost of electric power is dependent on the load factor of the supply, and, while it perhaps may be true that with a low load factor the cost of power would be high, it is also true that where the load factor is high the cost of power is often very definitely an *advantage* for electric locomotives, even considering the recent advance in steam locomotive thermal efficiency, especially in instances where water power may be economically available, as is the case in certain parts of this country. It may be that in the future in figuring carefully costs of power production the power companies may find it possible to compute prices so that there would be no question of power economy in electrification.

The figure quoted in referring to an average cost of coal at $2.68 delivered to the railroads seems low. It would be of interest to know whence the figure was derived. The 15 cents per ton for over all handling to the locomotive tenders also seems low, especially if fixed charges of the handling machinery are included.

The reference to "complete failures" as being disadvantageous to electric equipment is a little unfair. It would be of interest to compare operating records of steam and electric power. This would in many instances show considerable advantage to electric operation.

The fuel figures of 33 cents per freight locomotive mile for steam locomotives and 28 cents for Diesel, is interesting, but it would be of more value if the trailing tonnage were given, as the magnitude, of course, depends on the trailing tonnage, irrespective of the type of power. If 15 per cent differential is added to the fuel cost of the Diesel for lubricants, the costs would be nearly identical. The figure for electric power then might be computed for comparison. It is suggested that in presenting comparative data the authors endeavor to include information on all three types of power under discussion.

Mention is made of "dynamic augment." It is true that a great deal has been done to improve steam locomotive design in this respect with new alloy steels, and so forth, but it should be pointed out that this problem is still serious at the high speeds demanded in modern transport and is still a definite "disadvantage" of importance in steam equipment.

Regarding popular appeal, it must be acknowledged that with air conditioning of coaches and hermetically sealed windows, the average train rider has no idea whether the motive power is steam, electric, or Diesel, and in some respects the "merchandising appeal" is in favor of steam.

Dynamic braking is most helpful and produces a saving of power, but it is pointed out that both in this country and abroad the roads which use it are all situated in territory where water power is plentiful or fuel cheap, so the energy saved is relatively of little value. Many roads abroad use single-phase dynamic braking with the regenerated power absorbed in resistance.

The rates of $35, $87.50, and $65 per horsepower for Diesel, steam, and electric locomotives well might have been correlated to compare *horsepower at the drawbar*. If such a figure were employed, the overload horsepower capacity might be under some circumstances a logical figure to use. If this were to be done, the electric locomotive figure would be (4,800/9,000)×65, or $40. In any event, *drawbar* horsepower should be shown, not "engine" or "cylinder" horsepower.

Under certain conditions the maximum *short-period horsepower* would be a legitimate figure to use for an electric locomotive in comparing with steam and Diesel data, where the continuous horsepower figure is virtually the maximum also, as is pointed out in the paper. This overload capacity of the electric is available at start or for acceleration after slow-down and for operation on relatively short grades. This advantage is, of course, of value only in special cases, but should be taken into consideration in any analysis.

The item of *obsolescence* is the important factor in depreciation rather than *wear and tear*, and thus the locomotive should be considered as a unit and not combination of units. It is quite possible the gas turbine, or some other form of power, may make the Diesel, as well as other types of locomotives, obsolete over night just as the steam turbine, forty years ago, suddenly drove the reciprocating engine out of the picture.

The physical availability of a locomotive may have nothing to do with the actual availability as pointed out in the paper, and any advantage in this respect may be completely valueless.

The flexibility or "packaging" by multiple operation of a number of small units is very pleasant in theory but may not work out in practice and may result in considerable dead-head or light mileage movements. Most railroad business is one-way business with cars moving empty in the other direction. It is usually better to buy standard size locomotives and then schedule the operation if possible so the locomotives are efficiently loaded. As is indicated in the paper, the duplication of numerous accessories in multiple cabs, each capable of independent operation is relatively expensive, both in first cost and in operation. Figure 1 implies an advantage to Diesel equipment by multiple-cab operation and it is to be noted that electric-locomotive users could adopt this feature if they cared to incur the expense. In this connection it might be pointed out that the tender of the steam locomotive (unless equipped with a booster) is but a useless weight and reduces revenue trailing capacity.

It seems a little harsh in a review such as that under discussion that transportation engineers should be accused of a "common error" in making but halfway study of the problem of motive power. It may be pointed out that a combination of different types of locomotives is often undoubtedly necessary, especially in periods of transition, but it should be borne in mind that unified types of motive power have definite advantages over mixed types from many viewpoints, especially in terminals and shops. Many maintenance and operating problems are of course much simplified if all motive power is of the same type.

The Institute is to be congratulated in sponsoring a paper so obviously nonprejudiced and one which adds so much to the literature on this important subject.

Table I. Fuel and Power Consumption, Values, and Costs With Steam, Electric, and Diesel Motive Power

From the Association of American Railways Bureau of Railway Economics, Year 1941

	Steam Locomotive	Electric Locomotive	Diesel-Electric Locomotive
Switching Service			
Consumption	766 lbs coal per hr	121 kwhr per hr	5.8 gals fuel oil per hr
Cost per hour[1]	$1.149	$1.210	$0.348
Road Passenger Service			
Consumption	16.8 lbs coal per car-mile	3.5 kwhr per car-mile	0.26 gals of fuel oil per car-mile
Cost per car-mile[1]	$0.0252	$0.0350	$0.0156
Cost per train-mile of ten cars	$0.252	$0.350	$0.156
Road Freight Service			
Consumption	113 lbs coal per 1,000 gtm	27.45 kwhr per 1,000 gtm	2.00[2] gals fuel oil per 1,000 gtm
Cost per 1,000 gross ton-miles[1]	$0.170	$0.2745	$0.120

1. Coal cost assumed three dollars per ton. Electric power cost including distribution and transmission charges assumed ten mills per kilowatt-hour. Diesel fuel cost assumed six cents per gallon.

2. With only limited use of Diesel freight power in 1941 no values for that year are available. Diesel fuel consumption is found to vary between $1^{1}/_{2}$ gallons and $2^{1}/_{2}$ gallons per 1,000 gross ton-miles. An average of two gallons is assumed.

P. A. McGee (General Motors Corporation, New York, N. Y.): In a final analysis the selection of the proper motive power is a matter of an economic study which generally can be developed for each type of power from existing standard railroad accounts, the cost per switching hour, the cost per passenger car-mile or passenger train-mile and the cost per 1,000 gross ton-miles of freight for the three types of services considered by the authors.

It is of course necessary to qualify these costs by considering the nature and amount of work performed for the unit costs employed.

In the case of switching service, it is necessary to distinguish between passenger- and freight-car switching, together with the possibility of reducing switching hours and overtime with a particular type of power.

In the case of passenger service it will be necessary to consider the types of trains, schedules required, and the maximum speeds permissible together with helper watering and fueling services required, and the possibility of lengthening locomotive divisions, and, of course, the number of locomotives required.

In the case of freight trains, consideration must be given to the 1,000 gross ton-miles per locomotive hour, the maximum speed permitted with helper and grade conditions, the possibilities of lengthening locomotive divisions, and the number of locomotives required.

In effect this is the procedure recommended by the authors in their Appendix A. It is a practical method since the three types of power considered are in extensive use under various operating conditions. In the case where a railroad is considering a type of power with which it has had no previous experience, the unit costs can be obtained from railroads, having comparable conditions, employing the type of power being considered.

The required study by the transportation engineers mentioned by the author then should be largely the work of the accounting department and operating officers with a minimum of estimating guesses and wishful thinking.

Under the heading of "Selection of Locomotive Characteristics" the authors, having considered the requirements, state:

> "The steam and electric locomotive selected may be one which has previously been built, or a modification thereof, while the Diesel is the nearest commercial size (in production) which fulfills the requirements."

I draw your attention to this pertinent statement, as it is quite significant that the steam locomotive with over 100 years' development and the electric locomotive with over 40 years' development are still subject to constant modifications, while the Diesel locomotive of recent origin is now pretty well accepted as basically standard in design.

The modifications to the steam locomotive are generally brought about by the enlarging of clearances and strengthening of track structures which permit locomotive heights exceeding 16 feet 6 inches and individual axle loads up to 80,000 pounds. The expense of such modifications is obvious and can be justified on only very limited lengths of track with the result that locomotives designed to these limits are restricted in their operation with generally low potential availability.

The electric-locomotive modifications have even more restrictions in their use than the steam-locomotive. Most of the electric locomotives in use today are designed for and are confined to a very limited length of line which may be very level or heavy grade track. The possibilities for long through runs with maximum use for electric locomotives would appear to be quite remote.

Both the steam and electric locomotives today are featuring their potentially high power output. In many instances the train size required to develop the claimed high horsepower output on level track could not be satisfactorily accelerated or operated for any extended distance on even a one half of one per cent grade. With electric traction it is claimed that a small locomotive with about 50,000-pound continuous tractive effort can develop more horsepower than a good size city would require normally for its industrial requirements.

Our transportation engineer must recognize that the horsepower output of a locomotive is the product of pull and speed. Steam and electric locomotives are strong on speed and short on pull when developing their maximum horsepower.

In a close analysis of a particular railroad problem the modifications and exceptional horsepower outputs can be evaluated easily enough. It is when considering the problem in its general aspect that confusion and misunderstandings are likely to arise when modifications and unusual horsepower outputs are overstressed in evaluating the advantages of the three types of power.

This possible misunderstanding I think is well illustrated by Figure 1 of the paper referred to by the authors under the heading of "Overload Capacity." I would like to ask the authors how many steam locomotives of the characteristics shown in Figure 1 are in operation and what adaptability has this locomotive with regard to clearances and axle loads to practically all class 1 railroads in the United States? I would like to know how many electric locomotives with the characteristics shown are in use today and how the adaptability of this particular locomotive compares with other electrified railroads for freight haulage in the United States?

Answers to these questions will probably explain my point.

There are, however, factors associated with the locomotive characteristics that are not indicated on the curves and should be explained.

The three locomotives are presumed to have the same approximate horsepower output at around 40 miles per hour and although not definitely stated the implication is that the three locomotives are comparable in performance. This is certainly a gross misrepresentation.

In the first instance, although not shown on the curve, the Diesel locomotive has a starting tractive force of 214,000 pounds and a continuous tractive force of 120,000 pounds. It delivers its full rating at as low a speed as 10 to 12 miles per hour.

In other words, the Diesel locomotive has

3$^1/_2$ times the starting tractive effort of the steam locomotive, about 2$^1/_2$ times the starting tractive effort of the electric locomotive, and over 2$^1/_2$ times the continuous tractive effort of the electric locomotive. Furthermore, the electric locomotive has to obtain a speed of 60 miles per hour to develop the 4,800 horsepower nominal rating and must run at 40 miles per hour to develop over 4,000 horsepower. Note this condition with a locomotive having a maximum speed of 70 miles per hour.

The curve for the electric locomotive shows a short-time tractive effort rating of 25 per cent adhesion up to 40 miles per hour which is a very optimistic assumption. At the short-time rating the locomotive develops 9,000 horsepower which means that the power plant to which the system is connected may develop about 18,000 horsepower. A locomotive with a continuous rating of 50,000 pounds tractive force could never justify such a demand on the average United States railroad. The authors must recognize that, if such power demands are necessary for the economic development of electric traction, they have understated the cost of electric locomotives if they must be supplemented with power plants of this size which cost from $100 to possibly $300 perhorsepower.

General comparisons on a train-mile basis are seldom satisfactory without complete qualifying particulars of train size and speed. In freight service all such comparisons should be on a 1,000-gross-ton-mile basis.

Fortunately the question of fuel and power consumption costs for locomotives have been covered pretty well by the Association of American Railways Bureau of Railway Economics. Tables giving these values were published in *Railway Age* in April, 1942.

These tables cover switching service, road passenger service, and road freight service. For convenience I have taken the average values for the whole United States and tabulated them in Table I of this discussion for the three types of services considered. It might be well to explain briefly the conditions for the different types of services.

In switching service the steam locomotive includes all switching service which comprises both heavy and light types of steam locomotives. With electric traction, the switching service is primarily that of small 80-ton locomotives in passenger switching. With Diesel operation the switching service comprises both 600-horsepower and 1,000-horsepower locomotives which on the average are probably pretty well representative of the railways as a whole. It might be mentioned that in passenger service we would normally expect the 600-horsepower Diesel locomotive to consume not more than three gallons of fuel oil per hour.

We assumed for steam operation an average coal cost of three dollars per ton. With the electric service the fuel cost including the distribution and transmission charges would be between 10 mills and 12 mills per kilowatt-hour. We have assumed the lower value on the attached table. With Diesel locomotives we have assumed fuel cost of six cents per gallon. All the above costs are employed in both switching passenger and freight services.

The road passenger data are taken direct from the table published in *Railway Age*. It should be noted in this case that there is no value given for the fuel oil consumed with the electric locomotives which fuel oil consumption is of the order of 30 per cent of that required with the Diesel locomotive for five months of the year.

In freight service the table published in *Railway Age*, which refers to the years 1941 and 1940, does not give values for Diesel freight locomotives which were limited in their use during these years. Diesel freight fuel consumptions will vary from 1$^1/_2$ to 2$^1/_2$ gallons a thousand gross ton-miles. For the purpose of comparison we have used an average value of two gallons of Diesel oil. In this connection it may be noted that there is, of course, quite a little variation in the power consumed in freight service on the various districts.

From the savings shown with Diesel power under the various services it can be seen what large fuel economies can be obtained with the 45,000,000 switching hours per year, the 300,000,000 to 400,000,000 passenger train-miles per year and the 1,600,000,000 thousand gross freight ton-miles per year on our class 1 railroads.

J. W. Teker (General Electric Company, Erie, Pa.): J. C. Aydelott points out three factors—high-speed operation, acceleration, and locomotive weight, in the comparison of motive power which does not affect the broad outline of the paper, but the following will serve to enlarge upon and qualify these items to form a better distinction between the types.

Where high operating speed is a consideration, fundamentally the conventional reciprocating steam engine is limited more definitely with respect to maximum operating speed than the other two types with rotary drive. While steam power is built for high-speed service, there is always the problem of dynamic balance and its effect upon reciprocating parts and upon the track structure.

When acceleration is considered from the standpoint of weight on drivers only, then both the electric and Diesel-electric enjoy an advantage over steam, but when considered fully, the horsepower input to a train determines acceleration. In this respect the electric alone excels the others by virtue of its horsepower reserve, while the Diesel-electric by virtue of the electric drive exceeds steam only up to the speed where the steam can work at its full horsepower, say 25 miles per hour on Characteristic Curve, Figure 1 of the paper.

From the operational standpoint, like switching, the electric and Diesel-electric outperform steam because the speed is too low for good steam locomotive performance; but in main line operation, where acceleration is required after slowdowns, this is no longer true. Only the electric has the advantage, since both Diesel and steam are limited to their fixed horsepower output and are on a par with one another.

In operation on main line grades where speed is dragged down, the Diesel-electric will again begin to show slight advantage over steam but not to the extent that the electric does with its ability to apply reserve horsepower into the train.

Train performance is determined by the total train weight per horsepower when considering acceleration and operation on grades. The weight of the locomotive becomes an important factor as it contributes to total train weight and must be subtracted to obtain the weight of the revenue train. The trolley electric locomotive again is superior in this respect.

An additional factor commonly overlooked is the space occupied by a locomotive in its train. Beyond a certain horsepower the conventional Diesel-electric exceeds the steam and electric in length which immediately becomes a consideration in yard facilities, terminal station trackage, and sidings on the main line.

Paul Lebenbaum (Southern Pacific Company, San Francisco, Calif.): I have read the paper with a great deal of interest and believe it will fill a gap that has been existing for a long time in the field of electrification literature. There is one point, however, to which I wish to take exception.

Table I of the paper is an analysis of motive power characteristics, in which the authors indicate that one of the advantages of the electric locomotive is its flexibility.

In times past—in fact, running back to 1904—this office has made a considerable number of studies bearing on the electrification of two of its mountain grades. One of these, from Roseville, California, to Sparks, Nevada (a distance of 138 miles), reaches an elevation, eastbound, of 6,880 feet, and the other, from Bakersfield to Los Angeles, California (a distance of 166 miles), reaches an elevation of 4,025 feet. Maximum grades on both of these lines are 2.64 per cent for a considerable distance. These districts constitute a link in two transcontinental routes.

Traffic over the Roseville-Sparks grade fluctuates widely during the various seasons of the year, and the peak movement, during September and October of each year, constitutes 33$^1/_3$ per cent of the total ton-miles moved during the entire year. Even during this two-month period, the traffic peaks to a maximum in about the last week in September and the first week in October. Electric locomotives would have to be available to handle this maximum peak, and a large number of these locomotives would lie idle for the remainder of the year.

Under steam operation, we can draw upon steam power from other parts of the system, for handling the aforementioned peak, because peaks on the various divisions of the system do not coincide. For instance, when Imperial Valley products, like lettuce and cantaloupes, are being harvested in the early part of the year, traffic on other parts of the system is at a low. Similarly, over the Tehachapi grade, which is in the district mentioned above as lying between Bakersfield and Los Angeles, the traffic peak does not coincide with the peak over the Sierras between Roseville and Sparks.

To sum up, as far as the Southern Pacific Company (Pacific Lines) is concerned, the peak movements are handled by the shifting of steam locomotives from one district to another, whereas, if the mountain grades were electrified, the maximum of electrical equipment would have to be provided to handle the peaks, and a great part of this maximum would lie idle during a large part of the year.

R. T. Sawyer (American Locomotive Company New York, N. Y.): The last paragraph of this paper has offered a suggestion that a committee be formed consisting of

> "three groups of recognized transportation engineers experienced in the application of steam, electric, and Diesel-electric motive power. Each group could then proceed to determine the extent that each type should be used for a given case. When put together the three reports would indicate to what extent definite values can be given to the many factors involved."

This suggestion of a further study of this subject is desirable. The actual problem of motive-power application should be placed in the hands of three groups of transportation engineers with the idea of developing additional data of a specific nature to supplement the general treatment of the subject in this paper.

Some of the members who participated in oral discussion ventured the suggestion that such a study is hardly worth while for the reason that it is not possible to treat, in a general way, a subject that is, of necessity, specific in character and, further, that the solution of a specific application, such as contemplated, would be of little value in the approach to the broad problem.

Such conclusions as these naturally raise a question as to the possibility of placing in the record, for the benefit of the railroad industry the basic data with respect to the characteristics of existing motive-power types *under present conditions*. As Mr. Litchfield of Gibbs and Hill has pointed out, changes in both conditions and motive power take place, and the characteristics of types of ten years ago or ten years hence may differ widely. Such being the case the application of the basic data of today to a specific problem of railroad operation may, and undoubtedly will, produce an entirely different result than the application of similar data of, say, ten years ago. To me, this is one of the main reasons why a further study should be made at this time, as, broadly speaking, the steam locomotive, the Diesel or the electric, should not necessarily be universally used wherever road locomotive power is required.

My interpretation of the suggestion which has been made by the authors of this paper is *not* that the application of basic data to a specific operating problem will produce such a result that may be accepted with finality, but rather that the effort to produce such a result will at least necessitate the recording of comparative data which in itself can be of great value regardless of the results derived from its application.

The authors have at least provided the skeleton upon which such a study may be built, and I concur in the suggestion that the chairmen of the Oil-Gas Power and Railroad Divisions of the American Society of Mechanical Engineers be approached with the idea of learning how the membership of those groups would react to the suggestion that a committee be formed for the purpose of investigating the potential value of further study along these lines.

L. K. Sillcox (New York Air Brake Company, Watertown, N. Y.): The authors are to be commended for the preparation of a paper which may be utilized as a guide in a systematic and impartial evaluation of motive-power types in specific instances. Because of the complete manner with which the authors have treated their subject, it is difficult to suggest additions or revisions with improvement as the objective but I should deem typical original and maintenance costs of the power distribution and contact systems essential to electrification might well be included, with due allowance for facilities for the servicing of steam locomotives which electric locomotives do not require.

Another question which has occurred to the writer is relative to the depreciation of Diesel-electric power. Reference is made to the fact that different rates should apply to the Diesel engine, the electrical equipment, and the mechanical equipment. The established or estimated service lives of the three components would be of interest, as would those of the service lives of the electrical and mechanical equipments of electric locomotives. In this same connection, the authors at one point state that various railways write off Diesel-electric locomotives at annual rates varying from four to ten per cent. At another point the statement is made that the Interstate Commerce Commission fixes the depreciation rates applicable to each of the motive-power types. These two statements could well be explained for the benefit of those unfamiliar with railway accounting procedures.

The writer of this discussion feels that a fourth account, taxes, should be added to the Fixed Charges item of Table I. That the author agrees is indicated by the inclusion of taxes in the discussion of the Fixed Charges account in the text of the paper.

The author states that one railway obtains but 12 hours' service daily from its steam freight locomotives, eight of the remaining 12 being required for servicing and repairs while four hours are consumed awaiting assignment. This statement may be expanded to include steam passenger locomotives as well, notwithstanding some remarkable extended run performances. Oil-burning steam locomotives are regularly assigned by this railway to runs up to eighteen hundred miles, requiring 50 hours of continuous operation, in passenger service, yet the effect of limited runs demanded by traffic conditions reduces the system average to approximately 12 hours per day. Even in freight service, steam locomotives—again oil-burning—operate continuous mileages of approximately six hundred as a maximum.

In conclusion, the writer wishes to emphasize that, although much is written advancing the points of merit of the competing motive-power types by their proponents, in the final analysis selection must be made upon sound economic principles requiring close study of the conditions surrounding individual instances, and a paper such as the one under discussion will serve as an excellent guide to those responsible for an impartial decision based upon all factors involved.

A. G. Oehler: The authors, in commenting on depreciation rates applicable to locomotives, did not intend to offer any suggestion as to proper depreciation rates for Diesel-electric locomotives. Cognizance was taken however, of the fact that some motive-power engineers believe that different rates of depreciation might well apply, separately, to the electrical equipment, mechanical equipment, and the Diesel engine. In arriving at the decision as to the factor of depreciation being an advantage or a disadvantage, with respect to each type of motive power (as indicated in Table I of the paper), the authors used an arbitrary service life of 30 years for both electrical and mechanical equipment and 15 years for the Diesel engine. The discussion by Mr. Hamilton (New York Central) throws some additional light on this particular phase of the depreciation problem.

Without carrying this discussion of depreciation to great lengths, it may be said, in general, that depreciation rates are *allowed* by the Interstate Commerce Commission, usually after actual experience in the application of such rates has indicated their adequacy. When mention of rates from four to ten per cent was made in the paper, the statement can well be corrected, for purposes of clarity, to the extent of indicating that such rates have been reported by individual roads, in specific equipment cases, to the ICC under Order 15,100. Inasmuch as experience under this order has the objective of exploring straight-line depreciation possibilities versus retirement accounting methods, these rates (some exceeding even the ten per cent figure) are not established rates but are merely tentative rates, either applied for or accepted in the specific case.

It has been suggested by Mr. Withington, electrical engineer (New York, New Haven and Hartford) that the cost of $2.68 for coal, as given in the paper, is low. This is the cost of coal consumed, including freight and handling costs, for class 1 railroads in the United States, as compiled by the Interstate Commerce Commission for the first nine months of 1942. The cost of coal to the New Haven is, of course, considerably higher than this. The 15 cents per ton for over all handling to the locomotive tenders, as given in the paper, is a commonly used figure which is considered adequate for handling coal to the locomotive tenders but which may not include charges for hauling coal on the railroad where it is used.

P. Lebenbaum, electrical engineer (Southern Pacific), outlines conditions on his railroad which would necessitate having a sufficient number of electric locomotives on an electrified zone to handle peak traffic. This would require a relatively large investment in electric locomotives which would not be necessary where peak traffic might be handled by steam locomotives over an electrified section.

P. A. McGee (General Motors Corp., New York, N. Y.) has asked how many steam locomotives there are having characteristics such as those shown in Figure 1. These characteristics are those of a locomotive built by the Pennsylvania and are included to show potentialities rather than averages. The electric locomotive characteristics shown are those for the latest type of New Haven electric locomotives which are described in the September 12, 1942, issue of *Railway Age*, page 402. Five of these locomotives are in service and five on order. A Pennsylvania type *GG*-1 electric locomotive with 65-mile gears would have similar characteristics.

The Power-Recovery System of Testing Aircraft Engines

Discussion and authors' closure of paper 43-41 by G. E. Cassidy, W. A. Mosteller, and W. L. Wright, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, volume 62, 1943, May section, pages 240–6.

E. F. Dissmeyer (Commonwealth and Southern Corporation, Jackson, Mich.): The authors have stated in the presentation of their paper that, by using the power-recovery system of testing aircraft engines, the amount of critical materials used is only one-sixth of that required to supply an equal amount of power from power-supply companies. It would be of interest to know how many manufacturers of aircraft engines depend upon the testing of engines as a firm source of power. Is it now true that the power-supply company must have adequate facilities to take care of the entire manufacturing load, since there may be times when manufacturing of engines must be continued in spite of the fact that none are being tested?

J. N. Mahoney (Wright Aeronautical Corporation, Paterson, N. J.): We agree with the authors of the paper that for "production testing" an electric-power-recovery system of testing aircraft engines is desirable and is reasonably obtainable where but one size of engine is being manufactured, a situation which is generally the case in plants 2 and 3 outlined in the paper. Our company has been interested in such power recovery for at least ten years, to the writer's knowledge, and engineering studies and test layouts have been made of several schemes in conjunction with the manufacturers mentioned in the paper and some others. The company originally had a number of d-c electrical dynamometers used in connection with the smaller engines, and demonstrations were made with these d-c dynamometers on returning test-recovery electric power through motor-generator sets to the general power-distribution system of the manufacturing plant.

These d-c electric dynamometers were originally intended for use with engines of approximately 350 horsepower and in later years were supplemented with hydraulic dynamometers to absorb the rapidly increasing engine horsepower caused by the practically continuous development carried on in the industry. In this combination the electrical dynamometer practically became a vernier for the combined dynamometer system, because the hydraulic unit was necessarily of several times the capacity of the electrical unit. The problems of our organization through the years include the continued development and manufacture of engines of higher speeds and horsepowers for direct propeller driving and various gear ratios on these different engines for different diameters and types of propellers, including the coaxial twin-propeller drive, and drives for other than aircraft purposes. Our plants in the Paterson, N. J., district at the present time are manufacturing engines in a wide variety of sizes, with each of the sizes equipped for one or more of these various drives incorporated as a part of the design.

It will be noted from the paper that "power-recovery developments since the first power-recovery installation" have been made in connection with aircraft-engine plants erected by the automobile industry which at present, at least, are limited to the manufacture of but one size of engine; and the power-recovery systems used are limited to a rather moderate range of speeds and powers related to the particular size of engine. Our investigation of these installations and systems indicates that they are quite well adapted to the particular applications but do not have the power and speed range to meet the continuing changes in power and speed requirements of engines both in development and manufacture—which is our problem at present. We have had in mind for some time an electric generating unit, other than the combination of d-c generator and a-c motor-generator-set, which is adapted to generating a-c power at the usual system frequency, when driven at speeds that may vary in a ratio of from approximately 10 to 200 per cent of normal frequency.

Such an equipment would necessarily be designed to absorb the torque of the large slow-speed propeller drives, and the rotor should be capable of operating at the highest direct speeds at which any contemplated engine design will operate. The conditions in the electrical and airplane-engine industries during the past several years, which included both the defense and war programs, have prevented the proper development of such an electric-power-recovery unit in time for our large production program, and we have had to carry on with the type of propeller test stand as shown in Figure 10 of the paper to meet the extreme requirements of both our development testing and production testing.

G. E. Cassidy, W. A. Mosteller, and **W. L. Wright:** In reference to Mr. Dissmeyer's questions, it is not necessary to have a power-supply company connection equal to the entire manufacturing load, provided power-recovery testing is used extensively as the scheme of testing. Power-recovery testing as a firm source of manufacturing power supply is directly associated with the number of test stands installed. A few stands do not have sufficient diversity of operation to be depended upon for power. On the contrary, the diversity accompanying a large number of stands will result in a power supply that is entirely satisfactory. Figure 9 of the paper applies to a manufacturing plant using a large number of stands. The data shown are typical of what may be expected. The data, coupled with the fact that engine manufacture and consequently plant load, would hardly be at a high rate without accompanying testing operation, point to the conclusion that a power-supply company connection appreciably below the maximum manufacturing load would be entirely adequate.

In regard to Mr. Mahoney's discussion, it was not the authors' intent to leave the impression that a test stand of the power-recovery type is unduly limited as a means of testing a range of engine sizes. The authors made the statement that the larger the engine the more attractive power-recovery testing becomes. This is so, primarily because the kilowatt-hours of power recovered from large engine testing, say engines of 750 horsepower or 1,000 horsepower and above, is greater than from small engine testing, and because large engine testing is more difficult and uneconomical by wastage schemes than is the testing of small engines.

The general statement may be made that power-recovery testing belongs with large engines and that, for an average installation, economics will dictate that the range in engine sizes to be tested be limited to the larger engines. In other words, a power-recovery testing stand designed to test a single rating of large engine will most likely be equally applicable to the one rating if it is designed to test a range of large engines.

The authors feel that Mr. Dissmeyer's and Mr. Mahoney's comments are timely and pertinent and directly to the point of the whole paper—namely, to bring to the fore an engineering development that still presents opportunity for important development, improvement, and expansion.

Synchronous Motors With Controlled Excitation for Suddenly Applied Loads

Discussion and authors' closure of paper 43-31 by W. K. Boice, B. H. Caldwell, and M. N. Halberg, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, March section, pages 113–19.

A. F. Ordas (General Electric Company, Fort Wayne, Ind.): Controlled excitation offers the possibility of a constant amount of corrective kilovolt-amperes from a synchronous motor supplying a rapidly fluctuating load. For motors supplying equal constant torque peaks during their duty cycle, all that is needed is a "working" excitation of one value and an "idle" excitation of another value. The control for such a duty cycle may be quite simple.

Controlled excitation has the advantage that reduced machine losses during the idle part of the duty cycle means:

1. Improved efficiency.
2. Smaller frame size.
3. A reduction of the idle time of the cycle if the same frame size is used.

In other words, for a given frame size one can run more cycles per hour when controlled excitation is used.

The paper shows the importance of making an analysis for transient pull-out torque when sudden loads are to be applied. Many synchronous motors have been built to meet steady-state pull-out guarantees although they are used to drive suddenly applied loads.

Use of controlled excitation will save material for national-defense purposes by reducing the necessary motor size. The frame will be the larger of the two determined by

1. Rms kilovolt-amperes of the duty cycle.
2. The frame size necessary to give the transient reactance to meet the required transient pull-out torque.

Calculations indicate controlled excitation will allow high-speed unity power-factor motors to match the pull-out torque of the larger 0.8 power-factor motor of the same horsepower rating and improve on the 0.8 power-factor cycle efficiency.

Since the transient pull-out torque is largely a function of the transient reactance, reduction in frame size may be limited by the maximum transient reactance allowable. When the motor is to drive peak loads with low root mean square kilovolt-amperes, the frame size can be reduced until the transient reactance and transient pull-out torque are just within the guarantee requirements. Smaller frames necessarily have a higher minimum transient reactance than larger frames. Here controlled excitation will help by permitting a higher core loss, thus permitting use of higher flux density and, consequently, making possible a lower transient reactance than would be obtainable ordinarily with a given frame. Of course, the higher flux also means the starting kilovolt-amperes approaches that of the larger frame.

The over-all cost of a controlled excitation equipment will probably be greater than for a standard 0.8 power-factor motor at ratings below 250 horsepower.

S. B. Crary (General Electric Company, Schenectady, N. Y.): The use of controlled excitation for suddenly applied synchronous-motor loads is a logical development. Regulators are being used more widely for control of generator excitation for essentially the same reasons as outlined in this paper for motors; that is, reduction in size of the rotating electric apparatus, reduction in losses, and improved system performance. The results of the investigation for motors also agree with that for generators, in that standard methods may be used and high rates of response are not necessary. The ability of the machine, generator, or motor to withstand transient changes is determined chiefly by its transient reactance.

Power-system as well as industrial engineers should find real encouragement in this paper, in that the regulator and its control can be justified for ratings even below 5,000 horsepower, based on benefits which accrue to the industrial-plant operator directly. The power system benefits by obtaining a reduction in real and reactive-kilovolt-ampere system losses and improved load and system stability.

In the design of motors using regulated excitation, care should be taken to provide a margin above that apparently required for the shaft loads alone, to allow for supply-system impedance and system disturbances. It would constitute a disadvantage for this type of control if too great an economy of materials were attempted at a sacrifice of adequate pull-out margin.

W. K. Boice, B. H. Caldwell, M. N. Halberg: The authors agree with Mr. Crary that adequate margin in pull-out torque should not be sacrificed in the pursuit of maximum economy of materials.

Mr. Ordas has correctly summarized and explained the factors which determine the maximum saving in materials that can be realized through controlled excitation.

The paper has emphasized the saving of critical materials, as this is a particularly important advantage in the present emergency. Normally, however, it is felt that this advantage will be of secondary importance, and improvement in operating characteristics will be the principal justification for the use of controlled excitation.

The investigation program outlined in the paper is now being extended by the accumulation of actual field experience. During 1940 a controlled-excitation system was furnished with a 4,900-horsepower motor driving a d-c generator, which supplies power to a d-c reversing mill motor. The peak-load requirement was 250 per cent, which may be suddenly applied. Operation has been entirely successful. Construction is now under way on a 5,600-horsepower motor for a similar application where controlled excitation will be used.

Paired-Phase Bus Bars for Large Polyphase Currents

Discussion and author's closure of paper 43-17 by Lawrence E. Fisher and Robert L. Frank, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, volume 62, 1943, February section, pages 71–7.

L. L. Fountain and **C. P. West** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): 1. The authors have described a novel arrangement of paired-phase bus bars and have pointed out their advantages. Low losses and good regulation are demonstrated by test. The statement is made: "Furthermore, the dimensions that produce these desirable electrical and thermal characteristics lend themselves to convenient structural designs wherein the problems of joining adjacent bus-bar lengths, making tap-offs, support against mechanical and short-circuit forces and insulation are greatly simplified." We can hardly agree that the structural problems or making of tap-offs are simplified as compared to normal bus design. However, other advantages may compensate for increased difficulties in these respects. Paired-phase arrangements appear to be advantageous for transmission of power for long distances at low voltage.

2. The basis of the paired-phase performance is the close spacing between the phases of the pair which complicates the insulation between phases, particularly where taps or joints must be made. It is thus evident that the proposed arrangement is not well suited for bus designs in metal-enclosed switchgear. The mechanical conditions here are very different, and an edge-to-edge arrangement of bars and frequent insulated tap-offs would require much more space than present conventional designs.

3. The curves shown in Figures 4, 5, and 6 of the paper are plotted with voltage drop, against an abscissa of "over-all width" in inches occupied by the bus copper. It seems this method of comparison of the paired phase, laminated, and interlaced busses is not a fair one.

4. In the case of the laminated bus, the distance d in Figure 2 between phases is made larger than need be if insulation is used as in the paired-phase arrangement. If this distance were made the same as the distance between the edges of the copper bars of opposite phases, as shown in Figure 1 (vertical arrangement), then a much lower voltage drop and less unbalance in voltage would be realized.

5. In the case of the interlaced bus, the spacing d, Figure 2, between bars, should be made the same as s in Figure 1 for fair comparison. A separation into two groups of three bars each rather than with uniform spacing as shown in Figure 2 of the paper, so that the space between groups would give an over-all width, the same as occupied by the paired-phase arrangement, would, we feel, result in an equivalent or even lower voltage drop than shown for the type-3 paired-phase arrangement. Also, it appears the current would more nearly approach a 50 per cent division in each phase bar, than the 58 per cent required to be carried by each phase bar in the paired-phase arrangement. This in itself would result in less heating in the copper for the interlaced compared with the paired-phase arrangement.

6. Referring to Figure 4 of the paper, one can make a more accurate comparison of the relative performances of the several types of bus arrangements as they have been discussed, by comparing the values in the lower portion of the curves for laminated and interlaced busses with those curves for the paired-phase arrangement.

7. Curves are shown (Figure 17) giving the temperature rise for various sizes of bars in a six-bar paired-phase bus. It would be interesting to know what commercial rating is given to a typical bar size such as one-fourth by four inches in a housing such as illustrated in Figure 15. What temperature rise over outside air would be expected? Do commercial ratings conform to the standards of the National Electrical Manufacturers Association and AIEE?

Lawrence E. Fisher: With respect to the discussion of L. L. Fountain and C. P. West, it should be emphasized that we do not advocate the paired-phase bus-bar arrangement for all bus-bar applications. The paired-phase paper primarily intended consideration of paired-phase bus-bar arrangements for feeder distribution systems for industrial plants and similar applications. The data in the paper were presented in a manner to relate the paired-phase performance to performance of other common types of bus-bar distribution-system arrangements used for similar applications. The curves of the different types of arrangements were shown on the same drawing in a manner to permit any desired comparison. The over-all width w was chosen as the most appropriate common dimension of comparison, because the dimensions d and s of the different types of arrangements may be varied within a common given over-all width to obtain different performance characteristics.

The following comments are numbered to correspond with the numbers in the discussion:

1. Experience with feeder distribution systems of the low-impedance type, requiring close spacing of opposite-phase bus bars, indicates that the paired-phase arrangement provides advantages of easy joining between sections in forming long runs. The flat surfaces of the bus bars are exposed as shown in Figure 15 of the paper making it very easy when installing to clean thoroughly and prepare the contact surfaces before the bus bars are bolted together. Moreover since the wide flat surfaces of all six bars are exposed and thus readily accessible, it is easy to connect tap-off straps to the bus bars.

2. Paired-phase arrangements are not specifically advocated for enclosed switchgear in which the bus runs are short and where low-impedance performance is not important.

3. The curves shown in Figures 4, 5, 6, and 9 in the paired-phase paper are plotted from actual test data for the laminated arrangement, the interlaced arrangement, and the paired-phase arrangement, using the same bus bars for a rather wide variation of dimensions. As stated in the text, comparisons may be made in any way desired. It is not intended that comparisons be made only for the same over-all width.

4. It is apparent from the curve of Figure 4 that, as the pairs in the laminated arrangement are brought closer together, the voltage drop becomes lower, but it remains considerably unbalanced. If the *d* dimension in the laminated arrangement is reduced to the same value as the *s* dimension, namely $^5/_8$ inch, making the over-all width $3^1/_8$ inches, the voltage drop remains considerably greater in magnitude and more unbalanced than that of the interlaced arrangement or paired-phase arrangement.

5. If the spacing *d* in the interlaced arrangement were made the same as the spacing *s* in the paired-phase arrangement, the performance would be as indicated in Figures 4, 5, 6, and 9 for an over-all width of $3^1/_8$ inches. If these arrangements were separated into two groups of three bars each, so that the space between the groups would give an over-all width, the same as occupied by the paired-phase arrangement, it would not be considered an interlaced arrangement.

Such an arrangement was not discussed in the paired-phase paper, but tests conducted on this type of bus-bar arrangement, using six one-fourth by four-inch bus bars, indicate that the line-to-line impedance drops are severely unbalanced. The maximum line-to-line impedance drop is almost 75 per cent greater than the minimum line-to-line impedance drop. This assumes that the two groups of three bus bars are tied together in parallel in the arrangement *ABC . . . . ABC*. It is, of course, possible to connect these two groups in parallel in such a way that the bar having the lowest impedance in one group will be connected in parallel with the bar in the other group having the highest impedance. This would of course cause a very unequal division of current between the two bars in parallel. In the one leg having the most unequal division of current, the current in the one bar would be almost 75 per cent greater than the current in the other bar. In addition to the unequal division of current between the two bars in parallel, the a-c to d-c resistance ratio in each individual bar would be relatively high.

6. As stated previously, comparisons can be made as desired from the data presented.

7. It has been common practice to rate bus ducts (bus-bar feeder distribution systems) at 600 volts or less on the basis of 1,000 amperes per square inch of conductor cross section rather than on a temperature-rise basis. This practice is influenced by the National Electrical Code and the Underwriters' Laboratories. The paired-phase bus duct with six one-fourth-by-four-inch bus bars (Figure 15) would be rated accordingly at 2,000 amperes. The hot-spot temperature rise for this or other current values can be determined from Figure 17.

Switching Overvoltage Hazard Eliminated in High-Voltage Oil Circuit Breakers

Discussion and authors' closure of paper 43-19 by Lloyd F. Hunt, E. W. Boehne, and H. A. Peterson, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, volume 62, 1943, February section, pages 98–106.

Eric T. B. Gross (Cornell University, Ithaca, N. Y.): For the particular case of a high-voltage single-circuit transmission system, it has been known for some time past[1] that interrupting the line-charging current in a ground-fault-neutralizer system is more difficult from a switching standpoint than in the same system with the neutral solidly grounded. For the last 14 years or so modern-type lightning arresters have been used in Europe and other foreign countries to by-pass the ground-fault neutralizer at the end of a single-circuit line, in order to avoid the increase in overvoltages due to the resonant grounding of the neutral. In many cases such arresters are also used at the isolated transformer neutral in the station terminating a single-circuit line, even if a resonant coil is not installed in the station. The damping resistance of modern arresters is so small that there is practically no difference between the ground-fault-neutralizer system and the system with the neutral directly grounded, as far as surges are concerned; whether or not the coil is tuned is of no consequence. As previously mentioned,[2] this method is by far the simplest and cheapest.

In a number of similar cases in 120-kv single-circuit systems in Austria and other European countries, all the flashover and restriking troubles disappeared after the installation of the arresters at the transformer neutral. In one 66-kv system, the early type of lightning arrester with nonlinear damping resistance built in 1929 did not prove sufficient, and the flashovers which it should suppress disappeared only after two such arresters in parallel had been installed. However, in all later cases and particularly in the systems with higher voltages, one arrester of the newer construction[3] gave good results. This shows clearly how important it is to use the right kind of arrester or resistance as by-pass.

In the case of the Southern California line, it seems as if the Thyrite by-pass inside the tank of the coil[4] were too high and therefore not effective enough. It would seem advisable to use regular lightning arresters instead of the internal by-pass which is necessarily of comparatively high resistance. This discussion refers only to that part of the transient overvoltages due to switching which is induced by the resonant neutral grounding. The method mentioned will reduce the overvoltages to harmless magnitudes. It should hardly be questioned that the well-developed breakers in use in this country are unable to switch off the capacitive load of high-voltage lines.

Circuit breakers with internal resistances were generally used in Europe for long high-voltage lines and for transformers until about 15 to 20 years ago.[5] For anyone who is somewhat familiar with European practice, and who knows about the struggle to get rid of the internal-resistance breaker and of the tendency to follow American practice of long successful standing, it seems like a step backward now to introduce such a breaker here if there is more than a chance that another way of suppressing dangerous overvoltages seems possible.

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5. Discussion of PROLONGED INRUSH CURRENTS WITH PARALLEL TRANSFORMERS AFFECT DIFFERENTIAL RELAYING. AIEE TRANSACTIONS, volume 60, 1941, page 1306.

J. B. MacNeill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper is of marked interest, because it covers the interruption of charging currents on an ungrounded 230,000-volt line and deals with phenomena which cannot be duplicated completely in modern high-power laboratories.

Resistors in shunt to circuit interrupters have been used in the past for four general purposes:

(*a*). Where several interrupters are used per pole unit, shunt resistors across each interrupter will serve to divide the recovery voltage between the interrupters and thus reduce the likelihood of arc re-establishment after it has once been interrupted. For this purpose, resistors of large ohmic value from 100,000 to 1,000,000 ohms per unit are usable.

(*b*). On high-voltage interrupters, shunt resistors may be used to carry the terminal voltage down to the bottom of the interrupter, the charging current being conducted with low voltage drop, thus eliminating high voltage stress across the insulation of the interrupter when the circuit breaker is open. Here again, high ohmic values of resistance are adequate.

(*c*). Reduced values of resistance in the range of several thousand ohms are usable in parallel with interrupters to influence the shape and to an appreciable extent the peak of surge voltages which appear across circuit breakers during the interruption of short-circuit and line-charging currents. Compressed-air breakers with limited interrupting ability on circuits with extremely high recovery voltage rate have used such resistors to advantage.

(*d*). With only a few ohms of resistance connected in parallel with a switch, the opening of such a breaker places the resistance in series with the circuit, raising the power factor and greatly reducing the short-circuit current for subsequent interruption by a second breaker. An outstanding case of this kind for 35 years has been the resistance-breaker scheme used on the New York, New Haven, and Hartford Railroad electrification from New York to New Haven. On this system, the operation of line relays has caused the insertion of resistance at the main switchhouse at Cos Cob, so that the opening of a relatively light sectionalizing breaker has been adequate to interrupt the faulty section. Recently, however, with improved circuit interrupters, it has been found feasible to eliminate the so-called switch-house scheme, and permit the line-sectionalizing switches to open the short circuit directly.

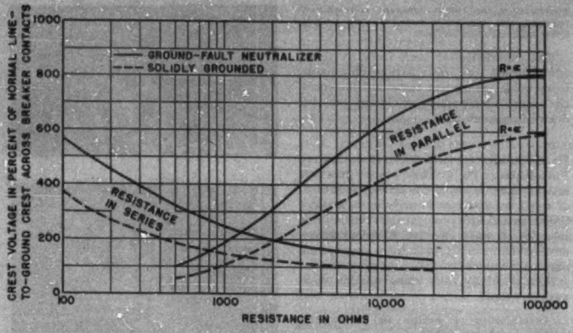

Figure 1. Resistance breaker interrupting charging current—230-mile 230-kv line

Phases *B* and *C* closed; two restrikes on phase *A*

An example of effective use of resistances discussed under items *a* and *b* is a series of tests completed on the West Coast during January of 1942 in which approximately 100,000-ohm resistors were used in shunt to each of four interrupters per pole unit of 230,000-volt breakers. These tests showed line-charging currents of approximately 75 amperes interrupted in total breaker times of $2\frac{1}{4}$ cycles with not more than 100 per cent surge voltage above the normal line-to-ground peak. This represents the performance of certain modern high-efficiency interrupters which are adequate with relatively high ohmic values of resistor in parallel, so that the residual current to be opened on the disconnecting break is negligible. One item to be noted in connection with the development described by the authors is that the residual current after the resistance has been inserted is approximately 40 amperes, and this must be interrupted on a plain oil break. Even with the high power factor in the circuit, after the resistance has been inserted, the delay in clearing this residual current is of some importance.

The authors are to be congratulated on working out the resistance scheme, here described, in a minimum space, for having shielded it properly to secure necessary insulation tests, and for having demonstrated it on one of the large American systems on which charging-current interruption has been a source of difficulty in the past.

R. D. Evans and **R. L. Witzke** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Hunt, Boehne, and Peterson have presented an important paper on the control of power-system switching transients. This is a well-rounded paper giving the results of

1. Miniature-system studies of the initial problem and the control by resistance breakers.
2. High-power laboratory tests.
3. Field tests of the initial switching transients and the resistance breaker for its control.

We are particularly interested in this investigation because it provides further evidence of the usefulness of the miniature-system method of investigating transients. For the present investigation this method provides the practical means of determining the ohmic value of breaker resistance for laboratory or field test.

In Figure 1 of this discussion are summarized the results of a similar study made in 1938 by the miniature-system method. It will be noted that the curves for the case of the resistor in parallel with the breaker are quite similar to the curves in Figure 7 of the Hunt, Boehne, and Peterson paper. As the breaker finally must interrupt current through the resistor, it is of interest to determine the effect of restriking during this part of circuit interruptions. For this reason additional studies were made with the resistor in series with the breaker. It will be noted that for this case lower values of resistance result in the higher transient voltages. Considering both types of charging-current interruption, the lowest transient overvoltage is obtained with a resistance of about 1,300 ohms corresponding to the intersection of the curves. This would correspond to the preferred value of resistance from the standpoint of limiting transient overvoltages on the system.

It should be recognized that switching overvoltages are functions of system layout, particularly of the length of line switched and of the grounding method. From the standpoint of these overvoltages, the worst types of systems are the high-impedance grounded and isolated-neutral systems. On solidly grounded systems the overvoltages may theoretically reach six times normal, as given in Figure 1, for a particular system. Lower overvoltages were obtained on a similar study for a considerably different system, as given in the paper by Evans, Monteith, and Witzke (reference 3 of the paper). Many field tests on several systems have been made with Westinghouse breakers, and the maximum voltage to ground that has been obtained is of the order of three times normal. On this basis the use of resistors to limit switching overvoltages may be quite limited and even unnecessary for the types of systems commonly used in this country.

W. F. Skeats (General Electric Company, Philadelphia, Pa.): To one who has worked with the theory of any type of equipment and made calculations as to its behavior, it is always extremely interesting to see how well the actual behavior of the equipment conforms to the calculated predictions. In the tests reported by Hunt, Boehne, and Peterson, voltage measurements were made on each side of each of the three poles of the breaker with oscillographic equipment selected and arranged so as to give an instructive record. Sufficient information was therefore available to check the performance both of the circuit breaker and of the generating and transmission equipment on either side of it.

In general, the generating and transmission equipment behaved according to theory. One interesting question in this respect was the extent to which phases already disconnected would be affected by surges on phases still in the process. With the system neutral solidly grounded, the mutual effect of this type in the transmission-line conductors was noticeable but not of sufficient magnitude to influence greatly the voltages obtained or to cause additional restriking in the breaker. With the ground-fault neutralizer in the circuit, however, there was in addition a quite considerable effect on the generator side of the breaker which caused directly an appreciable voltage surge and was also responsible for additional restriking in the breaker, which contributed still more to the voltage.

With regard to the circuit breaker the questions are:

1. Under what conditions will the arc restrike?
2. Will the oscillating current resulting from the restrike be cleared at the first high-frequency current zero?
3. How many restrikes will take place on any one phase in the course of an interruption?

The records indicate that

1. Without resistors, restriking may take place at any time up to almost a cycle after the last previous flow of arc current in the same breaker pole.
2. After a restrike at a small potential difference, the arc would, in general, not be extinguished at the following high-frequency current zero, but, after a restrike at a potential difference of the order of normal crest voltage or more, the arc usually was extinguished at this time, bringing about the possibility of cumulative voltage build-up.
3. With the solidly grounded system not more than one significant restrike per breaker pole was encountered on any one test. With the ground-fault

neutraizer in the circuit, however, and without resistors, two significant restrikes were several times experienced on one pole.

The knowledge of such facts as these complements the information obtainable from miniature-system tests and increases the accuracy with which conclusions may be drawn from such tests.

H. E. Strang (General Electric Company, Philadelphia, Pa.): This paper presents another illustration of the orderly sequence of steps so important in arriving at a solution to a particular problem and then making sure the solution is effective, as well as determining the extent to which its application may be broadened.

Fundamentally, the steps in this case were as follows:

1. Determination of problem and preliminary theoretical analysis of causes.
2. Transient-analyzer study permitting relatively quick determination of effect of various proposed solutions.
3. Tests at factory short-circuit testing laboratory to check conclusions arrived at through transient-analyzer study.
4. Staged field tests with complete recording equipment to determine performance under various actual field service conditions.

Regardless of factory test facilities available, such an investigation would have been inconclusive without the last step, the field tests. Never before has the industry been provided with so much actual recorded data on the nature and magnitude of overvoltages associated with switching long lines under various conditions. It is significant to observe from the chimney chart shown in Figure 11 of the paper (initially arranged by W. F. Skeats) that, regardless of the length of the line, the maximum line or bus overvoltages with a solidly grounded neutral ran only slightly over twice normal without the aid of the resistor, and that these conditions were handled without difficulty. This represents a fairly general case, and a condition under which hundreds of high-voltage breakers are regularly operating.

On the other hand, if some special local conditions produce a situation where, without resistors, voltages in the order of $4^{1}/_{2}$ times normal are produced, as shown in this chart, the resistors have proved effective in reducing these to the range of safe values, or in the order of twice normal.

This investigation has led the way toward making available quantitative values for terms which previously had been dealt with as hazy generalities. It has provided the method of determining the conditions which may lead to dangerous overvoltage switching surges and the means of controlling them where such conditions are found to exist.

T. W. Schroeder (General Electric Company, Schenectady, N. Y.): As electric-power systems have developed, transmission lines have gotten longer, and their voltages have increased. Occasionally, under special circuit conditions, the operation of circuit breakers in de-energizing these lines when unloaded has given rise to abnormally high voltage surges. This was the case for the particular application described which presented the need for the use of a special method of control for these particular conditions. Thus, it should be emphasized that the need for the addition of the resistor in terms of present experience is far from a general requirement when opening charging kilovolt-amperes in terms of long high-voltage lines or shunt capacitors.

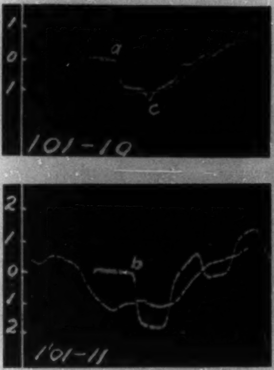

Figure 2. Oscillograms of voltages associated with energizing 180-mile transmission line at negative crest of 60-cycle source voltage

101-10 Voltage at source end
101-11 Voltage at far end

The analogy ably drawn in the paper between the behavior of a capacitor having zero surge impedance and a line or cable having distributed capacitance is interesting and worthy of further discussion. Figure 2 of this discussion shows a transient-analyzer oscillogram of the voltage at the sending and far open end of a 180-mile overhead line when it is energized at a negative 60-cycle voltage maximum. Oscillogram 101-10 shows the source-end voltage and 101-11 is the far-end voltage. The difference in time between the instant of energization, point *a*, and the appearance of voltage and its immediate reflection to double value at *b* is the time for the voltage wave to travel 180 miles which is roughly $(180/186{,}000\times10^{-6}=$ 970 microseconds, since the velocity of electric waves on open lines is approximately that of light. At this same time interval (970 microseconds) later, *c* the reflected wave arrives at the home end, where it is reflected almost entirely negatively except for the small positive reflection caused by a low value of source inductance. This reflection process continues until the original reflection decays because of line losses. At the far end the voltage transient is analogous to that obtained at the terminals of a capacitor when it is energized at voltage crest through an inductance. With resistance neglected, the frequency of oscillation for the capacitor is $1/2\pi\sqrt{LC}$, where L is the source inductance and C the capacitance. For the line, the fundamental oscillation frequency (see 101-11, Figure 2) is $1/4l\sqrt{LC}$, where L and C are the line inductance and capacitance per-unit length, respectively, and l is the line length.

The traveling wave voltages associated with interrupter restriking when de-energizing the first phase of a 180-mile artificial overhead line, described in the paper, are shown by transient-analyzer oscillograms in Figure 3. The line was opened at charging current zero (voltage crest) at point *a* of

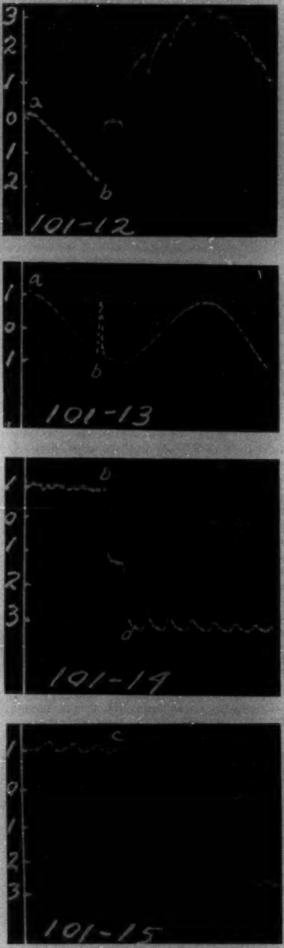

Figure 3. Oscillograms of voltages associated with de-energizing 180-mile transmission line with interrupter restrike at crest of switch voltage and final interruption at subsequent transient current zero

101-12 Voltage across switch
101-13 Voltage at source side of switch
101-14 Voltage at line side of switch
101-15 Voltage at far end of line

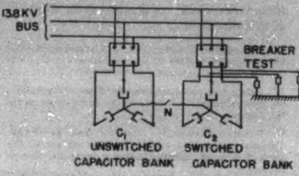

Figure 4

101-13, leaving a bound charge of approximately normal crest voltage *E*, on the line. At time *b*, near the switch voltage maximum, the arc was caused to restrike, starting the wave of $-2E$ down the line, which charged the line to $-E$. The wave's arrival at the far end changed the voltage at that point from *E* to $-3E$, neglecting losses (see *c* of 101-15), because of reflection without change in sign of the $-2E$ wave, and the reflected wave charges the line as it returns to the home end to $-3E$. Following the associated current waves along with this will show that now the interrupter current is again zero—hence the circuit was opened at time *d* of 101-14. It is of interest to note here that, at time *c*, although the open-end voltage has gone to $-3E$, the energy content of the line is given by $CE^2/2$, where *c* is the total line capacitance, whereas at time *d* the energy content has been raised to approximately $C(3E)^2/2$.

The prevention of restriking overvoltage by use of a resistor across the circuit-breaker contacts, as outlined in this paper for a long high-voltage transmission line, was also accomplished recently for a 15,000-kva 13.8-kv capacitor bank located in the substation of a large industrial plant. Three-phase full-scale tests reported in reference 5 of the paper indicated that, with standard breakers, when this magnitude of capacitor kilovolt-amperes was being interrupted, occasional restriking might be encountered. For this application it was decided to provide a resistor element across each break of the circuit breaker. These resistors were tested before installation, and field tests were made at the actual installation, where there was available a total of 22,500 kva of capacitors which could be connected to the breaker for test purposes.

Tests were made, not only to check the resistor effectiveness, but also to observe the performance of the standard breaker without resistors at capacitor ratings of 15,000 kva and above. Arresters were employed to protect the capacitors under the latter conditions.

Table I. Summary of Capacitor Switching Tests With and Without Breaker Resistor

No. of Tests	Switch N	Number of Restrikes	Number of Arrester Operations	Maximum Capacitor Kva Switched C_2
With Resistor Elements				
10	Open	5*	None	22,500
7	Closed	2	None	20,000
Without Resistor Elements				
13	Open	3**	1—at C_2 = 15,000 kva	15,000
7	Closed	None	None	15,000

* Beginning at 15,000 capacitor kva switched.
** Beginning at 10,000 kva switched.

Tests are summarized in Table I, with circuit connections as shown by Figure 4. A connection between the neutrals of the switched and unswitched capacitor banks was made which could be readily opened or closed.

Although more restrikes are shown for a comparable number of tests with the resistor than without it, it should be noted that those with the resistor occurred between 15,000 and 22,500 capacitor kva switched. In this range, without resistors, a restrike occurred causing an arrester operation; hence testing without resistors was stopped. However, tests were continued with resistors up to 22,500 kva, since the restrikes resulted in only low overvoltages because of the beneficial action of the resistors.

These tests accomplished the following results:

1. Permitted the conclusion that, although restriking occurs with the breaker resistor on switching capacitor banks of the higher kilovolt-ampere ratings, the resistor limits the resulting overvoltages to value below sparkover of an arrester of proper voltage rating for the system insulation values. The resistor is also effective in lowering the magnitude of the inrush current when energizing a capacitor bank.

2. Further substantiated the approximate limitation of standard breakers fixed by tests previously reported in reference 5 of the paper.

3. Showed the effectiveness, from a restrike prevention standpoint, of interconnecting the neutrals of switched and unswitched wye-ungrounded capacitor banks, pointed out from analysis and miniature tests reported in preceding reference. It was shown in that paper that the effectiveness of neutral interconnection decreases as the ratio of unswitched to switched capacitance C_1/C_2 decreases.

Lloyd F. Hunt, E. W. Boehne, and H. A. Peterson: Eric T. B. Gross points out that lightning arresters have been used in Europe and other foreign countries to bypass the ground-fault neutralizer at the far end of a single circuit line to avoid the increase in overvoltages due to the resonant grounding of the neutral. He implies that this is the simplest and cheapest method of overcoming the voltage surges. It is reasonable to expect that such a practice would limit overvoltages that might appear across the ground-fault neutralizer, but it is clear that it would fall far short of solving the overvoltage problem on the high-voltage system. Consequently, a comparison on a cost basis is not permissible, since the purpose to be achieved is not the same in both cases. The addition of resistors to high-voltage breakers of the multigap construction is an economical proposition. Moreover, such resistors solve the switching overvoltage problem under all conditions, including that of a solidly grounded neutral.

Mr. Gross also brings to our attention that the practice in Europe of installing resistors in breakers for the purpose of minimizing the prolonged inrush currents with the parallel transformers has been discontinued. Such resistors have been avoided in American practice mainly because of the

1. Improved transformer construction for the purpose of withstanding such currents.

2. Modern relaying practice (harmonic restraint relays) which adequately protect the system in spite of the transformer inrush phenomena.

It is, therefore, hard to see how Mr. Gross draws the conclusion that the elimination of resistors within breakers of European circuits from a relaying standpoint indicates that we are making a step backwards when simple resistors are installed within high-voltage breakers for the purpose of overcoming a severe overvoltage switching problem.

It is interesting to point out in connection with the distribution of voltages between the two breaks of an oil circuit breaker at the time of interrupting a capacitive circuit that, although the net voltage across the breaker is zero immediately following interruption, this voltage divides between the two breaks approximately +80 per cent of the crest value of line-to-neutral voltage across one break and −80 per cent of the crest value of line-to-neutral voltage across the other break. This fact is only of academic importance, inasmuch as, should one break discharge to the crosshead, the resulting voltage across the other break would collapse to the smaller net voltage across the breaker.

Mr. MacNeill presents some of the early history showing several possible applications of resistors in shunt with circuit interrupters. With values of shunt resistance in the neighborhood of 3,000 ohms, purposes *a* and *b* are accomplished as well as *c*, in so far as it applies to the interruption of line-charging currents. Aside from the reduced recovery voltage and shorter arcing times, resistors of this value have practically no effect during the interruption of short-circuit current, as the small resistor current (40 to 50 amperes in this case) is so very small, relative to the fault currents which flow prior to arc interruption. For all practical purposes as far as effects on the system are concerned, the fault is removed as soon as the resistor enters the circuit. From the standpoint of the breaker, the resistor reduces internal energy in spite of the resistor current. The delay of two cycles or so in clearing this residual current is of no practical importance.

Evans and Witzke have pointed out the results of a similar analysis which includes the effect of interrupting the residual resistor current. While this analysis leads to their conclusion that there is an optimum value of resistance to be used for their purpose, actually thermal and space limitations would, from a practical standpoint, generally eliminate the lower values of resistances before the increase in overvoltages due to the small resistance came into effect. Including the effects of these factors, it appears that the over-all optimum value, from a safe operating standpoint, is substantially greater than the value obtained by considering overvoltages alone.

Mr. Skeats has pointed out that there is a need for analyzing carefully the results of field tests so as to arrive at a better understanding of switching surge phenomena, and so as to be in a better position to carry on and interpret results of miniature-system studies. This is a very good point, and the authors agree wholeheartedly with this philosophy.

Mr. Strang has pointed out that these resistors are not necessarily required in hundreds of installations where high-voltage breakers are operating regularly today. This is certainly true. Switching overvoltages are affected by many interrelated factors. High-impedance grounded systems are generally more susceptible to the generation of high-voltage switching surges than solidly grounded systems. Although in many cases some benefit might be obtained from the use of resistors on solidly grounded systems, particularly where single-circuit long high-voltage lines are to be switched, the need for them would not be so great as it would be for similar systems operating with high-impedance grounding or with neutrals isolated.

Schroeder and Rietz have shown oscillograms which enable one to visualize more clearly the true nature of switching surges involving traveling waves on long transmission lines under specific terminal conditions. The summary of results obtained in switching capacitors is interesting and indicates that resistors are helpful in limiting overvoltages for this condition as mentioned in connection with Figure 1 of the paper. Thus the use of shunt resistors in breakers makes it possible to switch more capacitive kilovolt-amperes at one time than otherwise might be desirable from the standpoint of possible switching overvoltages. Many applications of this principle are being put to use today and are playing their part in conserving critical war materials. In numerous places one breaker with resistors is doing the job required of two without resistors.

Performance Requirements for Relays on Unusually Long Transmission Lines

Discussion and authors' closure of paper 43-37 by F. C. Poage, C. A. Streifus, D. M. MacGregor, and E. E. George, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, volume 62, 1943, June section, pages 275–83.

R. D. Evans (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The power-system quantities measurable at a relay location for determining relay operating characteristics are difficult to classify and present in a systematic manner. Many methods may be used for this purpose, such as voltage and current quantities, or their products as power quantities, or their ratios as impedances or admittances. The quantities may be expressed in terms of circuit constants and synchronous machine electromotive forces in magnitude and phase position, which would be a fundamental approach but which, unfortunately, is unduly complex. The authors have selected the near-end impedance of the network, including the long-line branch, and the ratio of near- and far-end bus voltages and their displacement angle. This basis is not only generally suitable but also is in a form which the power engineer can define readily from his knowledge of the *ABCD* constants of the circuit and its power-circle and power-angle diagrams.

The usual method for presenting operating characteristics of relays is in terms of voltage and current quantities; this will continue to be used in designing and testing relays. The method used by the authors has the merit of a different approach which clarifies some problems and facilitates understanding by some engineers. In addition, this method has the advantage of making available the solution of problems which had been worked out in related fields in connection with *ABCD* constants and circle diagrams.

In the derivation of the impedance diagram from the admittance form, the authors make use of the geometric inversion of circular loci. This method, well-known to geometers, is little known and still less used by electrical engineers. While this principle has limited application in electrical engineering, it is likely that there will be other instances of its usefulness.

D. M. MacGregor (Ebasco Services, Inc., New York, N. Y.): This discussion describes a relatively simple graphical method, which may be used instead of that given in Appendix V of the paper, for locating the δ_{rs} points on the impedance-circle diagrams.

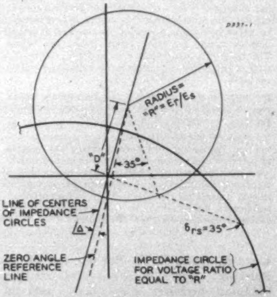
D337-1
RADIUS= "R"=Er/Es
"D"
35°
LINE OF CENTERS OF IMPEDANCE CIRCLES
Δ
δ_{rs}=35°
ZERO ANGLE REFERENCE LINE
IMPEDANCE CIRCLE FOR VOLTAGE RATIO EQUAL TO "R"

Figure 1. Graphical method of locating points on impedance circle diagrams corresponding to various values of the angle δ_{rs}

This method is based upon the equation:

$$\psi = \text{conjugate of}\quad \tan^{-1}\frac{D\sin(\Delta-\beta)+R\sin(\delta_{rs}+\beta)}{D\cos(\Delta-\beta)-R\sin(\delta_{rs}+\beta)}$$

All symbols are the same as in the original paper except that the letter R has been used to indicate the ratio of the receiving-end voltage E_r to the sending-end voltage E_s.

It may be shown that the preceding equation represents a circle with a radius R and its center on the line of centers of the impedance circles at a distance D from the origin.

The method of using this expression to locate the values of the angle δ_{rs} on the impedance circles is illustrated in Figure 1 of this discussion:

1. Lay off the distance D to any convenient scale on the line of centers of the impedance circles and on the $+jX$ side of the resistance axis.

2. Using this point as a center, draw a circle having radius equal to R and to the same scale as for the distance D.

3. Draw a zero-angle reference line as shown. This line corresponds to $\delta_{rs}=0$ and is located at the angle Δ degrees counterclockwise from the line of centers.

4. Measure off the desired values of δ_{rs} on the circle in a counterclockwise direction from the zero-angle reference line.

The intersection of a line through any one of the above points and the origin of the impedance diagram with the impedance circle of the corresponding voltage ratio R represents the impedance value for that particular value of δ_{rs}.

In actual practice, it is unnecessary to draw the construction lines on the impedance diagram, as it may be done equally well on a piece of transparent paper placed over the diagram. The final points of intersection may be transferred to the impedance diagram by pricking through the construction sheet with a compass point.

E. L. Harder (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper is valuable in stressing the utility of phase angle between current and voltage in fault detection. While some use has been made of angle in distinguishing between loads and faults in the same direction on a line,[1,2] the angle quantity has not come into general use, perhaps because there has not been a general need. Individual cases have undoubtedly appeared where the absence of general knowledge of the phase-angle characteristics of loads, swings, and faults has prevented exploiting to the full this possibility. The present paper will help materially to supply this deficiency.

Some of the past uses of angle may be of interest. On the Pennsylvania Railroad[1] trolleys of the electrified zone, the need arose for differentiating between a 3,500-ampere load which might occur with locomotives at the near end of a trolley and a 2,000-ampere fault which might occur at the remote end. As the trolley impedance was 15 times the source impedance, the source (substation) voltage dropped but little for the remote fault, and thus the impedances seen by the relay were almost inverse to the currents. The impedance element which was used in this case balanced E^2 against $EI\cos(\theta+\phi)$, which is the same as E against $I\cos(\theta+\phi)$. θ is the power-factor angle and ϕ the relay-adjustment angle that determines the maximum torque position. This element was adjusted for maximum torque with current lagging voltage by the angle that occurs during fault, or greater. Thus loads could be carried of lower impedance (higher current) than the relay setting for faults, if they occurred at a higher power factor.

Another case is the relaying for the long line used to supply construction power for Fort Peck Dam.[2] In this case advantage was taken of the ability to adjust the high-speed impedance element to trip for nearly twice the impedance (half the current) at fault phase angle that would be required to trip at possible load phase angles. The line

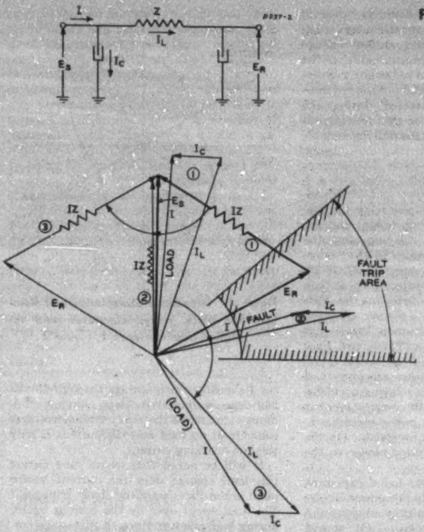

Figure 2

ABCD constants were used in developing the possible angles of load and fault currents, and the impedance relay setting was based on the equivalent impedance, *B/D*.

The use of impedance circles by the authors of the present paper provides a unique method of arriving at the relay settings and checking the available margins. For purposes of visualizing the phenomenon, however, the current locus has considerable advantage because of the more general familiarity with this method. For a long line the bus voltage is so nearly the same for loads and remote faults that the refinement of taking it into account is needed only for exact settings rather than for visualization or determination of the type of relay required.

Assume a long line of 60-degree angle between the ends. The triangle formed by sending and receiving voltages and the line drop is then equilateral, as shown by the heavy lines, Figure 2 of this discussion. If a fault occurs at the *R* end, the magnitude of the line drop does not change; it simply changes 60 degrees to position 2. Full load in the reverse direction results in a further change of 60 degrees to position 3. The current, I_L, over the line must always be the same angle behind the line drop and proportional to it in magnitude. Thus the currents, I_L, for the three conditions are 60 degrees apart as shown at 1, 2, and 3. A constant current, I_c, must be added to get the relay current *I*, resulting in somewhat over 60 degrees between load and fault currents at the relay. This shows the need of relaying elements having a trip characteristic somewhat as shown shaded to segregate loads and swings from faults.

It is apparent that moderate changes in the magnitude of E_R or E_S can only slightly change the position of the line drop and hence of the current. Thus, if the current is large, it must be about in the position shown. If small, it is not sufficient to operate the fault-detector element.

This treatment lacks the precision required for actual settings, which the method of the paper admirably provides. It is offered as an assistance in visualizing "what goes on", rather than as an alternate method of calculations, although, if used with judgment, it is frequently adequate for determining the required angular characteristics.[3]

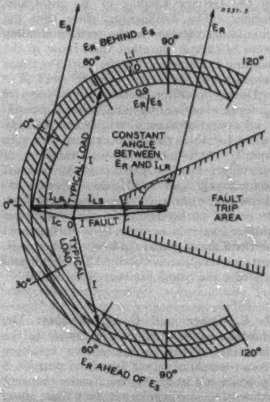

Figure 3. Current locus

O—Origin of vectors

F—Terminus of fault vector

Shaded area—Terminus of load and swing vectors

For other line angles than 60 degrees, the current locus can be used to show the possible positions of load currents. This plot is based on the conception of the current in the line as the superposition of two currents. One is produced by the sending voltage, (with the receiver short-circuited) and is labeled I_{LS} in Figure 3. The other is produced by the receiver voltage, E_R (with the sender short-circuited) It is I_{LR}, and is opposite to I_{LS} (using the same reference direction) if E_R is equal to, and in phase with, E_S. For other values of E_S, I_{LR} terminates somewhere in the shaded area. As E_R is moved ahead or behind E_S, I_{LR} moves with it. If E_R is increased or decreased, I_{LR} increases or decreases in proportion, thus describing the shaded area. The total current in the line is the sum, $I_L = I_{LS} + I_{LR}$, and is a vector from *P* to some point in the shaded area, depending on the magnitude and phase of E_R. To it must be added a constant charging current I_C, 90 degrees ahead of E_S, to obtain the relay current, I. Thus relay-current vectors are from *O* to *F* for fault at the remote end and from *O* to points in the shaded area for loads and swings resulting in various angles between E_S and E_R and various magnitude ratios E_S/E_R. This illustrates that load and swing currents do not fall in the fault-current trip region and shows the angular margin very well, since the angle scale is uniform.

REFERENCES

1. PENNSYLVANIA RAILROAD NEW YORK–WASHINGTON–HARRISBURG ELECTRIFICATION—RELAY PROTECTION OF POWER SUPPLY SYSTEM (Induction-Type Impedance Relays), E. L. Harder. AIEE TRANSACTIONS, volume 58, 1939, June section, page 271.

2. CONSTRUCTION POWER FOR FORT PECK DAM, E. A. Childerhose. *The Electric Journal*, volume 32, February 1935, page 47.

H. R. Summerhayes (General Electric Company, Schenectady, N. Y.): In discussing the three papers, 43-37, "Performance Requirements for Unusually Long Transmission Lines"; 43-38, "Protective Relaying for Long Transmission Lines"; and 43-39, "Staged-Fault Tests of Relaying and Stability on Kansas–Nebraska 270-Mile 154-Kv Interconnection", I wish to point out that the magnitude of this achievement can be realized better by stepping back a few years to get a proper perspective and by comparing the design of relays to the education of children.

We have come up a long flight of stairs in those years, stopping for breath at the landings, and then progressing again.

Our old relays could feel amperes or volts and act accordingly. We had taught them to count, in seconds, not cycles. They would not always "co-operate," and we had not taught them to "supervise". Perhaps we could have taught our relays more if we had known more about stability, swing curves, and transient conditions, all of which had to await modern means of calculation and measurement.

The authors have taught these new relays to see and to see so well that they can distinguish ohms from mhos and measure their size 270 miles away. They also perceive such things as phase angles, with the aid of carrier current, and they count in cycles or split cycles instead of seconds.

The relays have also been taught to co-operate with each other and to supervise, and the authors speak of the staged tests as determining the physical condition and the IQ of the relays—very similar to the tests made on children.

It is evident from the diagrams that the relays have at least a working knowledge of trigonometry.

I salute the relay engineers.

W. R. Brownlee (Commonwealth and Southern Corporation, Jackson, Mich.): The various authors of these papers are indeed to be commended for their vision and imagination in connection with a co-ordinated scheme of protection and operation to make a 270-mile transmission connection of genuine value. for their painstaking and exhaustive analysis in isolating the more difficult parts of the problem for study, and for their demonstration of sound engineering judgment in building around their studies and tests an over-all system of such proved worth. The results have made possible the supplying of badly needed power for war purposes in a relatively short time by the most satisfactory method. Many of the concepts utilized by the authors should also prove of material value in analyzing the protection and operation of shorter existing lines, so as to make certain that, under critical conditions resulting from war, the protective equipment does not limit unnecessarily the usefulness of major investments. Careful studies of maximum power ability versus the extent of relay backup protection are in order.

The utilization of an automatic three-phase line-grounding switch to provide complete protection of the transformers without the use of high-voltage breakers is another example of major savings in cost and in critical material by the co-ordinated use of protection methods. By permitting faults to remain on a transformer some five cycles longer, a single-pole automatic grounding switch could be used which would reduce the shock to the sending end of the system. However, this is probably not important in this particular application, since the length of the line provides a relatively high cushioning impedance.

Since carrier-current equipment is required for the relaying of the transmission line, consideration was probably given to the use of a transferred tripping arrangement over this channel whenever differential relays operate, thus avoiding the need for other than manually operated three-pole grounding switches. I would like to ask the authors what considerations led to their decision not to use such transferred tripping.

Since the United States was involved in this war, some have maintained that staged short-circuit tests should be abandoned for the duration, on account of the scarcity of materials for repairs or replacements of equipment which they conceive might be damaged by such tests. These same proponents probably would not argue against high-potential tests on new cable installations but rather would insist that the over-all reliability be demonstrated before the cable should be trusted as dependable capacity. It is, therefore, vitally important that, even after all phases of the problem were studied most exhaustively, over-all factory tests performed on the relays using an artificial line, and adequate low-voltage tests of the relay equipment made in the field, the authors had the judgment to insist on staged-fault tests. The unforeseen difficulties found and corrected during the staged tests are proof of the necessity of this final step in assuring over-all dependable performance.

E. A. Childerhose (Jackson and Moreland, Boston, Mass.): Calculating relay requirements, protective relaying design, and the staged-fault tests of relaying for the Kansas–Nebraska 270-mile 154-kv interconnection described in papers 43-37, 43-38, and 43-39 are extremely interesting, as they substantiate the work done on the Fort Peck transmission line (*Electric Journal*, February 1935, "Construction Power For The Fort Peck Dam"). These two lines are very similar, the Fort Peck line being 287 miles long, fed by autotransformers and operated at 154 kv. The relaying problems presented by the earlier line were somewhat simpler, because neither power interchange nor rapid reclosure was involved. On the other hand, it was operated closer to the steady stability limit.

In both instances it was found expedient to utilize the differences in line power factors existing in normal and fault conditions and to use distance-type relays. Of special interest are the oscillograms obtained, as none were taken during the tests on the Fort Peck line. The authors of these papers are to be congratulated on the thoroughness of their investigation of the problems to be encountered before designing the relay system and the operating tests made to determine its effectiveness.

Apparently the same error was made as on the Fort Peck scheme when the overcurrent element of the ground relay was connected originally to the current transformer in the tertiary winding of the autotransformer. The oscillograms show that the circulating currents in the tertiary winding were of shorter duration than those in the sending transformers of the Fort Peck line which lasted for several seconds.

During operation of this line it was several times de-energized by simultaneous ground-relay operation at each end. No satisfactory cause for these relay operations was ever found. Apparently it was transient in character, for the line could be reclosed immediately and would then operate indefinitely, without further relay operations. It would be interesting to know whether similar experiences have been encountered on the Kansas–Nebraska line, whether oscillograms have been obtained for them, and whether the cause has been ascertained.

F. C. Poage and **C. A. Streifus:** Mr. Harder's explanation of the change in the direction of the current vector resulting from reversal of power flow from position 1 to position 3 in his Figure 2 omits the very interesting and significant fact that, upon reversal of power flow, the receiving-end voltage E_r moves counterclockwise from the position 60 degrees lagging through 0 to 60 degrees leading with respect to the E_s voltage, corresponding to equal power flow in the opposite direction. The current vector I simultaneously swings through almost 240 degrees counterclockwise, having minimum value when the two terminal voltages coincide at no load and the current is only the line-charging current.

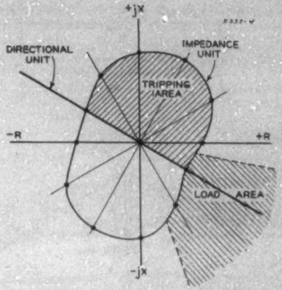

Figure 4. Impedance characteristics of Rainbow–Fort Peck relays computed and replotted from reference 1, Figure 2

It will be noted that at no time during this load change does the current vector point in the direction of the "fault-trip area." The area swept over by the current vector during load changes through this range corresponds to the impedance values in Figure 11 of our paper lying on the circle $E_N/E_K = 1.0$ between the successive values of 60, 30, 0, 330, and 300 degrees.

If the sending-end voltage E_s is *assumed* to be *constant*, Mr. Harder's Figure 3 and our Figure 4 are identical, and his points O and F are respectively O and O' in our sending-end power-circle diagram. His discussion amplifies and supplements the part of our paper covering "Current Magnitude and Phase-Angle Bases for Relaying."

Both Mr. Harder and Mr. Childerhose have mentioned the special relays developed for the 287-mile Fort Peck–Rainbow 161-kv line. Figure 4 shows the approximate characteristics of the impedance and directional elements of those relays replotted on the same impedance co-ordinates as are used in Figures 9, 11, and 12 in our paper. The ohm values are omitted from this figure, because we do not know the actual relay settings. However, it may be seen that if the tripping area is made large enough to safely overreach the far end of the line and enclose all the area as designated by B on our Figure 12, it is also broad enough to enclose some of the no-trip areas D and G in which it is necessary to prevent tripping during power swings or upon high-speed reclosing on an interconnection.

Since the Fort Peck line relaying was not required to meet these service conditions but was required to discriminate only between steady-state load and short-circuit, the relays used were well suited to the job. However, if this line were an interconnection between two systems or between a large generator and a system, it is quite probable that the relays could not meet the second of the six performance requirements for long-line relays as given in our paper.

In concluding this discussion we would like to emphasize an idea which is implied throughout the paper: namely, in order completely and correctly to correlate relay performance and system performance, both must be expressed in the same terms and plotted to the same co-ordinates. If we are to use relays which are responsive to current, or to volt-amperes and phase angle, the system performance needs to be expressed in terms of inphase and quadrature amperes or watts and vars. Similarly, if we are to use impedance-type relays which are responsive to ohms and angles, then the system performance also needs to be expressed in ohms and angles or in the equivalent rectangular co-ordinates of resistance and reactance. For transmission circuits the former need is met by current-circle diagrams and power-circle diagrams which have been standard tools of the power system engineer for many years. It was to meet the latter need brought about by use of the newer impedance-responsive relays and relay elements that the derivation, construction, and study of impedance circle diagrams was undertaken.

Reference

1. Construction Power for Fort Peck Dam, E. A. Childerhose. *Electric Journal*, volume 32, February 1935, page 47.

Staged Fault Tests of Relaying and Stability on Kansas–Nebraska 270-Mile 154-Kv Interconnection

Discussion and authors' closure of paper 43-39 by C. W. Minard, R. B. Gow, W. A. Wolfe, and E. A. Swanson, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, volume 62, 1943, pages 358–67.

John G. Holm (Stone and Webster Engineering Corporation, Boston, Mass.): The authors should be complimented on the interesting field tests they undertook with staged faults on a long transmission line. The effect of line faults on the stability limit of the line and their correlation with data obtained on the network analyzer is of particular interest.

The authors had an unusual opportunity in having at their disposal a 270-mile single-circuit line equipped with reclosing circuit breakers and operating at a voltage which is rather low in comparison with the line length. It seems to me therefore quite desirable that some additional data be published, in order to make the stability part of the paper more complete and thus permit its results to be coordinated with data previously published.

Insofar as the steady-state-stability limit of the Kansas–Nebraska line is concerned, I was able to check it with some of my own data obtained on the a-c network analyzer,[1] and I am glad to state that the authors' results and mine are in complete accord.

I was unable, however, to check or to recalculate the transient-stability limits given by the authors. The reason is that the authors gave no data on the setting up of their system on the network analyzer. It would be interesting to know how the power stations at the system ends were represented on it. Were the synchronous condensers at the system ends represented? Was there a load at either or both ends of the system? Were the generators of normal design? What were their transient reactance, short-circuit ratio, and intertia? What damper windings did they have? It seems to me that, with these data on hand, a clearer meaning could be found of the transient-stability limits of the line studied.

It appears from Figure 14 of the paper thae the field tests and the tests made on the network analyzer were made at different total breaker reclosing times. Had the two types of tests been made at identical breaker operating conditions, a check could be made between the two methods of investigation, as well as an assurance that the slope of the curve of Figure 14 is correct. This would be a point against the statement sometimes made by engineers to the effect that field stability tests and network-analyzer measurements do not coincide.

From the data given in Table I of the paper it may be seen that the clearing of the same type of fault at either end of the two-directional power-flow line, at the same total reclosing time in both cases, results in practically identical transient-stability limits. This reversibility—insofar as the transient-stability limit is concerned—is worth emphasizing, although it has been obtained in one case only.

It may be a long time before engineers will have the unique occasion of having at their disposal the interesting type of line the authors studied. I think, therefore, that it would be very valuable to have at this time the additional information discussed here.

Reference

1. Stability Study of A-C Power Transmission Systems, John G. Holm. AIEE Transactions, volume 61, 1942, pages 893–905.

A. E. Frey (Pacific Power and Light Company, Yakima, Wash.): The paper by Minard, Gow, Wolfe, and Swanson emphasizes the fact that it is possible with proper relay and control equipment to secure satisfactory operation under exacting conditions. It appears from the data presented that the new long-line relays, in conjunction with carrier-current-control equipment, have largely overcome the limitations which were apparent a few years ago on similar installations. Undoubtedly, the use of carrier-current-controlled relaying will come into wider use in the future, since with such a protective setup proper relaying can be secured for almost any operating condition. Up to the present time the application of carrier-current-controlled relaying has been primarily a question of investment and maintenance against benefits to be derived. This factor has been a more or less arbitrary figure, which unquestionably has been responsible more than anything else in the rejection of such protective equipment. Recently, certain other factors have come to the attention of the operating personnel which will probably make the application of such equipment mandatory, regardless of the investment or maintenance costs. Of course, an added incentive for carrier-current-controlled relaying exists if part of the carrier-current equipment can be utilized for communication purposes. We presume that the Kansas–Nebraska interconnection utilizes this dual possibility.

Present war conditions have made the pooling of power resources a vital necessity, and in many instances additional tie lines and transmission lines had to be provided. We feel that such lines should be provided with the most suitable equipment, since the benefits derived are to the mutual advantage of all concerned. We in the Northwest have had a power-pooling arrangement for some time,[1] and the proper relaying on such interconnected systems has always presented a peculiar problem. Also during recent years government-sponsored power systems have come into existence which to a certain extent parallel the privately owned systems. Here again, the relaying of the interconnections have presented problems, principally resulting from the fact that the older systems had to change their relaying methods to conform to the new high-speed requirements. Another point which has complicated the protective-relaying applications is the fact that some of the recently erected tie lines had to be put into operation immediately after the lines were completed which, of course, made it impossible to secure any of the data which could be obtained under staged-fault conditions. In several instances lines had to be put in service with temporary relay equipment, and subsequent analyzer studies indicated that comprehensive protection for some of these lines would call for similar equipment as is mentioned in the paper.

The value of the staged tests described in the paper is quite apparent. It is evident, for instance, that such items as relay contact trouble probably would have resulted in faulty operation if this condition had not been detected during the staged tests. There still exists some apprehension in some quarters concerning staged tests on power systems. We believe that a very convincing answer to this is contained in the paper which shows that the system disturbances were negligible and that apparently no damage to equipment resulted. Naturally, the expense involved in such tests varies with the scope of the investigation, and here gain that intangible factor of cost versus benefits derived enters into the matter.

The resultant action of the relay and control equipment during fault conditions is shown clearly on the charts under Figure 9 of the paper. It is interesting to note in this connection that the flow of megavars was unidirectional during all tests. While the authors state that the agreement between the analyzer studies and the field-test data, with respect to the transient-stability limits, was reasonably good, we find by examining Figure 14 that for 21-cycle clearing time the theoretical studies predicted a limit of 46–24 megawatts, whereas the test values at $24^{1}/_{2}$-cycle clearing time gave only 30 megawatts. Also the data in Table I show that for a fault in Nebraska the Kansas relays energize the oil-circuit-breaker trip circuit in 1–2 cycles, whereas on the other hand for a fault in Kansas the Nebraska relays operate in 3–$3^{1}/_{2}$ cycles. Since the relay terminal equipment appears to be identical, we are somewhat interested in this dissimilarity. While the paper does not specifically mention this point, we assume that the reclosing relays operate on the three-reclosure principle before locking out.

REFERENCE

1. Co-ordination of Operation in the Northwest. L. B. Cowgill. *Electrical West*, April 1942.

C. N. Hawley (Nebraska Power Company, Omaha, Nebr.) and **H. E. Margrave** (Kansas Gas and Electric Company, Wichita, Kans.): Since the actual operation of long lines is a comparatively recent contribution to the art of electric-power transmission, the authors have presented a timely and practical paper.

The comprehensive manner in which the staged-fault tests were made is indeed appreciated by those who are directly concerned with the operating behavior of the systems involved. With such testing completed before normal operation was started, the usual period of expectancy for some unforeseen condition to appear was brief. A confidence was developed quickly that operating results would be as planned. The interconnection has been in service for approximately six months now with regular schedules of power transfer, and, although only one tripout due to line fault has occurred, we have no reason to feel that protective relaying or other equipment will not function properly when required.

The automatic-reclosing features of the terminal breakers are controlled by operation selector switches. These selectors may be set to operate for any one of the following movements:

1. Manual or nonautomatic.
2. One initial reclosure in zero seconds (approximately 21 cycles) and one subsequent reclosure as predetermined by timer setting.
3. Automatic synchronizing only, no other reclosures.
4. Reclosures on de-energized line at intervals as predetermined by timer or automatic synchronizing on energized line as the case may require.

Initial reclosure in zero seconds (21 cycles) is also provided in this setting.

The normal setting now in use of selectors at both terminals is that described in item 4. Timers are set for subsequent reclosures with staggered timing in order to maintain right of way as to line trials or autosynchronizing as the case may be.

When line trips out, both terminal breakers close in 21 cycles or before systems fall too far out of synchronism. If this trial fails, terminal 1 tries line, and, if line holds, terminal 2 automatically synchronizes; however, if line does not hold, then terminal 2 in its turn, as determined by timer for de-energized line, tries line. If it holds, terminal 1 then takes over with autosynchronizer. In other words, the timers are in control when line is de-energized, and autosynchronizers when line is energized. With this setup, one attempt is made at initial reclosing before systems fall out of step, and two reclosures are made, one from each terminal, before lockout condition is obtained. If lockout occurs, system dispatchers take over, or, in case of communication failure, emergency instructions are provided so that terminal operators may proceed according to conditions as indicated.

Fifteen thousand kilovolt-amperes in shunt reactors consisting of three 5,000-kva units are provided at each terminal and are likewise controlled automatically as a normal condition. Over- and undervoltage relays are employed to bring reactors on, in case voltage becomes too high, or to bring them on, if line becomes de-energized. Predetermined time delay is provided in both cases. Regardless of whether reactors are in on or off position as may be normally required to control system voltage and reactive properly, they are set for auto-operation as previously mentioned.

To date, several faults have occurred on the systems immediately adjacent to the interconnection, with results that special relays properly observed condition and prevented tripping of line.

H. R. Summerhayes: For discussion, see page 423.

W. R. Brownlee: For discussion, see page 424.

E. A. Childerhose: For discussion, see page 424.

C. W. Minard, W. A. Wolfe, and **C. A. Streifus:** The authors and those associated with the design and testing of the relays are appreciative of the complimentary remarks by the several discussers of this paper and of the interest shown in this subject. Especially do we wish to thank H. R. Summerhayes for his contribution which humanizes a highly technical subject.

We are unable to include here all the data requested by Mr. Holm. In the analyzer studies the systems at the ends of interconnection were set up in detail, including loads and other interconnecting systems, using conventional methods. Direct-axis transient-reactance values were used to represent internal impedances of machines. No account was taken of damper windings. Because of the limited number of generator units available on the analyzer, synchronous condensers were not represented dynamically except in special cases.

Because the network-analyzer studies were made for the purpose of determining the best of several alternate arrangements of equipment and circuits before the interconnection was designed and built, it was possible to find, among all of the transient-stability studies made, only four which can be considered representative of the interconnection as actually constructed. All of these are plotted on Figure 14 of the paper. The general slope of the boundary curve M-N was established from other studies made on a two-machine basis and confirmed by further analyzer studies made under different operating and fault conditions. The curve theoretically should intersect the zero-time axis at the load representing steady-state stability and should coincide with the zero-load axis at extremely long switching times. Further refinement in locating this boundary line M-N is hardly warranted because of the large number of combinations of load and generation affecting stability which are possible in actual operation on the interconnected systems.

Some of the network-analyzer studies showed that the interconnection might have different transient-stability limits, depending upon direction of power transfer in the interconnection. However, these particular studies were made under conditions which were not represented in the staged-fault tests. In the present systems and during the staged-fault tests the inertias of the two systems are nearly enough equal to give very little difference in performance as the result of reversal in power-flow direction.

We believe that Mr. Frey has misinterpreted the data represented by the 46- and 24-megawatt points in Figure 14. These are loads at which specific tests were made. Under one of these conditions the systems were stable, and at the other they were unstable. The curve M-N which represents the approximate boundary between stable and unstable conditions lies between these two values. The differences between tripping and reclosing times at the two ends of the circuit, noted in Table I, are due to operating characteristics of the breaker and relays as set at that time. Most of these differences have since been removed, by resetting the relays and readjusting the breakers. The communication feature of the carrier-relay sets is used only for testing purposes and in case of outage on the separate carrier-current communication channel. The automatic synchronizing and reclosing features mentioned by Mr. Frey are reviewed very completely in the discussion by Hawley and Margrave.

The three-pole spring-operated grounding switch discussed by Mr. Brownlee is a part of the line air-break switch installed for safety reasons to permit short-circuit and ground protection during line work. The additional cost to add the trip-coil-actuated spring-closing mechanism was small compared with the cost of additional transfer trip equipment for the carrier-current channel, and the simplicity of this scheme was decidedly in its favor. It is not dependent upon the operation of any remote apparatus or any communication channel. Also it was desirable to relieve the carrier-relay system of this function so that the channel might be used later in more essential services. Since the transformer differential relays which operate the ground switch also trip the main terminal breaker which in turn opens the near end of the line before the slower-acting ground switch can close its contacts, there is no shock to the near system, and the effect on the far system is small as demonstrated in the two tests of Group I-*f*.

Experience cited by Mr. Childerhose of the Fort Peck line tripping out during normal operation by ground relays has not been duplicated on the Kansas–Nebraska interconnection. As explained in the paper, circulating currents in the tertiaries were higher than anticipated with the interconnection energized from one end only, and for that reason ground-relay connections had to be changed. Oscillograph records of these ground currents were obtained, but since the difficulty which they caused has been eliminated, no further study of these has been made.

Protective Relaying for Long Transmission Lines

Discussion and author's closure of paper 43-38 by A. R. van C. Warrington, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, volume 62, 1943, June section, pages 261–8.

H. R. Summerhayes: For discussion, see page 423.

W. R. Brownlee: For discussion, see page 424.

E. A. Childerhose: For discussion, see page 424.

A. R. van C. Warrington: The question was raised as to whether this scheme of relaying could be applied to shorter lines. The scheme was developed to overcome the ineffectiveness of the standard impedance-type fault detectors on very long lines, but it can be applied to any line down to two ohms secondary-phase to neutral impedance (somewhere around 20 miles at 115 kv) with the present relays, though it would be possible to modify the relay to cover shorter lines.

During an out-of-step condition it is generally desired to separate the system only at a predetermined point, such as the electrical center. This can be arranged by giving the out-of-step ohm units a higher impedance setting at the breakers chosen. With equal settings the relays nearest the electrical center will trip first. Furthermore, only one breaker trips (the one at the leading end) which facilitates reclosing and provides a distinction between out-of-step tripping and fault tripping.

The out-of-step tripping feature can be used with ordinary high-speed relays for precision control of the line loading. The *CEX* ohm units can be connected in series with protective relays and set to trip when the power flowing through the line exceeds the stability limit.

Field Disturbances Produced by Lightning

Discussion and authors' closure of paper 43-46 by G. D. McCann and D. E. Morgan, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, pages 345–57.

E. A. Evans (General Electric Company, Pittsfield, Mass.): In Figure 2 of the paper several typical ground-gradient records obtained by Wilson[1] are shown. The concept of an exponential increase in the electrical field at the ground as a thundercloud becomes charged, followed by a sudden decrease in the field when a stroke occurs, as shown in some of the illustrations, is familiar to most lightning-minded engineers. The fact that the field frequently increases in magnitude substantially at the instant of a discharge and then slowly decreases over a period of seconds, as shown in Figure 2b, right chart, of the paper is not so familiar.

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Figure 1. Lightning striking in foothills of Colorado Rockies

I first experienced this type of field change several years ago while inspecting a United States Forest Service lookout on Elden Mountain near Flagstaff, Ariz. A thunderstorm passed near the lookout causing corona from the sheet-iron roof. It was surprising to find that on a number of occasions the sound of the corona did not increase suddenly and then as suddenly cease when a stroke occurred, followed by a gradual increase in strength as the cloud charged up again. Instead, it increased strongly at the instant of a stroke and gradually decreased in strength over a period of several seconds until another stroke occurred. The sequence was then repeated.

Later that year during extensive observations on lightning at Devils' Head Lookout, Colo., the previously described sequence of field changes was indicated frequently by the sound of corona from the protective system, railings, and flag pole. Sound observations were confirmed by measurement of the discharge current from a fine wire.

This type of field increase should be expected not only for the condition of Figure 2b right, of the paper, but also at the ground beneath or near any region of charge concentration which does not discharge when a stroke occurs from a nearby concentration of charge of opposite polarity. This condition can be expected fairly frequently, because there are usually several discharge centers[2] in one storm. Inspection of a thunderstorm from a distance will usually show several violent convection systems. Each of these should have at least the minimum number of regions of positive and negative charge concentrations shown in the idealized illustrations of the paper.

These sudden increases in field near centers of charge can set off discharges[3] from one or more of the centers. On numerous occasions in Colorado, Idaho, and Massachusetts, such discharges have been seen to occur nearly simultaneously from two or more centers at once. The chance that the charge in each center built up to a value necessary to start independently a stroke at the same instant is fairly remote.

As the authors indicate, cloud height has been considered a factor in determining the relative proportion of cloud-to-cloud and cloud-to-ground discharges. In a report on the observations at Devils' Head, I stated, "When a majority of discharges were cloud to cloud, the charge frequently appeared to be concentrated considerably higher within the cloud than was indicated in storms in which the discharges were predominately cloud to ground."

Observations have also indicated that, in many storms in which cloud-to-cloud discharges predominated, charge concentrations of opposite polarity occurred fairly close together. The distance between them depends on the characteristics of the convection system or systems causing the charge separations. These characteristics may vary over wide limits from storm to storm and at different periods in the life of the same storm. It appears probable that the proximity of charge concentrations of opposite sign within the cloud is a more frequent cause of cloud-to-cloud discharges than the favorable distance relationships brought about by increases in the height of the cloud base above the ground.

Concerning the distance from the ground terminal of a stroke at which substantial field changes can occur, evidence on this was obtained on the occasion previously mentioned from corona demonstrations and from photographs. On a number of occasions, strokes up to four miles from the lookout caused audible corona demonstrations. Since the elevation at Devils' Head is 9,348 feet, the gradient necessary to cause corona is only approximately 75 per cent of that at sea level. Based on the strong corona demonstrations there, however, it appears probable that corona discharges of somewhat lower magnitude may occur even at sea level for considerable distances from the ground terminal of the stroke. On one occasion, a stroke to ground occurred approximately one-half mile from the lightning observatory at Pittsfield (elevation about 1,100 feet) as I was climbing up to it. Sharp corona was heard. Normally, the noise of lightning-recording equipment would prevent corona being heard within the observatory.

That substantial field changes can occur at large distances from the ground terminal of some strokes is evident from Figure 1. The visible length of the discharge channel along the cloud base was between 2.5 and 3.5 miles. The height of the cloud base above ground was 5,600 feet plus or minus

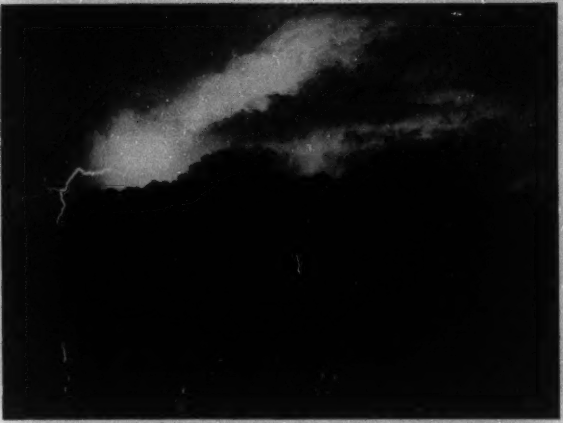

Figure 2. Lightning stroke from the upper regions of a thundercloud to ground

600 feet. (It was possible to determine the height of the cloud base more accurately than the length of the discharge along it, because the latter was not exactly at right angles to the line of sight of the camera.) It is very probable that substantial changes occurred in the electrical field at the ground beneath this stroke for distances somewhat greater than the length of the discharge channel. Figure 1 represents one complete discharge, which was probably multiple. Film was changed immediately before and after the discharge. Other photographs were obtained in Colorado and Idaho showing discharges along the cloud base which were of the same order of length.

In discussing the polarities of discharges, the authors mention the possibility of strokes from the upper positively charged regions of thunderclouds to earth. In Figure 2 is shown such a discharge which was obtained at Pittsfield, Mass. At the time, a very large storm cloud was passing approximately 20 miles north of Pittsfield, while a smaller cloud from which no discharges were seen was passing about seven miles north of the city. When this picture was obtained, a time exposure was being made to record some small discharges visible near the top of the larger cloud. Suddenly, the stroke shown occurred from the upper regions of the cloud to ground. To those who did not see the stroke, some doubt may arise as to its having gone to ground, because a portion of the stroke was hidden behind the nearer cloud. I was looking directly at the stroke and am absolutely certain that, with the exception of minor discharges within the cloud which illuminated it several times before the cloud-to-ground stroke, the entire discharge shown was one stroke and went to ground. This is the only picture of such a discharge of which I am aware. If our present concept of charge distribution in thunderclouds is correct, this stroke was positive.

REFERENCES

1. INVESTIGATIONS ON LIGHTNING DISCHARGES AND ON THE ELECTRIC FIELD OF THUNDERSTORMS, **C. T. R. Wilson.** Royal Society of London (England), *Philosophical Transactions*, volume A221, 1921, pages 73–115.

2. A STUDY OF THE MECHANISM OF THE LIGHTNING DISCHARGE, **E. A. Evans.** Stanford University *Bulletin*, fifth series, number 158, July 31, 1933, pages 37–41.

G. D. McCann and **D. E. Morgan:** It is encouraging to hear from E. A. Evans of his experiences with the various types of field changes reported in our paper, particularly the type consisting of an increase in magnitude, substantially at the instant of the discharge, and a subsequent slow decrease over a period of seconds. There is little doubt that this type of a field change may result from many conditions which arise in actual thunderclouds. As Mr. Evans points out, contrary to the ideal assumption, there are usually many charge generation centers. Of this type of field change one general conclusion may be drawn; the field change is always opposite in sign to the discharge which produces it.

It is obviously true that both cloud height and the distance between charge centers are factors regulating the proportion of strokes to ground and strokes between cloud-charge centers. Probably relations could be obtained between all three of these variables: that is, cloud height versus proportion of strokes to ground, distance between charge centers versus proportion of strokes to ground, and cloud height versus distance between charge centers. However, for statistical studies, distance between charge centers cannot be measured readily, and this leaves only the relation between cloud height and proportion of strokes to ground which can be actually related.

Mr. Evans states that his picture of a stroke from the top of a cloud to ground is the only one of which he is aware. Figure 3 shows a discharge of this type photographed by D. D. Clark of Kansas City. This was taken with a 16-millimeter motion-picture camera with the shutter and claw removed. The lightning stroke then constitutes its own shutter, and the uniform motion of the film imparts the time element. Because of the type of recording camera, the film is small, and the cloud definition is not clear. However, Mr. Clark based his statement that this was a stroke from the top of the cloud to ground on simultaneous visual observation as well as on the photograph as does Mr. Evans. It is interesting that this is a multiple-component stroke in which all the components follow either one or both of the two streamers extending from the cloud top, thus indicating that they are all of positive polarity. This is a current condition that is not known to have ever been recorded. However, the first component shows an additional horizontal streamer tapping charge from the lower portion of the cloud which may be of either polarity.

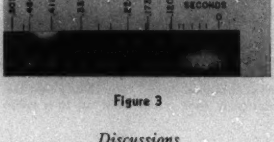

Figure 3

Generation of Electric Charges by Moving Rubber-Tired Vehicles

Discussion and authors' closure of paper 43-50 by S. S. Mackeown and Victor Wouk, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, volume 62, 1943, May section, pages 207–10.

Ernst Weber (Polytechnic Institute of Brooklyn, Brooklyn, N. Y.): It is not quite clear why the authors should consider that the potential difference created between car body and road pavement when the car is in motion should be entirely due to conduction current flowing through the resistance of the tires. The fact is that a potential difference must be created first before a conduction current can appear anywhere. In the rubber-tired vehicle this potential difference can be created only by redistribution of electric charges, and, since any known insulator has a finite though small conductivity, a certain "insulation" current will try to neutralize the potential difference which, however, is maintained by the continued friction of the tires.

The equivalent circuit of the car in motion as given by the authors in their Figure 5, is *not* complete; the complete simple equivalent circuit of a car moving on a highway must be as shown in Figure 1 of this discussion. The current I is the frictionally created convection current on the rear rubber tires on which the driving torque is exerted and where,

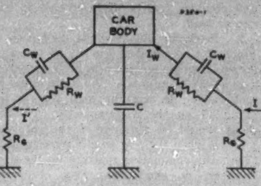

Figure 1

therefore, the maximum friction occurs in driving. If the notation of the authors is used, R_g denotes the surface resistance of two tires in parallel to ground and R_w the resistance of two tires in parallel to the wheels. C_w is the partial capacitance between two rubber tires and the wheels and car body, across which the electrostatic induction of potential takes place. C is finally the capacitance of the car body to ground (road pavement).

Now, the authors find currents I_w of different values under different conditions, which is as one would expect. But in order to judge the voltage that might be created between car body and pavement, it would be necessary, in the interest of safety, to consider the maximum values actually found. In this respect, certain inconsistencies occur which need explanation:

1. Assuming arbitrarily an injected current of $I=8\times10^{-6}$ ampere, the authors obtain a value of $(R_w+R_g)=1{,}325$ megohms from their curve, Figure 4, taken on the dynamometer stand. On the other hand, in their conclusion they quote a value of insulation resistance for vehicles with four tires in parallel, of 3×10^{9} ohms, so that one must conclude $(R_w+R_g)=6{,}000$ megohms for only two tires in parallel. With the measured current $I_w=1.74\times10^{-6}$ ampere at 40 miles per hour from Figure 4, this leads to a voltage

$$(R_w+R_g)I_w=10{,}440 \text{ volts}$$

as at least a possible if not a better value than 3,000 volts assumed by the authors. In fact, this is exactly the same value that R. Beach[1] found by direct measurement of voltage for normal inflation of the tires, as shown in his Figure 4.

2. Since in Figure 2 the authors plot current values which, by their own admission, are almost twice as large as the ones obtained in Figure 4, a further correction should be made, bringing the maximum possible voltage at 40 miles per hour car speed to $10{,}440\times3.3/1.74=19{,}800$ volts.

3. A certain amount of frictional charge must also be produced by the front tires, which will superimpose an additional current I' through the front wheel resistance R_g, thus increasing the potential difference to ground by a value of $I'R_g$. One might argue that part of the current might flow back over the path of I_w and reduce the potential difference, but, since I_w as measured already includes that effect, this would not be a serious argument.

4. Still larger values of current I_w were measured on the road as given in Table I of the paper, if one were to use Figure 2 and from there to extrapolate to 40 miles per hour car speed.

5. It follows, then, that the computations concerning minimum sparking potential and ignition of combustible mixtures are wholly inadequate.

REFERENCE

1. STATIC ELECTRICITY ON RUBBER-TIRED VEHICLES, Robin Beach. *Electrical Engineering*, volume 60, May 1941, pages 202–08.

Robin Beach (Polytechnic Institute of Brooklyn, Brooklyn, N. Y.): The approach to this subject of electrification of moving rubber-tired vehicles by the method of measuring the currents resulting from the voltages that are generated at the tire treads is unique and interesting, and the authors are to be congratulated for their contribution to this field of science.

Because of the high-voltage electrification of moving rubber-tired vehicles, they have long been considered potential sources of hazards to safety. What hazards there are would presumably fall under two general classifications:

1. Shocks to people.
2. Destruction of property.

The latter would result from fires and explosions caused by the ignition of inflammable mixtures of air with gases or vapors. If these potential hazards to life and property were not present, obviously there would be no problem. Clearly the fundamental factors which influence the degree of hazards created by an electrified vehicle are:

1. The magnitude of the potential of the car body above ground.
2. The capacitance of the car body with respect to ground, since this together with the voltage determines the quantity of the stored electric charge.
3. The duration of the electrification on the vehicle after it comes to a stop.
4. The properties of jump-spark discharges evaluated in terms of threshold energy and voltage to cause the ignition of inflammable products.

The paper under discussion attempts to prove, by the analysis of an assumed equivalent circuit diagram that

1. Voltages higher than 3,000 or 4,000 volts cannot be generated on car bodies.
2. Electrification cannot remain on a car body more than a few seconds after the vehicle comes to a stop.
3. Inflammable products, such as combustible mixtures of air and vapors, cannot be ignited by the sparks from a charged vehicle.
4. The voltage of the car body to ground is practically eliminated by the use of so-called conductive-rubber tires.

Let us consider in sequence each of these four claims.

1. From innumerable voltage tests, taken both on the highway and on chassis dynamometers, with approximately 200 vehicles, including pleasure cars of various sizes, types, and weights, panel-body trucks, and dump trucks, largely from the automotive fleet of the Brooklyn Edison Company, the writer has commonly measured voltages of the order of 10,000 to 15,000 volts from car body to ground. On one road test a value of 20,000 volts was measured on a lightweight pleasure vehicle operating on an asphalt highway at about 45 miles per hour.

Little difference in voltage readings was discovered, whether the vehicles were operated on dynamometers or on the roadways. In the latter tests, one terminal of the electrostatic voltmeter was connected permanently to the car body, while the other terminal, insulated from the car body, was grounded by being connected to a chain which was dragged in car tracks. In other instances, while the same vehicle was being used on the same highways during the same periods of test, the chain was dragged on the roadway surface rather than in car tracks. In fact, careful experiments were conducted to establish the equivalence of voltage tests on highways with voltage tests on the dynamometer, as it was recognized from the start that conditions as found on highway operation were the criteria of comparison, and that dynamometer testing could be used, as a convenience, only provided the results closely approximated those of the highway tests. This was found to be true.

Many repetitious voltage tests proved conclusively that the magnitude of the voltage of the vehicle to ground was a direct function of the per-unit loading of the tires, increasing markedly with increases of tire inflation for a given speed or, at a given tire inflation, with increase of car load, as for instance with large increases of loads on a dump truck. In each case, the number of pounds per square inch of tire surface in contact with the roadway was increased. Patterns of these contacting tire areas were recorded by carbon imprints and planimetered, and wheel weights were measured, from which data the unit tire loads were calculated and thereafter related with the resulting increases of voltage for a given speed. The relationship thus found gave strong support to the validity of the theory of "contact difference of potential" as the principle underlying voltage generation at tire treads.

2. The authors state that the charge on an electrified vehicle will be dissipated in a few seconds after the vehicle comes to a stop. On one of the most humid days in August, 1941, the writer was called to the Socony Company bulk filling station in Brooklyn to test the electrification of a particular 2,000-gallon gasoline-tank truck. After a 100-yard run around the filling racks in the plant, the truck was brought to a stop and the voltmeter terminal quickly connected to the truck, the other terminal having been previously grounded. The voltage was found to be 3,200 volts. This voltage gradually decreased to about 500 volts during a period of two minutes. The run was then repeated with a conductor dragging on the ground from the chassis. About the same initial voltage was measured, and the same time within five seconds was required for its dissipation.

Similar tests have been made repeatedly during more favorable periods for the production of high electrifications, and in many instances of a highly charged vehicle at standstill, only a few per cent decrease in voltage was experienced after periods as long as 20 to 30 minutes, and in some of these tests skid chains had been draped over the rear bumpers so as to drag on the roadway. From such tests clear evidence was provided as to the futility of the use of drag chains on gasoline-tank trucks as a means of keeping them discharged. Also instances are known where mechanics have been thrown to the ground by shocks received from touching parked gasoline trucks, even as long as two hours after the electrified trucks were parked near the filling racks. Two cases are known to the writer, one here in New York City and the other near Boston, where shocks of such severity were received by two men while boarding busses, that their deaths ensued shortly afterwards.

A few people, who have obviously had no experience in this field of investigation, have advanced the explanation for the existence of the high voltages on rubber-tired vehicles, both while operating and at standstill, as the effect of the electric-field gradient at the earth's surface which exists normally at a low voltage, although sometimes at high values as the result of induction from nearby charged clouds. It is known that fair-weather ground field gradients reach a maximum value of about 200 volts per vertical foot, which could lead to the possible existence of about 1,000 volts to ground for passenger cars. On city streets, however, sheltered electrostatically as they are by adjacent steel-framed buildings, even this low voltage could not exist. If a high ground gradient from a charged cloud existed on an electrostatically unsheltered highway, obviously this effect of ground gradients could not account for the almost linear variation of the generated voltage from car body to ground, on a road test starting from zero voltage at zero speed to the high voltages of 10,000 to 15,000 volts at high speeds. If at standstill, a high voltage from the car body to ground, as measured by an electrostatic voltmeter, was caused by a high electrostatic ground gradient, then this voltage would reestablish itself again directly after the car body was grounded. Innumerable tests by the writer have disproved that the high electrification remaining on cars which have come to standstill results from the effect of ground gradients. While it is conceivable that, in an isolated instance, an electrified car body could discharge at standstill under the influence of a high ground gradient—a condition which can be determined readily—yet it is most improbable that this effect could occur repeatedly in road tests which were taken on different days and particularly under fair weather conditions.

3. In the conclusion of the paper under discussion, the authors state that a condenser of 750 micromicrofarads had to be charged to several thousand volts to produce a spark capable of igniting a combustible mixture of gasoline vapor and air. In Circular C-438 of the National Bureau of Standards, "Static Electricity", by F. B. Silsbee, the statement is made that the jump-spark from a capacitor of 72 micromicrofarads charged to 5,250 volts, thereby delivering an energy of only 0.001 joule, produced the ignition of an inflammable mixture of gasoline vapor and air.

Obviously rubber-tired vehicles, which may range in capacitance from below 500 to over 1,500 micromicrofarads, and which may be commonly charged at the time of coming to standstill to voltages in the range of 10,000 to 15,000, constitute potential and dangerous sources of ignition of inflammable vapor mixtures and gases. In such cases all that is required for the initiation of a fire or an explosion is the synchronized timing of the spark, the presence of the proper ingredients for an explosive mixture, and their correct proportioning within the limits of combustibility. Doctor Silsbee also states that hydrogen has been ignited by as little as 2×10^{-5} joule at 1,300 volts.

4. The writer has had no opportunity to test conductive rubber tires, and hence he can express no experimentally supported statement. But since the resistance of concrete pavement appears, from what measurements have been made, to be, at least, two or possibly three orders of magnitude higher than the resistance of the conventional rubber tires as were measured by the authors, the writer would expect little success with conductive tires as a means of reducing the voltage generated on moving vehicles. Even the authors' own equivalent-circuit diagram of Figure 5 would support this expectation. If the resistance through the pavement to ground is, say, two orders of magnitude larger than the series resistance R_g, then the value of R_g, whether it be the value, 3×10^8, as given by the authors, or even approximately zero, could only affect the high resistance of this circuit, as shown in Figure 5, to the extent of about one per cent. Hence, whether the rubber tires were conductive or otherwise would not be expected to have any appreciable effect in reducing the voltage generated on moving vehicles.

The solution of this problem of the generation of voltage on rubber-tired vehicles is retarded by the property of extremely high resistivities of roadbeds, as they are constructed at present—using concrete and asphalt compounds. Also the contact resistance between the tires and the roadway has never been investigated and this may be of a very high order. So in the absence of a solution to the problem, the best that can be done under the circumstances is to recognize the probable presence of a hazardous condition of high voltage and relatively large storage of electric charge on the vehicle, and to take the necessary precautions against possible shocks, fires, and explosions from the vehicles after they come to standstill.

S. S. Mackeown and **Victor Wouk:** The discussions by Professor Beach and Professor Weber call for certain comments.

We too have measured the voltages developed by a car mounted on a dynamometer. Our results showed, however, that the determination of the currents generated was more fundamental and gave more consistent results. The potential that a car acquires is determined not only by the current produced at the contact of the tires and the road, but also by the resistance of the tires. The resistance of the tires varies greatly from time to time, and it is difficult to obtain results consistent enough to make any general deductions. Our measured potentials check approximately with those determined by measuring the current.

The following comments refer to the numbered paragraphs in Professor Beach's discussion:

1. The potentials we measured and calculated for an automobile running on a dynamometer are considerably lower than those measured by Professor Beach. We know of no explanation for this difference except that our work using the dynamometer was done in the evening when the humidity may have been relatively high, and consequently the resistance of the tires was lower than in the daytime. We found a small increase in the current generated when the pressure in the tires was increased. This compares with Professor Beach's results which show marked increase in the measured voltage with increase of tire inflation. The reason for this difference may be that our results are relatively independent of the resistance of the tires, whereas the measured potential will depend on this resistance. As the pressure in the tires is increased, there is less heating of the tire, and, consequently, the resistance of the tire will increase with increase of tire inflation. We agree with Professor Beach that the current generated is due to contact differences of potential, but we do not find that this current changes greatly with increase in friction or load.

2. Professor Beach finds instances in which the potential of the car at standstill decreases only a few per cent after periods as long as 20–30 minutes. We have found no such cases. In working with the car not grounded on the dynamometer, a severe shock would be obtained if the car were touched when the rear wheels were turning. (With the conducting mixture on the tires, however, no shock was ever obtained). However, with the normal tires, in a few seconds after the wheels were stopped, no shock was felt on touching the car. A voltmeter connected to the car showed a steady decrease in potential of the car, as the speed of the wheels was decreased, and showed substantially no potential difference between the body of the car and ground a few seconds after the wheels stopped turning.

Common experience indicates that a car or a bus brought to a standstill does not continue to be at a high potential. It is very seldom that a person boarding a car or bus receives a shock, even though the bus or car has been driven at a good rate of speed and is boarded immediately upon stopping. A calculation shows that, in order that a car decrease its potential only a few per cent in 20–30 minutes, it is required that the car be insulated by a resistance greater than 4×10^{12} ohms. This is a resistance which is a thousand times greater than what we find generally exists between a rubber-tired vehicle and ground.

3. We are at present engaged in a study of the characteristics of a spark necessary to cause ignition of a combustible mixture of gasoline vapor and air. It is simple to show in the laboratory that, with a small spark gap, sparks of relatively high current will not ignite a combustible mixture.

4. We agree with Professor Beach that the resistance of the road is very important in determining the potential acquired by a car. There is one point, however, which should be remembered: a moving automobile is continually contacting fresh surface of the road, and, even if the road be a complete insulator, the car will share its charge with the road. This will have the same effect as if the road had a lower resistance. Furthermore, it is possible that the positive charge "wake" left on the road behind the front wheels neutralizes to some extent the negative charge on the rear wheels, and thus enhances the effect just mentioned.

The following comments refer to the paragraphs in Professor Weber's discussion:

1. When a vehicle is in motion, the charge is distributed over the major portion of the circumference of the tires. The potential of this charge is not constant, but increases as the tire leaves the pavement, and probably reaches a maximum before the tire has turned 180 degrees. The equivalent resistances R_w and R_g are average resistances. They are very much less than the measured resistance between the car and ground when the car is stationary, because of the much broader surface over which the charges are distributed. These resistances are effective when the car is moving. When the car is stationary, it is necessary to use the measured resistance between the tires and ground.

We believe that it is proper to determine the voltage of a moving car by using the equivalent resistance, as was done in the paper.

2. We agree that at times a car may reach a higher potential than the figures given in the paper. These figures were not for the maximum potential but for cases which we consider typical.

3. We are of the opinion that, when a car is moving, the front tires will generate a charge and have a resistance similar to that of the rear tires. The charge produced will be doubled, and the resistance decreased to one half on this assumption. Therefore, the potential determined when the rear wheels are the only ones moving will be the same as the potentials generated when all four tires are running. It should be remembered that the current to the car was measured when the car was grounded through a microammeter of relatively low resistance. The resistance of the front tires was so great compared to the resistance of the microammeter that the front tires could be considered as perfect insulators.

4. We believe that our results indicate the potentials that a car acquires in normal operation. The fact that when a car is stopped there is normally not enough potential to cause a shock to an individual, confirms our opinion that potential is too low to cause a spark which will ignite a combustible mixture of gasoline vapor and air.

An Improved Axial Air-Blast Interrupter for Severe Operating Duty

Discussion and authors' closure of paper 43-32 by P. L. Taylor and H. W. Martin, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, pages 323–33.

R. C. Dickinson (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Apropos of the opening paragraph of this paper, a single-pole De-ion air breaker of the metal plate type for 12-kv railway service was tested 11 years ago in the Westinghouse high-power laboratory for endurance. A total of 72 interruptions was made without any maintenance whatever. Of these 72 tests, 53 were made consecutively in a total time of 1 hour and 44 minutes. The current averaged 18,000 amperes, and the circuit voltage was 11 kv, 25 cycles. In the remaining 19 tests, three interruptions averaged 47,000 amperes at 25 cycles, and the remainder were at currents down to 2,700 amperes. At the end of this test the breaker was still in operating condition. This test represented a phenomenal performance at that time and is still considered very good.

Referring to the paper, "Magnetic De-ion Air Breaker for 2,500–5,000 Volts," by L. R. Ludwig and R. H. Nau,[1] reference is made to 27 interruptions at from 22,000 to 55,000 amperes at 1,900 volts single-phase and 76 interruptions at 14,000 amperes and below. These tests were made on the same arc chute and contacts.

I would also like to discuss certain other statements in this paper which, when read superficially, may be misleading. Referring to Figure 12 which purports to show the closing 115,000 peak amperes by the breaker in question, our figures indicate that the actual current on this test was approximately 57,000 amperes rms which is the commonly accepted way of expressing such currents. In comparison with this, the magnetic De-ion breaker described by Ludwig and Nau has closed successfully 64,000 rms amperes on a close–open interruption at 2,000 volts.

In the fourth paragraph of Taylor and Martin's paper reference is made to "the interrupting of currents of the order of 60,000 rms amperes in the 500,000-kva 15-kv size." From the paper referred to, "Circuit Interruption by Air Blast," by Edsall and Stubbs,[2] the maximum current shown to be interrupted by their 500,000-kva 15-kv air-blast breaker is approximately 53,000 amperes *at 2,300 volts*, with the assistance of a resistor across the contacts.

Referring to remarks by Taylor and Martin under "Conclusions" and reference 14, which is a paper entitled "A New 15-kv Pneumatic Circuit Interrupter," by Ludwig, Rawlins, and Baker,[3] I assume that the following statement is the one in question:

> "For the lower ranges of current and at system capacities where the voltage-recovery rate after arc extinction is not too great, this type of interrupter is quite satisfactory. As the current or voltage is increased, the air requirements mount, until at 1,000,000 kva, pressures in excess of 200 pounds per square inch are required. The nozzle type of interrupter is therefore not appealing."

First, it is to be noted that the paper from which the preceding quotation is taken was written for breakers of three times the voltage rating and of ten times the kilovolt-ampere interrupting capacity as the breaker described by Taylor and Martin. It is to be presumed that the high interrupted currents given by them were at considerably less than the maximum rated voltage of the breaker, five kilovolts. The actual voltage on these tests is not stated by the authors, although numerous other tests are shown in the order of 40,000 amperes and 1,800 volts.

It is to be noted that a resistor is used in this breaker to reduce the rate of rise of recovery voltage, although the voltage rating is very moderate.

In view of these factors, the statement quoted from Ludwig, Rawlins, and Baker still appears valid and not at variance with the performance of the breaker shown by Taylor and Martin.

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1. MAGNETIC "DE-ION" AIR BREAKER FOR 2,500–5,000 VOLTS, L. R. Ludwig, R. H. Nau. AIEE TRANSACTIONS, volume 59, 1940, September section, pages 518–22.

2. CIRCUIT INTERRUPTION BY AIR BLAST, W. S. Edsall, S. R. Stubbs. AIEE TRANSACTIONS, volume 50, 1940, September section, pages 503–08.

3. A NEW 15-KV PNEUMATIC CIRCUIT INTERRUPTER, L. R. Ludwig, H. L. Rawlins, B. P. Baker. AIEE TRANSACTIONS, volume 59, 1940, September section, pages 528–33.

L. J. Linde (General Electric Company, Philadelphia, Pa.): This paper presents, in a very interesting form, the development of an air-blast breaker for highly repetitive duty which can be used with metal-clad equipments. The evidence presented to demonstrate the abilities of this breaker to handle high currents during a large number of operations is impressive and to be expected for any properly designed air-blast breaker.

In 1940 a paper was presented to the Institute[1] in which an air-blast breaker of comparable dimensions was described in some detail. This circuit breaker was rated at 250,000 kva for services up to 15 kv (see Figure 1 of this discussion), and test data were presented to support this rating at voltages of from 4,200 to 14.5 kv. Approximately 30 such breakers were installed on utility systems during that year, and they have established a reputation for reliable service with a minimum of maintenance.

Those breakers were designed and built at a time when there was some question as to whether a line of self-contained magnetic-type breakers could be built to cover adequately this moderate capacity field. Since that time the Magne-Blast breaker line has been extended to include ratings from 50,000 to 250,000 kva up to 15 kv, and nearly 3,000 such breakers are now in service on a wide range of application duty. Since a majority of breakers of these ratings are used as removable units with vertical-lift metal-clad switchgear, simplicity of control, simplicity of provisions for breaker removal, and freedom from external auxiliary operating power supply, if consistent with satisfactory operating performance, are major considerations.

One of the principal difficulties encountered in the design of such a self-contained magnetic breaker is to get efficient light-current interrupting performance in a breaker which must also handle very heavy currents. In the Magne-Blast breaker, this is accomplished by providing a small

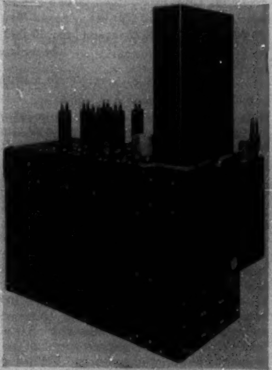


Figure 1. A removable air-blast circuit breaker rated at 250,000 kva, 1,200 amperes, 15,000 volts

Breakers of this design are being used in vertical-lift metal-enclosed gear on 4,200-volt service

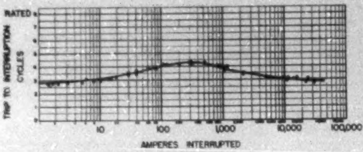


Figure 2. A series of interruption tests on a 250,000-kva Magne-Blast circuit breaker to show the breaker operating time in cycles for currents up to breaker rating

These tests were made at from 4,200 to 7,200 volts three-phase

piston, self-operated as the contacts open, which directs a gentle puff of air in such a way that it moves a stringy light-current arc out into the arc chute where it is cleared promptly. Graphic evidence of the effectiveness of this puff of air is presented in Figure 2 of this discussion.

The air-blast breaker is recognized as being admirably suited for high-capacity station-type duty, as evidenced by the number which have been built and placed in service with ratings up to 5,000 amperes, 2,500,000 kva at 15 kv. From an interrupting standpoint, the air-blast breaker is also well adapted to highly repetitive duty, but it should not be inferred that it is alone in this field. Ability to handle a large number of operations at values of current approaching full interrupting rating is of scientific interest and, in some degree, is required in all circuit breakers. However, from an average application standpoint, and particularly for these moderate capacity ratings, it is far more important to know how many times a breaker will operate under conditions which it will encounter in its everyday service. Since no load or light-current operations usually form a large part of such service, mechanical simplicity, ruggedness, and freedom from unnecessary complex accessories are important factors.

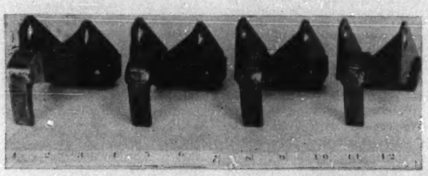


Figure 3. An unretouched picture comparing a new arcing contact with the three contacts removed from a 150,000-kva Magne-Blast breaker that had interrupted from 10,000 to 20,000 amperes 100 times and from 20,000 to 30,000 amperes 50 times without maintenance or replacement of parts

These tests were made at from 3,000 to 4,200 volts three-phase

Some years ago a Magne-Blast breaker was put on a repetitive operation test and completed 10,000 operations, interrupting between 1,200 and 2,000 amperes at 2,300 volts three-phase. During this time no maintenance work was performed on the breaker, and the arc chutes were not removed to inspect the contacts. Following this test and without any changes, the breaker successfully passed a number of full-capacity interrupting tests in excess of its rating and a standard high-potential test.

A breaker withdrawn from stock was tested recently for repetitive duty at higher currents. These tests included 100 operations at currents above 10,000 amperes at 4,200 volts, three-phase, and 50 operations at above 20,000 amperes. All of these were made with the same set of contacts and arc chutes without maintenance of any part of this Magne-Blast breaker (Figure 3 of this discussion). As evidenced on this picture, the contacts were in condition to continue operations.

REFERENCE

1. MEDIUM-CAPACITY AIR-BLAST CIRCUIT BREAKERS FOR METAL-CLAD SWITCHGEAR, R. M. Bennett, B. W. Wyman. AIEE TRANSACTIONS, volume 60, 1941, pages 333-8.

J. Elmer Housley (Aluminum Company of America, Alcoa, Tenn.): The authors have described an improved method of interrupting an electric circuit. It is desirable that we have such information representing an advance in the state of the art. The writer has observed closely the operation of a similar air-blast circuit breaker installed in a Southern hydroelectric plant in 1938, followed by the installation of four similar breakers of a later design in 1940, one of which replaced the original experimental breaker. Information developed in field tests and through field experience has, of course, been made available to the manufacturer covering such equipment.

The air-blast breaker is in competition with a highly developed line of oil circuit breakers and oil-poor type of breaker. In general the demand for the development of air-blast circuit breakers has been insistent. The speed of these breakers is rather high, and the reduction of the hazard of oil in small indoor rooms and the reduction of d-c operating devices have been some of the points of interest to operators of the equipment.

Interest in the use of air for operation of breakers and in the interruption of electric circuits has been apparent on the American scene for many years. The engineer has a global perspective, however, and he has watched with interest the development of all types of circuit-interrupting devices throughout the world and has been able to evaluate the salient features of these devices. From the knowledge thus obtained there have sprung many improvements, such as accessibility of parts, improvement in the maintenance problem, and an increase in the number of operations between inspections.

E. W. Boehne (General Electric Company, Philadelphia, Pa.): This is a discussion of Appendix B of the paper, entitled "Transient Recovery Voltage Control With Axial Air-Blast Breakers," by Karl Lerstrup. Doctor Lerstrup invites us to accept the first derivative or slope of the recovery voltage as a measure of the stress on the arc gap following interruption. Even if we make the concession of a single-frequency recovery circuit, which is not stated, the slope of the recovery voltage is not an acceptable measure of the rate of rise of recovery voltage. Doctor Lerstrup steps out of this false premise with the statement which starts paragraph 5, namely: "The rate of rise of electrical stress on the gap, immediately after a current-zero value, characterized by the derivative de_o/dt, is a system constant." This is effectively true only at the point of the maximum rate of rise and in 1935[1] was termed the recovery impedance, Z_r, of the circuit and is the ohmic contribution of the circuit to the simple but generalized expression for the rate of rise of the voltage across any circuit, that is,

$$r \cdot r = k \cdot I \cdot \omega \cdot Z_r \text{ volts per microsecond}$$

$$\omega = 2\pi$$

It was further shown in 1935 that, when a resistor shunted the breaker contacts, which ohmically was relatively small in comparison with the recovery impedance, the resistor became the recovery impedance in the preceding expression. Making this substitution:

$$r \cdot r = k \cdot I \cdot \omega \cdot R \text{ volts per microsecond}$$

But $I = E/x$; hence the reduction to

$$r \cdot r = k \cdot E \cdot \left(\frac{R}{x}\right) \cdot \omega \text{ volts per microsecond}$$

This is the conclusion Doctor Lerstrup reaches afresh without the qualifying limitation regarding magnitude of the resistor.

Although it is stated, if R is small enough gap 1 of his Figure 18 will not break down, he points out in Figure 20 that the ratio R/x used in actual performance is 20 to 30. Even if the maximum value of 30 is taken for a 2,300-volt breaker as described in the paper for 150,000 kva, the resistor will be about one ohm and must be able to pass a current of about 1,300 amperes and also be so designed as to withstand the mechanical stress of this current while heated. When confined to a small space, the insulation clearance becomes a problem.

From a functional standpoint it can be shown that any fixed resistance entering the circuit at current zero (and it must enter at a current zero) can only approach a 90-degree phase-angle displacement with the final interruption as the resistor is increased without limit. The Magne-Blast breaker of 150,000 kva is a self-contained breaker, that is, it requires no compressed air. The resistance characteristic developed by the arc itself, in the unique interleaving arc chute, modifies the recovery voltage by phase-angle shifts which exceed 90 degrees by as much as 30 or 40 degrees.[2] No resistors are required. The controlled arc is the resistor, a resistor which is never seen, and with each interruption it disappears promptly with no shower of molten metal. The Magne-Blast-breaker performance is independent of the severity of the recovery circuit. This principle of rapid efficient interruption is available for voltages up to 15 kv.

REFERENCES

1. DETERMINATION OF CIRCUIT RECOVERY RATES, E. W. Boehne. AIEE TRANSACTIONS, volume 54, 1935, May section, pages 530–9.

2. THE GEOMETRY OF ARC INTERRUPTION, E. W. Boehne. AIEE TRANSACTIONS, volume 60, 1941, page 524.

P. L. Taylor, H. W. Martin, and Karl Lerstrup: The introduction to this paper refers to "the evident need for breakers capable of scores of high-current interruptions between maintenance periods, which are at the same time easy and economical to maintain."

An improved axial air-blast interrupting device, which meets those specifications, has been presented. This improved device was developed initially in the size and arrangement which impose the most stringent design limitations.

Comprehensive test data support the conclusion that this device is capable of considerably greater integrated duty between maintenance periods than is any other device with which we are familiar.

The presentation of performance data on this improved axial air-blast interrupter for severe operating duty appears to have stimulated the interest of other investigators in the important breaker characteristic which we have called "the integrated duty capacity," and it has provoked some very interesting discussion. Various investigators have searched their records and have reported some previously unpublished test data on the integrated duty capacity of other interrupting devices, thus evidencing their agreement that greater integrated duty capacities in circuit breakers are desirable.

Mr. Dickinson reports in his discussion a series of tests on a 12-kv single-pole metal-plate De-ion breaker. We understand that this was a special 1,000,000-kva single-pole railway trolley breaker. Unfortunately, he does not state at what voltage the only three high-current tests were made. In spite of the fact that this was a considerably larger breaker than the one described in our paper, the integrated duty to which it was subjected on the test reported seems to be somewhat less than that shown in our Table V on a 150,000-kva axial air-blast breaker.

Mr. Dickinson refers to a paper by Ludwig and Nau on the magnetic De-ion breaker in which he states ". . . reference is made to 27 interruptions at from 22,000 to 55,000 amperes at 1,900 volts single-phase and 76 interruptions at 14,000 amperes and below. These tests were made on the same arc chute and contacts."

With reference to these tests, the Ludwig and Nau paper says: "Figure 10 shows a photograph looking up into an arc chamber which has been used during 27 interruptions at the maximum short-circuit current of the laboratory, and in addition 76 interruptions of currents 14,000 amperes and below at 2,300 volts single phase . . . the 76 interruptions of currents ranging from 9 to 14,000 amperes rms were made on an arc chamber. . . ."

The two statements of currents and duty are at variance, and the Ludwig and Nau paper does not indicate that the same contacts were used throughout. Combining the two statements as best possible, and referring to Figure 11 of the Ludwig and Nau paper, we conclude that there were 24 interruptions at 22,000 to 41,000 amperes, 3 interruptions at 45,000 to 55,000 amperes, and 76 interruptions at 9 to 14,000 amperes. No statement is made as to whether the contacts were suitable for further use after these tests. Arcing times up to 20 cycles at low currents were reported on the low-current tests. Mr. Dickinson offers this test series for comparison with our test series consisting of 49 operations at 20,000 to 41,000 amperes and 51 tests at 10,000 to 20,000 amperes. Since contact deterioration is proportional to a power appreciably greater than one of the current interrupted, the test series on the axial air-blast breaker appears to represent considerably greater integrated duty.

Mr. Dickinson suggests that our indication of maximum current closed as 115,000 *peak* amperes may be misleading. The American Standard Definitions of Electrical Terms, *C* 42, reference 05.05.265, shows "peak value" to be an acceptable alternate for "crest value," which latter term was used in the Ludwig and Nau paper, to which Mr. Dickinson refers, in which they show a maximum "crest value" of making current on their tests of 89,000 crest amperes. We are unable to find any reference in the Ludwig and Nau paper to a making current of 64,000 rms amperes, but we wish to point out that the rms value of the making current on the test shown in our Figure 10 is 67,000 amperes, not 57,000 amperes as calculated by Mr. Dickinson. The oscillogram to which Mr. Dickinson takes exception was only included, because one of the reviewers questioned the ability of the breaker to close on fault current. The isolator contacts used on this breaker are of the same general design that has been used successfully for several years on the breaker described in a paper by Edsall and Stubbs.

Mr. Dickinson refers to a statement in our conclusion which reads as follows: "This is contrary to the findings published by other investigators[1,14] who found the current-interrupting "ceiling" of the axial (radial- or nozzle-type) air-blast breaker to be too low for use on American systems." We had more specific reference to the paper, "The Cross-Air-Blast Breaker," by Prince, Henley, and Rankin[1] than to the Ludwig, Rawlins, and Baker paper from which Mr. Dickinson quotes. However, we quote more completely from Ludwig, Rawlins, and Baker. "In Europe variations of the nozzle type . . . For the lower ranges of current and at system capacities where the voltage recovery rate after arc extinction is not too great, this type of interrupter is quite satisfactory. As the current or voltage is increased, the air requirements mount, until at 1,000,000 kva pressures in excess of 200 pounds per square inch are required. The nozzle type of interrupter is, therefore, not appealing."

We have shown in our paper that neither the axial air-blast nor the cross-blast type of air-blast breaker is peculiarly suited or limited to European practice. The company with which Mr. Dickinson is connected is using the axial-blast principle in certain of their high-interrupting-capacity air-blast breakers. They are also using air pressures in excess of 200 pounds per square inch on breakers using the cross-blast principle, which does not in our opinion, make them "unappealing," as was thought by Ludwig, Rawlins, and Baker. Whereas a resistor is used on our breaker to reduce the rate of rise of recovery voltage, this resistor is not required for proper operation. Actually, a number of the high-current tests reported was made without benefit of resistor.

In view of these factors and the evidence previously presented, it appears that the axial-type air-blast breaker is an American device developed to meet American standards, and that its performance at least equals that of the cross-blast type in all respects, and can no longer be indicted by the proponents of the cross-blast type as being a European device suitable only for European application.

Mr. Linde has made the following very interesting statement, which may, however, be misleading: "Ability to handle a large number of operations at values of current approaching full interrupting rating is of scientific interest and, in some degree, is required in all circuit breakers. However, from an average application standpoint, and particularly for these moderate capacity ratings, it is far more important to know how many times a breaker will operate under conditions which it will encounter in its everyday service."

No one can differ with the statement that it is very important to know the capabilities of a breaker under everyday service conditions. We cannot agree, however, that the ability of a breaker to handle large numbers of operations at high currents is a matter of little more than scientific interest. If this latter premise were valid, it might be permissible, without further tests, to assign an interrupting rating of say 80,000 amperes to the breaker described in our paper.

The characteristics of both air-blast and air-magnetic breakers are such that a breaker capable of large number of interruptions, at, say, one-half rating is not necessarily capable of even one interruption at or near rating. In breakers of all types the relative increase in deterioration of contacts with increased current is very rapid. Consequently, an interrupting device, such as is described in our paper, which is capable of interrupting large numbers of high-current faults is capable of interrupting very much larger numbers of medium-current faults and on many thousands of load-current operations. The determination of integrated duty capacity by means of high-current tests is thus very much more practical, particularly under present conditions. At the same time, the demonstration of a high integrated duty capacity on high currents insures that, when breakers are installed where they will be subjected to frequent operation at high currents, they will give real performance.

We appreciate Mr. Linde's contribution to the consideration of the integrated duty capacity of breakers by making special tests on the Bennett and Wyman breaker. It is unfortunate that he was unable to make any tests at or near the maximum rated interrupting current of the breaker. Our experience in testing magnetic breakers indicates that, in general, they are incapable of even a moderate number of interruptions at currents of the order of 30,000 amperes. Perhaps other investigators have had more fortunate experience.

We fully agree with Mr. Boehne's remarks about the voltage conditions around zero current. In order to keep the paper within readable length and quickly get to the main subject, we used the symbol de_o/dt as a characteristic representative for the rate of rise of recovery voltage. The qualifying limitation regarding the magnitude of the resistor is realized and stated, although not in Mr. Boehne's words.

We are pleased that Mr. Boehne brought up the question of the physical limitations of the resistor. Size has been an almost insurmountable difficulty in all applications to resistors to circuit interrupters, with the single exception of the axial air-blast breaker, which is the subject of this paper.

It is generally agreed that, in order to be effective, switching resistors must be of relatively low ohmic value and thus carry relatively high currents, and that the resistors must be designed to withstand these currents during the normal insertion period plus a generous factor of safety. In oil breakers these requirements lead either to enormous dimensions or to questionable safety factors. We recall some breakers built in Switzerland which had their resistors mounted in a separate tank. In another case a utility in Sweden elected to remove the resistors in their breakers rather than risk operation with them.

In the case of our axial-blast air breaker the conditions for the application of resistors are much more favorable. It must be borne in mind that in this breaker the arcing time does not exceed one current loop and the resistor is not required to carry appreciable current for more than one-quarter cycle. This is the reason why we can conveniently design our resistors at 20 to 30 times the minimum short-circuit reactance (at rated voltage) and why we can provide resistors that are at the same time effective and entirely safe without great expense in space or material.

Mr. Boehne has used the R/X ratio of 20 to 30 given in our paper as applying to the minimum voltage application and has, therefore, arrived at an incorrect value of resistance. The resistors in this particular case are required only to carry a maximum current of up to 700 amperes for periods up to one-quarter cycle. Thousands of tests on various breakers and several years of operating experience on breakers of the class described in the Edsall and Stubbs paper "Circuit Interruption by Air Blast," have proved the adequacy of our resistor design. Mr. Boehne's fears that our resistors may disappear in "a shower of molten metal" seem, therefore, to be unfounded.

Further, as mentioned elsewhere in this discussion, the breaker described has been subjected to numerous tests without benefit of resistors. Resistors are not essential to the operation of this breaker, but they increase the efficiency of operation, add to the safety factor, and reduce the voltage stress on connected apparatus.

We cannot subscribe to the unqualified use of expressions like "phase-angle displacement" when transient conditions are being considered. We must assume in this case that the zero passage of current is taken as a criterion. If so, we agree that with a fixed resistor we can only approach a 90-degree displacement, but who will want to obtain more? As soon as we get sufficient displacement so that the situation is well in hand, any further displacement is a matter of scientific interest only.

Mr. Boehne refers to the Magne-Blast breaker and his paper, "The Geometry of Arc Interruption." This paper is undoubtedly one of the more important papers on circuit interruption and offers an excellent treatment of a rather difficult subject. It shows how, with a variable resistor, the current can be forced to zero at almost any point in the cycle.

To be practically significant, however, this principle should be developed to the point where it is possible to force gently the current to zero in a time interval less than one-half cycle. Interruptions of this type have been realized at the lower voltages in Denmark and France. If it is necessary to use an appreciable time to prepare for the forcing of current zero, it would seem far better to use a jet of air and blow out the arc at the first natural current zero instead of wasting time in a tedious lengthening of the arc.

CONCLUSION

While the initial development of our improved axial air-blast interrupter reported was made on a high-interrupting-current low-interrupting-kilovolt-ampere step, the features of the device can be adapted to the complete range of sizes of axial air-blast breakers.

Air-blast breakers differ from other types particularly in the actuating means used. It requires a little time to get accustomed to the idea of pneumatic operation. For the usual application it will be found that the cost of installation and operation of air-blast breakers does not differ appreciably from that of other types, when the auxiliaries, including storage batteries, are included in the consideration.

In this connection it is interesting to note that on a recent check, made by the company with which the authors are connected, it was found that all of the outdoor oil circuit breakers in course of manufacture from 115 kv to 161 kv, inclusive, are pneumatically operated, each with its own individual compressed-air system.

We feel that the improved axial-type air-blast interrupting device presented here is the answer to the demand for a breaker with a much greater integrated duty capacity.

REFERENCE

1. THE CROSS-AIR-BLAST CIRCUIT BREAKER, D. C. Prince, J. A. Henley, W. K. Rankin. AIEE TRANSACTIONS, volume 59, 1940, September section, pages 510–17.

A New 50,000-Kva 5-Kv Oilless Circuit-Breaker and Metal-Clad Switchgear Unit

Discussion and authors' closure of paper 43-44 by R. C. Dickinson and B. I. Hayford, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, June section, pages 302–06.

L. J. Linde and **V. L. Cox** (General Electric Company, Philadelphia, Pa.): The introduction of this 50,000-kva 5-kv oilless circuit breaker is evidence that air breakers for this particular service are a permanent addition to the field of switching devices. The authors have described a design that is simple and adapted for use with horizontal draw-out metal-clad equipments.

A 50,000-kva 5-kv Magne-Blast (oilless) circuit breaker has been developed, and a considerable number of them have been produced in vertical-lift-type metal-clad equipments. This breaker has the same contour, and its dimensions and appearance are in close agreement with the present existing oil circuit breaker, Figure 1. It has been considered important to maintain a close similarity of design to that of the present oil breaker and metal-clad structures in order to use common parts and subassemblies in manufacture, where possible, in the interest of aiding production, and to make the most efficient use of space in the metal-clad unit. Figure 2 illustrates the outline of a basic metal-clad unit.

Figure 3 illustrates a cross section of the breaker now in production. The familiar Magne-Blast arc chute is mounted on and supported by the two insulator bushings. As the breaker contacts open, the current transfers from the primary contacts to a single silver tungsten-surfaced arcing contact. The resultant arc transfers from this

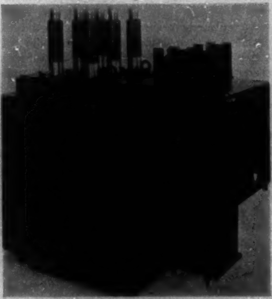


Figure 1. A 5,000-volt Magne-Blast breaker rated at 50,000 kva

This breaker is used with vertical-lift metal-enclosed switchgear. The insulated box barrier is withdrawn to the rear to expose the arc chutes and contacts

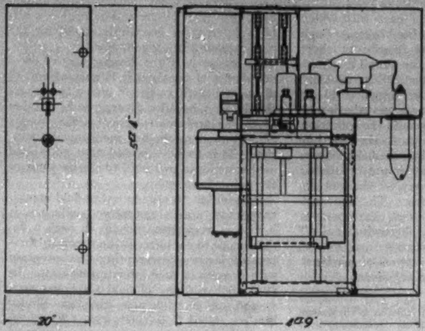


Figure 2. An arrangement drawing of the basic metal-enclosed switchgear unit used with the 50,000-kva Magne-Blast breaker

Cable connections can be made through potheads or insulators out of the bottom, back, or top

contact to the arc runner and shunts into the circuit a single multiturn blowout coil. The arc travels rapidly along the divergent runners and is forced by the arc chute into a serpentine pattern where the increase in arc resistance forces the current to decay and interrupt at an early current zero. A simple cup of molded insulation functions as an air "booster" and assists in light-current-interruption performance.

Figure 4 pictures a series of tests made at currents throughout the range of the breaker rating. Arcing time in cycles is plotted against current interrupted in amperes to show the consistent performance of this circuit breaker. It is apparent that the "booster" is successful in supplementing the magnetic field during the light-current interruptions to hold the arcing time to a minimum value. Recent laboratory tests have demonstrated that a light-current arc flows upward by convection currents. Similar tests show that this breaker will interrupt such currents in approximately 50 cycles when the arc chute is pointed directly up and the "booster" is disconnected.

A careful test program, including interruption tests, high-potential tests, operation tests, and so forth, has determined that an air breaker of this rating may be comparable to the equivalent oil circuit breaker in space requirements. This oilless circuit breaker should find many applications for 2,300- and 4,000-volt feeders, motors, generators, and general station auxiliary power service.

R. A. Hentz (Philadelphia Electric Company, Philadelphia, Pa.): The appearance of an oilless circuit breaker of the 50,000-kva 5-kv class is a welcome one. On the Philadelphia Electric Company system there are a large number of 2,300- and 4,000-volt customers where breakers of this size have application. When 100,000 kva was the smallest available, the use of oilless breakers was discouraged, as the cost and space required made the competition too stiff. While the number of circuit-breaker oil fires in such installations has been small (possibly because the breakers operate rarely), such fires have occurred. The advantage of eliminating any chance of an oil fire from the breaker is an important one.

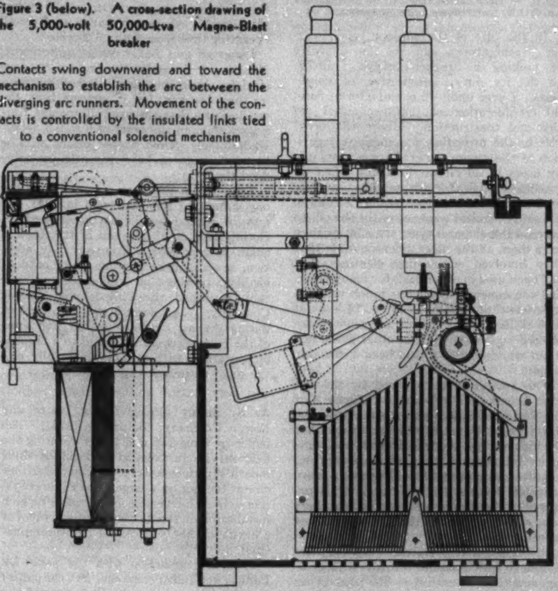
The subject of cost is mentioned twice in the paper, but no comparison with the cost of the oil breaker is given. It would be interesting to know how the two compare on a percentage basis.

I believe there is an advantage in having the finger contacts on the movable element as shown in Figure 8 of the paper. The simple stationary part shown on the right-hand side should have little need of adjustment, compared to movable part on the left. The ease and safety of being able to work on the finger contacts with the truck removed from the cubicle make this design attractive.

Paragraph 11 of the paper refers to the interruption of magnetizing currents. Figure 4 shows that eventual interruption is approximately 25 cycles. Such times are in marked contrast with the interruption of similar currents on an air-blast breaker, and this feature of the magnetic-type breaker must be listed as one of the unsolved problems which must continue to receive the attention of designers.

Figure 3 (below). A cross-section drawing of the 5,000-volt 50,000-kva Magne-Blast breaker

Contacts swing downward and toward the mechanism to establish the arc between the diverging arc runners. Movement of the contacts is controlled by the insulated links tied to a conventional solenoid mechanism



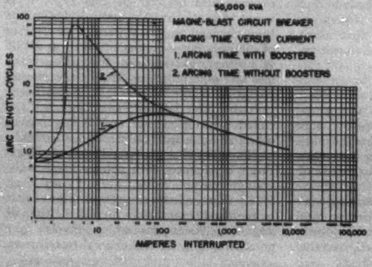

50,000 KVA
MAGNE-BLAST CIRCUIT BREAKER
ARCING TIME VERSUS CURRENT
1. ARCING TIME WITH BOOSTERS
2. ARCING TIME WITHOUT BOOSTERS
ARC LENGTH—CYCLES
100
10
2
1
10
100
1,000
10,000
100,000
AMPERES INTERRUPTED

Figure 4. A curve of arcing time versus current interrupted

The dotted line shows the arcing time measured during tests where the "booster" was removed

R. C. Dickinson and **B. I. Hayford:** This discussion leaves the reader in some doubt as to the low-current performance of the breaker described by Linde and Cox. The text reads as follows, "... Recent laboratory tests have demonstrated that a light-current arc flows upward by convection currents. Similar tests show that this breaker will interrupt such currents in approximately 50 cycles when the arc chute is pointed directly up and the 'booster' is disconnected." Figure 4, which shows curves of arcing time versus amperes interrupted, indicates 80 cycles of arcing without the booster, at four to five amperes.

If the tests for this curve were made with the breaker in its normal position, with the arc chute pointing straight down, then the preceding statement by Linde and Cox would indicate that the arc would never go into the arc chute, and therefore it would not be interrupted at all in the low-current ranges of perhaps four to five amperes.

A Vertical-Flow Compressed-Air Circuit Breaker and Its Application on a 132-Kv Power System

Discussion and author's closure of paper 43-51 by H. A. P. Langstaff and B. P. Baker, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, April section, pages 188–92.

R. A. Hentz (Philadelphia Electric Company, Philadelphia, Pa.): Oilless circuit breakers may be considered in three classes:

(*a*). Indoor, generally rated about 5,000 volts, of the magnetic type.

(*b*). Indoor, generally rated 15,000 volts of the air-blast type.

(*c*). Outdoor, rated 34,500 volts and above, also of the air-blast type, though not always applying to the same form of interrupter as *b*.

The advantages of classes *a* and *b* are well known of which the principal one, of course, is the elimination of oil with the consequent entire avoidance of oil fires. These breakers have also other favorable features but of a secondary nature, an example being cleanliness. The advantages of class *c* do not appear to be so outstanding. When a bad fire is outdoors, the effects are not so far-reaching, in that the heat and soot are not so confined as in the case of indoor installations, and such soot as is deposited on adjacent equipment is less harmful and more easily removed (stream of water). However, outdoor fires can be disastrous, and such have occurred, so that the elimination of oil is still desirable. Perhaps the trend of the industry will be toward the use outdoors of oilless breakers for most new installations, with relatively little replacement of the oil type as compared to replacement of indoor type, using large quantities of oil where the justification of such a step is greater.

In addition to the more obvious advantages of the air-blast breaker, relative to the oil type, there is the ease with which charging currents can be broken. Such currents are broken as readily as short circuits; this is not the case in oil breakers.

The installation of the 138-kv air-blast breaker described in the paper is to be hailed by the industry as a contribution to its progress in that it permits the collection of valuable field experience to the end that all the "bugs" will be eliminated the sooner, and breakers giving a minimum of trouble will be available for the after-the-war expansion of our facilities. The air-blast breaker is no exception to new developments in general; there are "growing pains" through which they must pass.

Experience with all types of air-blast breakers points to two conclusions:

1. High-power laboratory and field operations show them to be satisfactory in so far as the design of their interrupting elements are concerned.

2. Field experience shows that much progress is yet to be made before they can be mechanically satisfactory, particularly in the air system.

In the case of the outdoor type, freezing temperatures add to the difficulty of making a breaker always available to do its duty, irrespective of conditions. I urge upon all manufacturers careful consideration of the mechanical design and construction. Many will recognize in the preceding statements a repetition of the ground covered some years ago by users of oil circuit breakers, when an excellent electrical design was offset by some mechanical feature not being adequate. These mechanical weaknesses, in the oilless type no less than oil type, are now, as they were then, all the more unfortunate in that they involved engineering elements that had been used in other fields for many years, and consequently there was much valuable experience on which to draw. I do not wish to imply that the manufacturers have ignored such experience in other fields, but I am wondering if it has been given due weight.

The greater space required by the air-blast breaker, as indicated by Figure 8 of the Langstaff-Baker paper, should not be disturbing. It must be remembered that the high-voltage outdoor air-blast breaker is in its infancy and that, as time goes on, improvements probably will result in more compact designs in the future. This occurred in the case of oil breakers, and there is no reason to believe that history will not repeat itself.

In the paper it is stated: "By compressing air to 1,000 pounds per square inch, cooling, and then expanding to 350 pounds per square inch, dry air is secured ...." One of the advantages of field experience is to find out whether this is always so. Operators will be well advised to watch this, along with the many other features that will require attention.

H. E. Strang (General Electric Company, Philadelphia, Pa.): The authors of this paper have concluded properly that the most satisfactory means of establishing the merits of and expediting the progress in design of high-voltage air-blast breakers is to acquire field experience under actual operating conditions. It seems certain, based on test data presented here and supplemented by work along this line previously discussed, that the basic principle of a vertical or axial-flow air-blast interrupter is entirely sound for the interruption of high-voltage arcs.

At this stage of the development of these new types of breakers it is natural and of ultimate benefit to the industry that there should be a healthy divergence of opinion as to the preferred physical arrangements of parts and method of mechanical operation. It is to be expected that additional field experience will tend to clarify some of the points involved.

In addition to accumulating field experience under normal operating conditions, it is also of very great benefit in such a development to supplement the necessarily extensive factory interrupting test program with a series of field interrupting tests. In 1941 such a series of field tests was conducted on a triple-pole air-blast 138-kv breaker rated 1,500,000 kva. Even on superspeed reclosing service over a range of duty up to 1,870,000 kva (where therefore this fault had to be cleared *twice* in 20 cycles) this breaker showed a range of over-all operating time from 3.8 to 5.2 cycles.

Langstaff and Baker report quite favorable arcing times for the factory tests on straight single-operation interrupting duty on their breaker, but it appears from the oscillograms that the over-all opening time is something in the order of 6 to 9.5 cycles. Undoubtedly, some simple means will be found to reduce the apparently excessive dead time.

Two 138-kv breakers, identical with the one submitted for field tests in 1941, are now in regular service on superspeed reclosing duty on the system of the Appalachian Electric Power Company. In addition, seven 34.5-kv and one 69-kv outdoor air-blast breakers of similar designs have been furnished the Philadelphia Electric Company. The former were in service during the last lightning season, and some of the breakers were subjected to a number of operations with results which fully met the most optimistic expectations.

A. N. Shealy (Pennsylvania Water and Power Company, Baltimore, Md): The paper by Langstaff and Baker covering the field service installation and initial three month's performance of a compressed-air circuit breaker on a large 132-kv power system is most interesting. The value of such installations in the development and proof of a device of this type cannot be overemphasized.

From a comparison with the paper by Ludwig and Baker (reference 5 of the paper) presented two years ago which covered the experimental breaker of this type, it is noted that the isolating switch design has been changed materially, and a description of the isolating switch as installed would be of interest, particularly since this switch has an important function in the closing operation of the breaker. It is also noted that the three isolating switches reach the closed position within one cycle of each other, and this brings to mind the possibility of difficulties with high-speed ground relays. From experience with conventional high-voltage air-break switches, it has been found that high-speed ground relays must be removed from service during the time of switching operations which involve the paralleling of grounded-neutral transformer banks. It would appear that the same difficulty would be experienced during the closing operation of the compressed-air circuit breaker if current flows in one or two phases for approximately a cycle before the three-phase circuit is established. This is aggravated under certain switching conditions by one or two poles arcing at a greater distance as the switch approaches the closed position, and it seems reasonable to expect that with this breaker ground currents might flow for more than one cycle. If consideration has been given to this, the authors' comments would be interesting.

It is not recalled that the continuous trickle of dry air through the porcelain columns has been reported previously, and it would be interesting to know if this is merely a precautionary measure or the result of difficulties with the inner surfaces of the porcelain columns. The small amount of air used for this purpose is not objectionable.

H. A. P. Langstaff:

Performance to Date

To date the breaker has had a total of 57 operations, only five being automatic. All were satisfactory. During one very cold spell the gradual dropping of pressure in the high-pressure system and the continued running of compressors resulted in finding the two pressure relief diaphragms punctured and the two check valves frozen, caused mainly by their inherent design. No piping nor valves were injured. New relief diaphragms were installed, the ice thawed, and the system was returned to normal.

Heaters have been installed temporarily to prevent any additional freezing until the piping is rearranged, leaving the cooling coils outdoors and locating the check valves within the compressor housing. Our most recent and coldest weather reaching approximately 12 below zero developed no trouble. Our operating instructions call for all 132-kv breakers to be operated manually for test during cold weather.

A number of air-blast breakers have been equipped with air dryers, but as yet we have had no indication whatever of the necessity of such a unit.

During a recent one-month period we had five manual open and close operations, and the compressors have operated an average of 1.67 hours out of each 24-hour period. We are keeping such records to determine inspection schedule for the air compressors.

Design and Installation

The speed of this breaker meets our present system and relaying requirements. Had we specified a faster operating unit and been willing to pay the extra cost, the manufacturer would probably have met our requirements.

We have no extremely fast-operating ground relays on the circuit this breaker controls, but I believe it possible that its functioning would meet such relay switching requirements as shown by factory speed tests.

Space requirements as mentioned by Mr. Hentz, it seems to me, should be ample in most switching structures. The height requirements may be the limiting feature.

The comment regarding use of bushing-type current transformers is quite justifiable. We would prefer their use, and future designers should keep these in mind.

As stated in the paper, the breaker design and assemblies met our specifications, however, as we already had installed wave trap and capacitor in this particular circuit and were able to make a decided improvement in source of potential supply for all present and proposed future relaying, we left the existing carrier equipment in place, and installed the new capacitors which were shipped with the breaker in another location and connected them to the station bus. Our accounting was also simplified.

During the breaker assembly it was thought advisable to provide greater flexibility in the vertical porcelain columns, and this was obtained by the use of different gaskets, bolts, and three cup washers properly compressed with each bolt in the porcelain hardware.

In conclusion, our experience thus far would, I believe, direct our decision in favor of air-blast design, should our requirements call for additional 132-kv breakers.

The Auto-Blast Interrupter Switch

Discussion of paper 43-52, by E. A. Williams, Jr., and W. G. Harlow, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, April section, pages 176–81.

P. H. Light (General Electric Company, Schenectady, N. Y.): It has been shown[1] that there is a definite economic field for the application of interrupter switches applied to unit substations 15 kv and below. On some applications several load-center unit substations are connected to a single short feeder on which short-circuit protection for the feeder and load-center transformers is obtained by a circuit breaker at the feeder source. On these applications some means of disconnecting the transformer from the feeder is often desired. To make it unnecessary to de-energize the feeder when a single transformer is de-energized, the disconnecting means should be capable of interrupting the magnetizing current of the transformer and in some cases, possibly, load current as well. A liquid-filled switch has been available for interrupting magnetizing current, but the liquid-filled switch has some limitations that are not present in an air-filled switch. In the application of any disconnecting device that is not capable of interrupting the system short-circuit current, there is always a possibility, however remote, that the switch may be opened under fault conditions. Under these conditions the switch will probably fail to interrupt the fault current, and the arc will be maintained within the switch enclosing case. If the switch is liquid-filled, the arc will cause volatilization of the liquid, building up very high pressure within the enclosing case, possibly rupturing it. If the switch is oil-filled, there is also the danger of creating a fire. The air-filled switch is not a fire hazard, and, inasmuch as air is compressible, there is less possibility of injurious effects extending beyond the enclosing case. Thus, if the disconnecting device is operated under fault conditions, the damage resulting from failure will probably be less if the switch is air filled instead of liquid filled.

A fundamental feature of an air-filled switch is its large size in contrast with a liquid-filled switch. In order to maintain the standard basic-impulse insulation levels corresponding to other switchgear switching devices, such as power circuit breakers, with air as the insulating medium, large clearances are fundamentally necessary, resulting unavoidably in dimensions in the order of those given in the paper.

For the application in which the switch is mounted on the transformer tank, the use of the switch will probably be limited to the larger load-center unit substations and master unit substations rated 15 kv and below. For this application the new switch fills a long felt need for a disconnecting device of this kind.

Reference

1. Fundamental Considerations in Selecting Metal-Enclosed Switches for Unit Substations and Secondary-Network Transformers, W. J. McLachlan, D. L. Beeman. *General Electric Review*, May 1942.

A Multiorifice Interrupter for High-Voltage Oil Circuit Breakers

Discussion of paper 43-53 by L. R. Ludwig and W. M. Leeds, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, March section, pages 119–25.

H. E. Strang (General Electric Company, Philadelphia, Pa.): It is, no doubt, of considerable interest to the industry that the high-voltage breaker as described by the authors utilizes directed oil flow as a means of increasing the efficiency of arc interruption. While the interrupters used differ mechanically, it seems quite possible that the principle of interruption may be very similar to that employed in the oil-blast breaker. In both cases it is recognized that oil is highly mobile and can be moved easily and directed accurately. Both use pressure created by action of a primary arc to move and direct the oil in such a way that it removes the arc products, replacing them by new fresh oil having high insulation strength.

While it is claimed that the major benefit accrues from the lateral flow of oil along the path of the arc, yet the construction appears such that there is a multiple-orifice outlet, providing the means of establishing a flow of oil across the path of the arc. The value of the multiple-orifice outlet was established in connection with the first 287,000-volt three-cycle impulse breakers where the flow of oil was caused by a piston, and where a double outlet for the cross oil flow was found to be more effective than a single one. This was discussed fully before this group as early as 1936 and has been used extensively since in multibreak interrupters now in service on many five-cycle tank-type breakers from 110 to 230 kv.

From a standpoint of mechanical construction, there seems to be some reasonable doubt as to whether the complication of the extra vertical operating rod within the interrupter and the rocker shaft with its contact at the top is necessary, since this function can be accomplished so simply by the cross-bridging member in the multibreak interrupter.

Undoubtedly, some further refinements and improvements are to be expected in this new member of the family, since for correspondingly rated breakers the operating times reported in this paper are consistently longer than those previously demonstrated by comprehensive field tests on both 230-kv and 138-kv five-cycle oil-blast breakers, which were both reported in full to this group.[1,2]

References

1. New Multibreak Interrupter for Fast-Clearing Circuit Breakers, **R. M. Spurck, H. E. Strang.** AIEE Transactions, volume 57, 1938, December section, pages 705–10.

2. Tests on Performance of a High-Speed Multibreak 138-Kv Oil Circuit Breaker, **Philip Sporn, H. P. St. Clair.** AIEE Transactions, volume 57, 1938, December section, pages 696–703.

W. F. Skeats (General Electric Company, Philadelphia, Pa.): This paper shows charging-current tests at 33 kv across one sixth of a pole of a 287-kv breaker but makes no mention of such tests on any of the units expected to withstand a higher voltage per grid. I should like to ask the authors if any such tests have been made.

J. T. Logan (Georgia Power Company, Atlanta, Ga.): It is significant to note the current interest being given circuit breakers. Heavy power demands and extended interconnections are beginning to shove the stability problem back into limelight. High-speed disconnection of faulted transmission lines is essential to the promotion of system stability. Another justification for faster-operating breakers is high-speed switching which, in the writer's opinion, is doing more toward improving the quality of electric service than any one practice adopted during the past decade. Not only are the effects of reflected voltage disturbances minimized, but circuit breakers with high-speed operating characteristics are reducing the need for large expenditures in transmission lines. Present-day conditions do not permit constructing additional lines where existing lines can be made to serve adequately through modernization of terminal facilities. New lines must be deprived of prewar frills, consequently, means must be devised for offsetting the effects of increased number of line flashovers. There appears to be no good reason why a transmission line constructed at relatively low cost, equipped with high-speed circuit breakers cannot render a quality of service equal to that rendered over so-called "lightning-proof" lines not equipped for high-speed reconnection to the system. The authors of this paper have done a very commendable job in exploring the possibility of minimizing the number of contacts required for interrupting the arc, thereby simplifying the operator's upkeep problem. Their test results indicate little else to be desired from the standpoint of arc extinction, and, if I should be permitted to make one suggestion, it would be in connection with lessening the time of circuit-breaker closing to a value commensurate with speed of opening.

C. P. Garman (Bureau of Power and Light, Los Angeles, Calif.): During the early stages of design of the Boulder Dam transmission lines, it was realized that high-speed switching would increase the transmission capacity of these lines. The transient-stability curves indicated, however, that the greatest gains could be made by shortening the fault durations to about one tenth of a second; anything less than this would not give proportionate gains. Therefore, oil circuit breakers were purchased for the original two circuits with a specified interrupting capacity of 2,500,000 kva and a time of opening not to exceed three cycles (0.05 second) from one quarter to full interrupting capacity.

At that time, circuit breakers of this speed and capacity had not been built, although information obtained from the testing laboratories of the large electrical manufacturers indicated that they could be built. Time and experience has substantiated this belief, and the benefits obtained from the use of high-speed switching have manifested themselves in more ways than increased transmission-line capacity. The knowledge gained during the first year of operation of the Boulder Dam transmission system, however, indicated that certain other characteristics not called for in the original Westinghouse breakers at Boulder were very desirable: that is, time of interruption of currents less than 25 per cent of rated capacity.

When sections of line were dropped at Boulder Dam power plant, the time of interrupting the charging current by the Westinghouse breakers would, in many cases, be in the order of 8 to 12 cycles. Oscillograms, as well as system disturbances, indicated that very bad transients were present during the last few cycles of arc interruption. The extent to which this increased number of cycles required to interrupt charging current affected transient disturbances was not known, but it was believed that most of the transients could be eliminated if the charging current could be interrupted in five cycles or less, and the oscillograph records show no tendency for the arc to restrike after the current wave has passed through zero.

When the third circuit to Boulder Dam was under consideration with two additional generators included in the high-voltage ring bus at the Boulder Dam power plant, the interrupting duty requirement was increased to a point somewhat beyond 3,000,000 kva. It was, therefore, required that the additional circuit breakers to be provided at Boulder Dam power plant for the third circuit should have an interrupting capacity of 3,500,000 kva, from one fourth to full interrupting capacity in three cycles, and not to exceed five cycles of arc interruption in any event.

At that time, the Westinghouse company indicated it could build a dead-tank-type breaker that would meet these requirements. It also indicated that it could develop an interrupting device that would increase the interrupting capacity of the original dead-tank-type breakers at Boulder Dam power plant to 3,500,000 kva, and meet the above stated time requirements. Negotiations were accordingly started between the United States Bureau of Reclamation, City of Los Angeles Bureau of Power and Light, and the Westinghouse company for the development and application of this device to the breakers in question.

The paper by Mr. Ludwig and Mr. Leeds entitled "A Multiorifice Interrupter for High-Voltage Circuit Breakers" describes the design and presents an analysis of tests made in the company's new high-power laboratory on the device which will be installed shortly in the original Westinghouse 287-kv breakers at Boulder Dam power plant.

The multiple-orifice interrupters described represent a distinct advance in the art of interrupting currents at high voltage by devices that generate their own forces to affect arc interruption. The test results are very gratifying. The oscillograms indicate that the Boulder Dam breakers, when rebuilt, will meet all specified requirements and interrupt effectively all charging currents in the order of those existing on the Boulder Dam transmission lines in four cycles or less without tendency to restrike.

Cathode-ray-oscillograph records and tests indicate that the resistors selected for stress distribution across the device effectively dampen transient voltages, since no overvoltages were recorded exceeding 100 per cent of normal crest during a series of 28 laboratory tests interrupting charging currents.

H. V. Nye (Allis-Chalmers Manufacturing Company, West Allis, Wis.): The paper by Ludwig and Leeds is an excellent presentation of the principal design features and performance of the breaker described.

There appears to be two novel features in the design of this interrupter which merit separate consideration. The first of these is the ingenious method employed of getting around one of the inherent disadvantages of previous high-voltage double-arc interrupters. I refer to the time element required to draw a pressure-producing arc before the main arc can be drawn, upon which the oil blast is to operate. In the breaker described in this paper, both of these arcs are drawn almost simultaneously, and a great saving in arcing time results. This is clearly shown by a comparison of Tables III and IV of the paper where a 138-kv breaker drawing these arcs in series takes about twice the arcing time of the 230-kv breaker drawing them simultaneously. Of course, the mechanical operating times on these breakers were different, the 138-kv breaker requiring 3.8 cycles to part contacts as against $2^1/_2$ cycles on the 230-kv. Nevertheless, the action is very clearly brought out by these data.

The introduction of multiple orifices without multiplying the number of arcing contacts, while not entirely novel, is very effectively done and, I think, presents a notable advance over the multiple contact devices previously used for many breakers of equivalent ratings. In all, the authors are to be complimented on the excellent manner in which they have presented their subject and for the test data they have given comparing the various constructions.

Oil-Impregnated-Paper High-Voltage Condenser Bushings for Circuit Breakers and Transformers

Discussion and author's closure of paper 43-54 by H. J. Lingal, H. L. Cole, and T. R. Watts, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, June section, pages 269–75.

George A. Matthews (The Detroit Edison Company, Detroit, Mich.): H. J. Lingal, H. S. Cole, and T. R. Watts should be complimented for their accomplishment that they so aptly described in their paper.

They have described new shop practices and a new type of high-voltage condenser bushing that has unusually high electrical and mechanical strength. The high electrical strength in this new type of bushing is obtained through the use of oil-impregnated paper as the insulation in the condenser. Uniformity in these bushings will be assured by the new tools and shop practices. Permanency of strength and long life will be obtained by the use of an oil-filled, sealed porcelain and metal casing if it remains tight. I believe that, excepting as practiced by The Detroit Edison Company since 1936, the condensers in bushings have not been wholly encased in porcelain and steel.

In 1936 The Detroit Edison Company started to rebuild their Westinghouse 138-kv multipiece porcelain compound-filled condenser bushings that had been in service 12 years. It was necessary to do a major job of rebuilding because the gaskets had decayed so far that they no longer formed a weatherproof seal, and the bushings were absorbing water, and the oil in the breaker or transformer tanks was syphoning through the shellacked paper insulation in the condenser with the result that the electrical strength of the bushings was very low. Our objective in rebuilding was to produce a condenser bushing that would be self-protecting against impulse voltages permanently in a long life and with greatly reduced maintenance.

After sufficient testing it was found that a suitable bushing could be produced by oil impregnating the 12-year-old condensers, then enclosing them in oil-filled and sealed casings of porcelain and steel. In order that the internal pressure would always be greater than the atmospheric pressure compressed nitrogen gas was applied. The gas pressure varies throughout the temperature range, −15 degrees Fahrenheit to 130 degrees Fahrenheit, from 1.5 pounds gauge to 14 pounds gauge maximum.

While experimenting, we found that the old shellacked condensers, unimpregnated encased in oil and porcelain, failed under the first application of a steep front wave, although a 32-inch rod gap was connected in parallel. Whereas these same old shellacked paper condensers after vacuum treatment and oil impregnation, encased in the same steel and porcelain, oil-filled casings were found to be self-protecting against front of wave flashover in tests. This in our minds definitely proves the value of oil impregnation.

Typical oscillograph data taken from tests of oil-impregnated condenser bushings as we rebuilt and are now operating them follow:

60-cycle flashover test, before and after impulse tests, at 450 kv corrected for standard air conditions

60-cycle, one-minute holding test at 355 kv

Impulse Tests:

Front of wave flashover

With $43^{1}/_{4}$-inch rod gap in parallel with the bushing. Lower end of bushing immersed in oil as used in service

Wave front rising at approximately 3,000 kv per microsecond

Bushings flashed over 25 times with a negative wave

Bushing flashed over 100 times with a positive wave

All flashovers occurred externally, that is, above the supporting flanges.

In the year 1939, after the rebuilt bushings had been in service three years, a number of them were taken out of service for a check test. They were given 60-cycle holding and flashover tests before and after 25 positive and five negative impulse tests for front of wave, flashover having an average crest of 1,750 actual kilovolts. The wave front rising at approximately 2,400 kv per microsecond. After these tests, the bushings were taken apart, and examination showed that they were not damaged. They were then reassembled and returned to service.

These tests were not so severe as the initial tests described hereinbefore that we ran on these same bushings. This was due to the limited capacity of the laboratory. They were sufficiently severe, however, to bring out any objectionable weaknesses.

At this date we have 500 of these rebuilt bushings in service. They differ from this new Westinghouse bushing as follows:

1. They operate with compressed nitrogen gas at a pressure above the atmospheric pressure.
2. Our condenser insulation is shellacked paper that was oil impregnated after 12 years of usage.
3. Gaskets are Neoprene instead of Neoprene cork.
4. Gasket loading stops are lead instead of Neoprene asbestos.
5. Sliding contact is provided to compensate for expansion, whereas the Westinghouse bushing uses a diaphragm cap.
6. We do not have brazed or soldered joints.
7. We use an indicating pressure gauge located just above the supporting flange instead of an oil-level indicator.

Our reasons for operating these rebuilt bushings under gas pressure are twofold:

1. If a leak should occur, water will not enter as long as there is pressure.
2. If there should be undue temperature rise within the bushing, the pressure would be increased. Either of these changes from normal conditions would be indicated visibly at the pressure gauge and as our operators are furnished with an ambient temperature-pressure curve, any abnormal condition of the bushing would be detected within a very short time after it occurred. We believe this pressure-gauge feature is superior, as it gives us a daily indication of the condition of the bushing, thereby making periodic inspection unnecessary. The oil-level indicator would not indicate leaks that might occur above the surface of the oil, nor would it as definitely indicate unwanted temperature rise.

Although the unnecessarily large number of brazed and soldered joints, if well made, will not adversely affect the life or cost of operating the bushing, they do increase however the manufacturing costs and constitute just another thing to watch. It is well known that brazed joints are likely to be porous unless great care is exercised.

I suggest that the Neoprene asbestos might swell and thereby lift the porcelain enough to cause leaks at the gasketed joints. We would use lead rings instead of the compound for the stops.

In conclusion, I state that the data presented by paper 43-54, as well as considerable other data, indicate that oil-impregnated paper is the best insulating medium for condenser bushings that is known today, and that if it is properly protected from contamination, it will retain its high electrical strength for many years.

A. W. Gothberg (Public Service Electric and Gas Company, Newark, N. J.): The authors are to be complimented on developing a high-voltage bushing having a large reserve in working insulation strength, well coordinated insulation, and on establishing the impulse flashover and breakdown curves. Many mechanical improvements have been incorporated to overcome the difficulties and defects of earlier types. This bushing is designed to give an excellent factory-assembled job, but it lacks the simplicity of assembly for satisfactory field maintenance.

In order to make even minor replacements it is necessary to unsolder two seals, one of which is around the threaded stud. To back off the lifting lug nut past the solder-coated threads and to remake and test properly these solder seals after reassembling the bushing is not a very convenient field job.

It may be argued that any replacements should be made at the factory because of the importance of driving out and eliminating moisture and gases during the filling process. There are two answers to this:

1. If operating companies can make successful cable splices where the insulation working stresses far exceed the working stresses used in the bushing, they should be able to do a proper filling job.
2. If this bushing requires assembling and refilling under such carefully controlled conditions, an effort should be made to simplify the design so that it can be maintained in the field.

No large user of high-voltage bushings wants to ship his bushings to the factory for the replacement of a porcelain or some minor part, particularly if it is to cure an epidemic of trouble. Such a procedure may require a large number of spares, and in some cases it will increase the outage time of the equipment on which the bushing is installed. In addition, the handling and shipping charges may far exceed the cost of the repairs.

Although the design of this bushing, both from an electrical and mechanical standpoint, appears to be far superior to its forerunners, it is suggested that more thought be given to a simplified assembly of a bushing from an operator's viewpoint. No matter how perfect a piece of equipment appears on test, troubles are bound to occur, especially if they can be caused by external conditions. Unless these troubles are major ones, they should be capable of being remedied in the field, and the equipment should be designed accordingly.

J. B. Whitehead (The Johns Hopkins University, Baltimore, Md.): There are several good reasons why the use of oil-impregnated paper in condenser bushings and terminals should give better performance than those of earlier types. In particular, the use of a low viscosity oil gives a better degree of saturation and, as is well known, this increases dielectric strength and stability. Further, the use of continuous sheets of paper is a distinct advantage since it is now generally understood that failure of impregnated paper insulation in cables usually starts in the oil channels between successive turns of tape.

It is doubtful, however, whether full advantage has as yet been taken of these methods for improvement in the bushings described by the authors. The methods employed here are evidently an approach to those used in the assembly of the insulation of the so-called oil-filled cables. Experience with this type of insulation, however, has shown that its best properties are reached by more careful drying and evaporation methods than described by these authors. In fact, the length of the metal foil layers and the absence of oil channels introduce some question as to whether complete impregnation may be reached even under the programs referred to.

Moreover, it is stated that in the bushings for the higher ranges of voltage it has been necessary to use paper tape assembled cable fashion on the inner sections of the condenser bushing. This naturally introduces the vulnerable oil channel. While the average operating voltage gradient is relatively low as compared with cable practice, it is nevertheless desirable that information should be given as to the actual values of stress which are reached in these oil channels, bearing in mind their relatively short radius and the differences in dielectric constant between paper and oil.

As bearing on the question of impregnation, it would also be helpful if power factor stress curves could be given over and above the operating range.

There is one feature of the test results which raises a serious question as to whether it is possible to compare the performance of these bushings with that obtained in the best oil-filled cable insulation. Apparently, all or most of the failures have taken place at the edges of the metal foils forming the electrodes of the successive steps of capacitance. This means that there is a weaker point in the bushing than the weakest point in the impregnated paper insulation wall. In my judgment still further performance and still further utilization of the advantages of oil-filled insulation will be obtained by further protection or stress reduction at the edges of the metal foil layers.

H. J. Lingal: In reference to discussions presented by G. A. Matthews and J. B. Whitehead, it is necessary to wind the inner layers of the condensers for the higher-voltage bushings from tape or strip applied helically around the conductor, since machines are not available for winding sheets more than 100 inches wide. In this type of winding, in bushings as in cable, small oil-filled channels exist between adjacent turns in a layer of paper. Although Dr. Whitehead has shown that failure of oil-impregnated paper in cables usually starts in these small channels, there is no cause for alarm about them in the condenser bushings, as the operating voltage gradient is only about one third as high as in cables where the same type of channels are present. Furthermore, in cables the voltage gradient inherently increases sharply to a maximum at the surface of the conductor, while in a condenser bushing there are many coaxial cylinders of copper foil, dividing the insulation into layers approximately 0.1 inch thick, having practically equal voltage on each layer. This result could be accomplished in cables only by using graded insulating materials, having a lower dielectric constant in each successive layer, and obviously, not many such layers are feasible.

In bushing condensers using the same insulating material throughout, the fundamental principle of design provides a voltage divider comprising many coaxial cylinder capacitors of equal capacitance in series. By proper correlation of the increase in diameter with the uniform decrease in length of the successive copper-foil cylinders in the stepped and tapered ends of the bushing condenser, the total applied voltage is divided equally among the many concentric layers.

Therefore, even though the paper tape, admittedly somewhat inferior to the continuous sheets of paper, is used for the innermost layers of the larger bushing condensers, it is not subjected to a higher-voltage gradient. The fact that a large margin of safety exists is borne out not only by laboratory tests, but also by 1,500 bushing-years of actual field service as reported by Mr. Matthews.

However, when machines for winding paper more than 100 inches wide become available, they will probably be adopted for winding the larger bushing condensers, mainly to reduce time and cost of winding.

During the testing of the two bushings on which test data are given in the paper, power-factor measurements were taken at voltages up to 200 kv. The values obtained from these measurements, when plotted, form practically a straight line, indicating the absence of corona which might be expected if impregnation was not complete. Radio influence voltage measurements confirmed this indication.

The degassed oil for impregnating quickly absorbs any residue of air left in the condenser at the end of the evacuating operation which removes 99$^{1}/_{2}$ per cent or more of all free air. The completeness has been tested by unwinding a number of bushings after they had been put through the evacuating and impregnating treatment. Furthermore, the routine overvoltage test on each bushing is sufficient to puncture any layer which is not thoroughly oil-impregnated. Any such puncture on test is observed readily on the testing transformer instruments at the time and also revealed by the subsequent tap voltage test and Schering bridge readings.

The final tests on the 196-kv bushing were made in an oil tank. Before these tests, the condenser was exposed to air for considerable time. During the exposure to air, we suspect that some oil near the surface was drained out and replaced by air, and that when the condenser was immersed in oil in the test tank, a part of this air was trapped in the paper at the ends of the condenser steps. The breakdown of this entrapped air is believed mainly responsible for the breakdowns occurring at the ends of the foils. However, there is inherently a slight concentration of stress at the ends of each foil cylinder because of electrostatic flux fringing out to the longer foil cylinder of the next layer inside.

In the discussions some materials and details of construction were criticized constructively. In many cases a designer has available several suitable materials from which a selection can be made. The one he uses should have a background or record of service that will justify the designer's choice. In selecting Cork Neoprene sealing gaskets, and Asbestos Neoprene stop gaskets, the designers chose materials with which we have at least a decade of highly satisfactory experience on many thousands of bushings built and in service.

In these bushings the use of castings, where oil, air, or moisture tightness is required, has been abandoned. Such parts are made from sheet or rolled sections or tubes. This type of construction, together with the need to use the minimum of critical materials, has dictated the use of welding and brazing to form many parts for which castings were formerly used.

Rebuilt bushings in which the expansion space above the oil is filled with nitrogen under pressure are described by Mr. Matthews. The tightness of the bushings is indicated by pressure gauges installed on the flange. This is a very reliable means of indicating tightness. It has been considered in making the design of the oil-impregnated bushing and nitrogen under pressure with a suitable pressure indicating gauge can be added to the present bushing when and if required. It is believed, however, that the simple magnetic gauge indicating the oil level is more easily read and interpreted by the average maintenance man who would have to be trained to interpret the effects of temperature and gas absorption on the readings from the pressure gauge.

In reply to Mr. Gothberg's discussion, the technique required to dismantle and reassemble these bushings is difficult only to the extent that different tools and some new procedure is required to do the job. The first requirement is a knowledge of the bushing construction and the procedure to be followed in dismantling and reassembling so that the quality of the rebuilt or repaired bushings will be as nearly as possible that of a new bushing. With this knowledge, a repair can be made by a competent mechanic in a surprisingly short time. The special tools required for dismantling and reassembling the bushing can either be procured in advance, or readily made when needed. A repair on this bushing will be no more difficult than many an operation now being performed every day by the operating companies' maintenance departments.

These organizations have available skilled and experienced workmen. They are expert in performing many operations requiring a high degree of skill with expert knowledge of the apparatus. These organizations would not assign a man to rebuild a motor, a transformer, a circuit breaker, or any other piece of apparatus on their system without seeing first that he had been given adequate instructions and providing him with information on the construction of the apparatus and the functions of its various parts. Since high-voltage bushings have both high service and money value, we have recommended that repair or rebuilding be done only by those experts in the work who have the proper equipment. Unless such men are available in the operating company maintenance organizations, we feel that the cost of returning a bushing to the factory is justified if major repairs are to be made.

The discussions indicate differences of opinion regarding some of the mechanical design features and the degree of completeness of evacuation and impregnation. A complete agreement exists between the discussers and authors on the conclusion that oil-impregnated paper—well processed and protected as in the bushings described in the paper—will have unusually high dielectric strength and long and trouble-free life.

Application of Lightning Protective Devices in Wartime

Discussion and author's closure of paper 43-63 by I. W. Gross, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, April section, pages 173–5.

Edward Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Everyone is in agreement with the purpose of Mr. Gross' paper. The war accentuates the importance of the preservation of service continuity so that war production may be interrupted as little as possible. This is the major objective. At the same time, more than ordinary emphasis is being put upon the need for accomplishing this with the minimum amount of time and labor devoted to maintenance and repair, and with the least amount of material in devices whose purpose is to insure continuity of power flow.

The accomplishment of such programs will involve, in some cases, saving by spending—spending for additional or more effective lightning protection, as mentioned in the paper. Mr. Gross, however, has put more emphasis on the savings that look attractive through reduction in arrester ratings and in the use of smaller types. The reduction in arrester ratings has apparently worked out to great advantage on Mr. Gross' systems, and he is to be felicitated on the careful and effective engineering that has gone into this practice. However, its general adoption without careful consideration may be loaded with troubles, since there are systems on which it is likely to result in frequent arrester failures causing outages thereby interfering with power flow. For the most trouble-free operation, it is necessary on some systems to apply arresters of higher than normal rating.

At an earlier session, there was presented a report of the protective devices committee on "Correlation of System Overvoltages and System Grounding Impedance." A perusal of this report is useful in the problems that arise in arrester application. General recommendations may be dangerous, and they carry considerable responsibility. It is for this reason, probably, that it was not possible to obtain complete subcommittee agreement on a few of the considerations involved in the endeavor to insure the greatest degree of uninterrupted power flow by the means most economical in maintenance, labor, time, and material.

Regarding the suggestion that the internal mechanism of the sealed gap elements in lightning arresters could be made of iron in place of brass, it must be recognized that there are certain limitations in the use of materials in lightning arrester gaps if good consistent operation is to be maintained. Probably all lightning arrester manufacturers have given serious consideration to substitution of materials where possible and have put such substitutions into effect where it does not interfere with the successful operation of arresters.

There is another application, not mentioned in Mr. Gross' paper, in which material savings may be made. This is in the lightning proofing of transmission lines. On new construction, overhead ground wires and De-ion protector tubes compete for this service. For new construction, the overhead-ground-wire construction is, in general, more economical in dollars. However, considering the materials involved, protector-tube installation will frequently protect a transmission line with less material than that involved in an adequate overhead-ground-wire installation.

REFERENCE

1. CORRELATION OF SYSTEM OVERVOLTAGES AND SYSTEM-GROUNDING IMPEDANCE, AIEE working group on correlation of system-grounding impedance. AIEE TRANSACTIONS, volume 62, 1943, January section, pages 17–24.

P. L. Bellaschi (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): Mr. Gross outlines the over all economies that may be attained from protective devices applied on a sound engineering basis. The application of an 80 per cent arrester on grounded neutral systems is a good example of a twofold gain in over all economy; first, in the use of a lower rating arrester; second, in the greater margins of protection obtained. However, we would do well to emphasize once again that this application requires engineering predetermination or check-up in each case to the effect that the dynamic voltage at no time shall exceed the rating of the arrester. Where an 80 per cent arrester is permissible, there is the possibility of further economy,—transformer insulation one class lower than normally would be required may be considered. This presupposes that the lightning arrester is not called upon to discharge excessive surge currents. Such may be the conditions in well-shielded stations or where lightning severity is low. Many transformers with reduced insulation are in service. Their good record speaks well for this practice.

I heartily agree with Mr. Gross that a good going over of present installations may reveal vulnerable points and that the correction may pay dividends. I have reference particularly to stations built years ago now serving vital zones, to inherently low-impulse strength apparatus (such as rotating machines, and similar apparatus, and so forth) when directly exposed or located on low-voltage circuits coupled to high-voltage lines through transformers and to other cases of the kind. The location of the arresters up closer to the transformers is one of the usual corrections found. Occasionally, it is found in addition that the remedy is to provide up-to-date and efficient protective equipment to the apparatus. Unfortunately, some of these vulnerable spots often come to light after lightning has had the opportunity to do the picking, and damage has been done.

In conclusion, I may point out that the report of the transformer subcommittee[1] on the "Protection of Power Transformers Against Lightning Surges" as well as the recent report on "Correlation of System Overvoltages and System-Grounding Impedance" by the working group on correlation of system-grounding impedance, both should be of assistance also in the application of lightning protective devices.

REFERENCES

1. PROTECTION OF POWER TRANSFORMERS AGAINST LIGHTNING SURGES, AIEE transformer subcommittee on electrical machinery. AIEE TRANSACTIONS, volume 60, 1941, pages 568–77.

2. CORRELATION OF SYSTEM OVERVOLTAGES AND SYSTEM-GROUNDING IMPEDANCE, AIEE working group on correlation of system-grounding impedance. AIEE TRANSACTIONS, volume 62, 1943, January section, pages 17–24.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): Mr. Gross has prepared a stimulating paper on which I give the following special comments.

Section III, item 3 of the paper seems to advise the protection of cables subject to lightning exposure. All cables connected to overhead lines do not require protection. Depending upon the possible magnitude of entering surges, the lengths of cable, and the line and terminal surge impedances, such cables are often self-protecting by reducing the surge voltages to values which they can withstand without damage. For example, two 132-kv oil-filled underground six-mile lines in Chicago are sufficiently long and otherwise meet these conditions, so as not to require arrester protection. Other cable lines operating at 33, 66, and 132 kv, but of relatively short length, have lightning arresters installed at the junction of overhead lines and cables. Although some of these transmission cables have been in operation for as much as 13 to 15 years, no cable, pothead, or joint failures have ever resulted from lightning on either those cables with arresters or the cables that are considered self-protecting.

For the country as a whole, records of several years' operation show that less than one per cent of the total failures on cable systems are caused by lightning. In general, it is necessary to study each individual cable installation in order to determine whether supplemental lightning protective measures are necessary or justified.

With reference to item 8 of section III, it should be pointed out that the desirability of installing arresters in close circuit proximity to the station or substation equipment does not apply quite so rigorously for distribution-line equipment. As indicated in my previous AIEE articles, we have followed for many years the practice of having only one arrester per phase in each block on our four-kv distribution system, even though there may be transformers installed at two or three different places on that phase in the block. Our statistical studies described by E. H. Grosser, Jr., in *Electrical World* for January 27, 1940, showed that an arrester installed one, two, or in some cases three spans away protected the distribution transformer equally as well as an arrester at the transformer pole. This extent of protection might, of course, be lessened for rural systems. It should, in any case, be considered as a means of reducing or avoiding an increase in arrester investment to secure a possibly questionable gain.

This suggests an additional point in the economics of arrester application, particularly as applied to distribution systems. This is the "group" protection of equipment. Where several pieces of electrical equipment are in reasonably close proximity, they may be adequately protected by a single arrester or set of arresters. An appreciable saving in arresters may result from this practice which, as stated above, we have found to be practicable and satisfactory.

Under item 8 also properly belongs the suggestion to change where possible on distribution systems, to the "interconnection" method of protection. The tie between the lightning arrester ground and the secondary neutral of the distribution system involves no critical material other than a few feet of wire, yet has been found to afford a considerable reduction in lightning troubles.

An additional point that appears to be of importance is the maintenance of lightning arrester grounds. The value of low resistance grounds, except for those cases where the ground is not directly involved in the protective scheme cannot be too strongly emphasized. Periodic testing of grounds is advised with reduction in ground resistance being made as appears necessary. Even the use of additional ground rods to improve grounds of high resistance may sometimes be considered justifiable, although semicritical materials may be involved.

W. J. Rudge (General Electric Company, Pittsfield, Mass.): Mr. Gross is to be complimented on his straightforward treatment and presentation of a subject containing such a diversity of viewpoints.

As a representative of a manufacturer, we wish to emphasize the importance of Mr. Gross' statement that

> "It is not the intent of this paper to imply that the suggested methods of protection and practices are suitable for universal application."

We wish to urge that, in each particular case where consideration is given to the application of the suggested measures, the latter be looked at in the light of the following questions.

1. Will protection to important apparatus be improved?
2. Will service to important war loads be improved?
3. Does the use of the small amount of critical materials involved in arrester protection more than offset the risk of damage to important apparatus which involves many times greater amount of critical materials, and which is difficult to replace or requires many man-hours to repair the damage?

The suggestion of using reduced rating of arresters has proved satisfactory on a few well-regulated and well-grounded systems. However, unless the system is well-grounded and well-regulated, experience has shown that the use of arrester ratings below those now listed as standard by the manufacturers for grounded neutral systems will result in an increase in the number of arresters which are damaged by excessive voltage with a corresponding increase in service interruptions, and exposure of apparatus to failure while the failed arrester is being replaced.

Therefore, when contemplating such measures, one not only should be familiar with system operating practices but also should be familiar with the arrester characteristics and application data published both in the AIEE standards and manufacturer's literature. This is necessary to avoid misapplications that would otherwise result in destruction of critical materials and loss of service to important war loads.

When considering the possibility of using line-type arresters in place of station-type arresters, the relative protective levels of the two devices should be kept in mind. A co-ordination study should be made to determine how much encroachment there will be on the margin of protection, particularly when old transformers are involved. The protective levels of both line- and station-type arresters have been presented to the Institute in the form of lightning arrester subcommittee reports. These data show that the *IR* discharge voltage of a line-type arrester rated 73 kv is approximately 25 per cent higher at 5,000-ampere or 20,000-ampere discharge currents than for the station-type arrester. Also, that the discharge voltage (*IR* drop) of the station-type arrester when discharging 20,000 amperes is no higher than that of the line-type arresters when discharging only 4,000–5,000 amperes. It should be realized that the station-type arrester is a more rugged device and has higher discharge current-carrying capacity. It also should be of interest that Thyrite station-type arresters actually use less copper and copper alloy than Thyrite line-type arresters of equivalent rating.

Mr. Gross suggests use of arrester protection on machines which are not so protected at present. This suggestion warrants serious consideration, particularly during the war when replacement of machines, or repair parts involving critical materials, and man-hours for repairs should be kept to a minimum, and when loss of service of the machine is so vitally consequential. The amount of critical materials involved in arrester protection is almost always extremely small as compared with similar or even more critical materials in machines or other apparatus to be protected.

The paper mentions the possibility of using other devices such as protector tubes, open air gaps, or control gaps. To avoid misinterpretation by the user the recognized limitations of these devices should not be overlooked. For example, high-voltage protector tubes will not hold the impulse voltage below the transformer *BIL* value but are suitable for protection of line insulation, disconnecting switches, and so forth. The report "Protection of Power Transformers Against Lightning Surges" presented by the Institute lightning arrester transformer subcommittee states:

> "The rod gap is not fundamentally a good protective device because if selected to protect the apparatus at the long times it will not provide protection at the short times. On the other hand, if it is set to provide adequate protection at the short times, the gap will be so short it will be susceptible to frequent flashover because of switching transients and minor lightning surges."

If the substitution of plain gaps for arresters risks a failure of only one medium-sized transformer, it is doubtful if their use could be justified because the critical materials used in the one transformer would exceed by far the critical materials used in all of the arresters on the entire system to say nothing of the labor required to place the transformer in service, having it rewound, and the possible loss of load to some important industry. For example, one average size, single-phase transformer 5,000-kva, 66-kv, has approximately 660 times as much copper and 120 times as much steel as one single-pole station-type Thyrite arrester rated 73 kv.

In this connection, it is of interest to note that even a small size distribution transformer, say 10-kva 2,400-volt, contains 47 times as much copper and 200 times as much steel as contained on a modern distribution arrester used to protect and preserve the transformer.

In connection with the rebuilding of old arresters, we wish to point out that the old arresters of the aluminum-cell type use decidedly more critical material per kilovolt of rating than modern arresters. The same could be said of the oxide-film type. Therefore, if the suggestion for rebuilding old arresters involves materials which the user already has on hand, it is reasonable. However, if the rebuilding of these older arresters involves purchase of new supply parts, these very likely will require more critical materials than if the old arresters were retired, and their critical materials salvaged and modern arresters using less critical materials and giving more reliable protection are installed.

Mr. Gross mentions the critical materials used in arrester manufacture. In this connection, the General Electric Company has saved approximately 100,000 pounds of aluminum per year by substituting galvanized, fabricated steel fittings for arresters. The critical materials in a modern station arrester amount to only 20 per cent by weight.

We believe that all manufacturers have already complied with the request that the use of strategic and critical materials be avoided where possible, or kept to a minimum quantity where it is impracticable to make substitutions. Mr. Gross has suggested that the internal mechanism of the sealed gap elements be made of iron instead of brass. However, the so-called "nonarcing" property of brass has been recognized for many years as essential for proper arrester operation, and no practicable substitute has been found for this small amount of brass used in arresters.

Mr. Gross mentions testing and the maintenance of arresters, and we concur that regular routine inspection of the equipment with carefully planned maintenance should be made to keep arresters, as well as all electrical apparatus, in the best possible condition as a wartime measure. We feel that testing of arresters may be advantageous in some cases, but it should be recognized that testing not only uses manhours which might be employed to better advantages but also necessitates removal of apparatus from service.

M. C. Westrate (The Commonwealth and Southern Corporation, Jackson, Mich.): This is a very timely paper and might well be titled, "How Lightning Arrester Application Helps the War Effort" since most of the items covered represent current practice regardless of the war.

Under "Reduced Rated Arresters" the application of arresters on a 132-kv system is given as an example, intimating that the rating of apparatus should be five per cent higher. This is not current practice, and it is believed that 138 kv is the preferred system voltage rating.

Protection of equipment not already protected is rather difficult to justify at this time. However, good engineering and common sense would indicate that protection should be added or improved where experience has shown it to be necessary. A word of caution would be well to the effect that protection should not be neglected on new or temporary installations which might be somewhat tempting and which might later prove to be pennywise and poundfoolish.

The testing of lightning arresters is perhaps the most important part of this paper as far as the war is concerned. No doubt many users have arresters which it might be desirable to move or put back in service, provided they can be tested to assure satisfactory operation. Leakage current, voltage breakdown, and power-factor tests have been offered but are not adequate, and any contribution which can be made as to the best way to field test lightning arresters would be very helpful.

H. C. Louis (Consolidated Gas Electric Light and Power Company of Baltimore, Baltimore, Md.): I wish to discuss particularly that section of the paper relating to possible savings from the application of reduced rated arresters. Actual experience on the Baltimore system with arresters rated below the 80 per cent limit has shown some very unsatisfactory results on the solidly grounded neutral systems of the various voltages used by us. These lower rated arresters had been applied in order to obtain maximum protection of apparatus especially of older types. Losses of these arresters were such, not only during lightning storms but also at times when there were no disturbances, as to result in unnecessary service interruptions.

A lightning arrester should provide maximum protection with minimum interruption of service. The degree of protection should be balanced against service interruptions caused by arrester failures. The Baltimore experience definitely indicated excessive arrester losses with needless service interruptions from the application of arresters rated below 80 per cent. On the other hand, modern arresters rated at 80 per cent are apparently providing the proper degree of protection to apparatus, and application of these to solidly grounded neutral systems is now our standard practice.

We are, of course, sympathetic to actions contributing to conservation of material and labor and the war effort, but these should include a consideration of all factors. The very small original savings of material and labor from the use of reduced rated arresters may be offset many times by needless arrester losses and service interruptions possibly affecting war production.

E. E. Piepho (The Detroit Edison Company, Detroit, Mich.): This paper would have been timely before the war; it is doubly so now that we must conserve materials needed for war production. Such conservation should be exercised when devising new installations of lightning arresters. It can be realized also by providing better lightning protection for existing equipment, thus prolonging its life and by rebuilding old-style lightning arresters to improve their effectiveness.

Since lightning arresters are usually connected between line and ground, they are required to withstand only the system voltages which may occur between line and earth at the point of connection. These voltages may be predicted with reasonable accuracy for normal conditions and for various fault conditions on the system. Methods for calculating line-to-ground voltages during system faults have been ably discussed in the technical literature in the past few years. Consequently, there should be little difficulty in applying arresters to withstand maximum system voltage conditions.

Early application of arresters was determined by the line-to-line voltage. This may be correct for systems with ungrounded neutrals, or with neutrals purposely grounded through reactance or resistance; but it is a very inefficient application for solidly grounded systems. Realization of this fact led to the eighty per cent rule, whereby arresters rated only eighty per cent of the line-to-line voltage could be used on solidly grounded neutral systems.

If it were possible to stabilize earth potential at the system neutral, this could have been a sixty per cent rule, since line-to-neutral voltage is only fifty-eight per cent of line voltage. This may be possible in stations where grounding transformers are located, but calculations usually show more or less shift of earth potential from neutral for certain fault conditions. It is not at all uncommon, however, to find that seventy to seventy-five per cent arresters can be used on a solidly grounded system with no greater risk than using full-rated arresters on an ungrounded system. Better protection may thus be provided with some saving in arrester materials.

For the past ten years The Detroit Edison Company has used arresters on its 120-kv and 24-kv solidly grounded systems with ratings ranging from sixty-five to eighty per cent of line voltage. The failure rate of these arresters has averaged approximately the same as for the six-kv arresters used on our ungrounded five-kv distribution system. These latter arresters, you will note, are rated 120 per cent of line voltage.

Two years ago, in order to improve voltage regulation, we changed over a part of the 24-kv system to 41.6 kv by reconnecting substation transformer banks from delta–delta to wye–delta. Since the transformers were insulated for 24 kv, it was necessary to use the lowest rated arresters possible in order to provide adequate protection. The neutrals of transformer banks were grounded at various points on this system so that the maximum line-to-ground voltage under fault conditions would not exceed 120 per cent normal, which made it possible to use 30-kv arresters. Up to the present there have been no transformer or arrester failures.

Considerable saving of arrester materials can be realized by salvage of old equipment. Older style arresters which have ordinarily been discarded because of low insulation as shown by Megger test, can be renewed by cleaning insulating parts such as the wood supports of oxide-film arresters, or in the case of autovalve arresters, cleaning gap parts, wiping dust off the carborundum discs, and replacing mica spacers. Leaky hardware seals can also be renewed by chipping out cracked cement with an electric-driven chipper and replacing the cement after applying a sealing coat of asphaltum paint.

When reconditioning old arresters, we take the occasion to rebuild them for reduced rating in accordance with present practice. For this purpose, the arrester rating is calculated by allowing 300 volts per cell of the oxide-film arrester and 170 volts per mica-spaced gap of the autovalve arrester.

Protector tubes which have eroded beyond their current requirements, but not to destruction, can be renewed by removing the hardware, and replacing the inner fiber tube and fused electrodes with parts of original dimensions. This can be done easily with a substantial net saving of material.

Older types of apparatus such as street-lighting switches, air-blast regulators and transformers, and generators or motors with old insulation can be kept in service by improved lightning protection. We have been able to continue use of an old-style street-lighting switch by applying additional lightning arresters. Specifically, this required a low-cost arrester close to the vulnerable parts. Old air-blast induction regulators have been kept in reliable service by the use of protective condensers and arresters on the line side. This is expensive protection but relatively cheap compared with repair or replacement cost of such regulators. Similar protection has been used successfully on old generators and motors whose insulation level is too low for ordinary arrester protection.

The methods which have been described are only a few of the many ways whereby critical materials can be saved. Some of these methods can be made available to our customers as well. Ordinarily, we can afford to follow broad policies in the application of arresters, but these are the times when we must consider such applications in greater detail, especially as they affect the materials now in great demand.

I. W. Gross: The use of reduced rated arresters has received considerable comment, some of it critical and some going even further in applying reduced ratings than outlined in the paper. We therefore find ourselves faced with the situation where Mr. Beck is suggesting applying arresters in some cases with a rating higher than normal line-to-line voltage of the system; and Mr. Rudge seems skeptical about applying reduced rated arresters below the 80 per cent level given in manufacturers' literature. Mr. Piepho, with satisfactory experience, uses arresters as low as 60 per cent of full rating.

The principle is laid down in the paper that for best results arresters should be applied on the basis of the maximum 60-cycle "voltage expected across their terminals in service." In our first extensive application of arresters on this basis, we disregarded the manufacturer's rating of the arrester and got down to the real fundamental, which was "What was the safe 60-cycle voltage across the arrester?" We then selected an arrester rated not less than this and, with the courage of our convictions, made extensive changes on our 132-kv system accordingly. While this change was made largely on the oxide-film arresters, we also started using the newer or modern type on the same basis at that time. It is interesting to note that our experience on our extensive 132-kv system has been that where these reduced rated arresters were applied as above indicated, we have never experienced in the last twelve years a failure of an arrester or lightning failure of equipment protected by such arresters.

The point which it was intended to emphasize in the paper was that, if arresters are applied on the basis of voltage expected across their terminals in service, considerable savings in arresters, arrester and transformer materials, and equipment failures might be possible. That principle it still is believed is sound, and the experience of others (Piepho, Lewis, and Halperin, and our own) seems to bear this point out completely. In other words, results of actual practice seem far to outweigh the speculative fears and doubts of Rudge and Beck.

Mr. Beck apparently has the mistaken idea that too much emphasis was given in the paper to the savings that look attractive through reduction of arrester ratings and the use of smaller types. It should be pointed out, however, that there was no intent to put undue emphasis on any one of the nine items presented and discussed in the paper, and I am at a loss to understand why he feels that emphasis has been placed in particular on any one of them. The intent was merely to present the different items of possible savings, discuss them, and leave to the reader the constructive use of any one or more of them as he found it would produce material benefits to his system and as an aid in saving critical materials.

On the subject of using line-type distribution arresters in the place of station-type Mr. Bellaschi mentions that the 80 per cent arrester is predicated on its not being called upon to discharge excessive surge currents. This factor of high discharge current capabilities of arresters seems to the writer to be somewhat overdone. Actually, experience, so far as we are aware, indicates that the maximum lightning current in distribution arresters out on lines is something in the order of 35,000 amperes. At stations where a large amount of equipment is usually located, the highest lightning current discharged by arresters is in the order of 15,000 amperes. Both of these currents are well within the capabilities of present-day distribution line-type and station-type arresters. There should be no question, therefore, of any serious trouble from failures of modern arresters because of excessive lightning currents.

Mr. Rudge seems to be averse to using line-type arresters in the place of station-type and predicates his position on the published characteristics of the arresters showing that station-type arresters have a lower protective level than the line-type and also higher capabilities of lightning current discharge. As mentioned above, the lightning discharge capabilities of modern arresters are certainly not important, even if they should be considered at all. Regarding the second point of voltage levels held by the two types of arresters, it might be well to consider the fact that distribution-type equipment in general up to the 15-kv class inclusive has a lower impulse strength than similar equipment in the so-called power class. Yet we find the power equipment used at stations where Mr. Rudge would apparently recommend using a station-type arrester holding the level below the distribution arrester. That is, he is adding two factors of safety at the station where the power class of equipment has an impulse level higher than the distribution class, and the station-type arrester holds the level below that of the line-type. This point of view it is hard to reconcile.

In viewing this condition further, we note that lightning conditions of severity are apt to be greater at distribution equipment on the line than at power equipment in the station. A setup such as this seems decidedly illogical, and we cannot help but feel that in the future more consideration should be given to applying less expensive arresters in a station than is now currently recommended by catalog literature.

We have spent a great deal of time, money, and effort in the past in determining lightning conditions in the field as they actually appear at or on different pieces of equipment, and if we are not in a position today to do a better job of applying lightning arresters on the basis of their 60-cycle rating and their protective characteristics and obtaining improved protection of equipment, we lay ourselves open to the accusation of negligence, at least, in not applying the knowledge we have gained from the last fifteen years or so of field research on natural lightning as it affects the transmission system.

Mr. Halperin's point that long cables are self-protective is very well taken, and we are glad to note that his experience bears out the theory on this point. Also, his scheme of protecting block areas by one arrester is also an ingenious application of arrester protection on an economical basis. Planned foresight and courage in applications of this kind to determine from experience what results may be obtained are certainly to be encouraged. In the paper the reference to locating arresters one pole distance from a transformer perhaps should have been amplified to indicate that the type of setup in mind was confined to single banks of transformers where an arrester was applied to supply protection only to such a bank. Where such applications are made, it is considered desirable that the arrester be placed at the transformer itself rather than a pole distance away in accordance with somewhat ancient practice.

Mr. Rudge has commented on other protective devices such as protector tubes, open air gaps, and control gaps tending to indicate that they do not supply as good protection as lightning arresters. The purpose of the paper was not to discuss the relative protective merits of the devices themselves, but merely to indicate that where protection is required, there are other devices that can be used and have been used successfully in addition to lightning arresters. All of the devices are known to have been used in certain cases of application with success. The comparative protective features of these devices can be found in engineering literature and, if applied on a sound basis, should give some degree of protection.

Mr. Westrate takes exception to the use of the time honored system of voltage rating, namely, 132 kv, and says that this is not current practice but that "138 kv is the 'preferred system voltage rating.'" The paper referred to a "132-kv nominal system." In our company we have adopted the standard of using the 132 kv as the *nominal system voltage*. This practice has numerous advantages and is quite in line with the National Electric Light Association publication 043, April 1930, entitled, "Preferred Voltage Ratings for A-C Systems and Equipment," as they also define the "nominal system voltage is for convenience and designations but not as a basis for design." As a convenience for references on drawings, reports, and so forth, this practice which is being used by us at the present time is much more convenient and less readily misunderstood than the 138-kv type of designation.

The point brought out by Mr. Piepho that by better application of arresters where old equipment is involved, such equipment can be kept in service longer, is very well taken.

The author is most appreciative to those who have offered discussions on this paper in bringing out and emphasizing some of the points which appeal to them as being important either in the way of supplying supporting data or in emphasizing what precautions should be taken in applying some of the suggestions covered by the paper. As mentioned in the paper itself, it was intended to have this material published as a committee report, and it may be clear to some of the readers of the paper from the discussions why it was impossible to get agreement among some dozen lightning-protection specialists on a subject where there are so many various factors involved.

The Cathode-Ray Oscillograph Applied to Long-Time Switching Transients

Discussion and authors' closure of paper 43-35 by G. W. Dunlap and N. Rohats, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, May section, pages 231–4.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The authors have given a very thorough description of the cathode-ray oscillograph which has been found to be very useful in the high-voltage engineering laboratory of the General Electric Company, both for laboratory and field tests. Its particular use, of course, is preferable to the magnetic oscillograph wherever transients have to be measured at points where only high impedances can be connected without distorting the phenomena to be measured.

It would seem of interest to mention that the circuit diagram of Figure 4 of the paper can be considerably reduced in the number of tubes as shown in Figure 1 of this discussion. As can be seen, two thyratrons have been omitted from this circuit. The remaining thyratron serves both as an initiating device for the cathode beam as well as the switch for the relay operation to shift the film drum. By this means the total power required is of the order of 130 watts. Consequently, this oscillograph can be used as a battery-operated device. This also has been indicated in Figure 1 of this discussion, the conversion from the battery voltage being obtained by a dynamotor. This allows the oscillograph to be used on isolated circuits, measuring line currents directly without use of a current transformer.

The use of one initiation tube only requires that a unidirectional initiation impulse is changed to an oscillatory transient of relatively high frequency. This has been accomplished in some cases by means of current transformers so that the oscillograph could be initiated regardless of polarity within five microseconds. Since the oscillograph is intended to measure long-time transients only, the loss of the first few microseconds is unimportant.

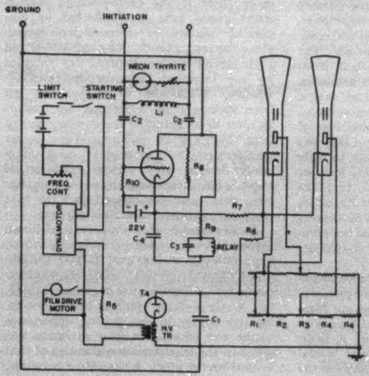


Figure 1. Oscillograph connection for battery operation

In the laboratory the instrument has been found useful in the measurement of voltage distribution on capacitance devices during flashover, sixty-cycle flashover, and hold tests. Other uses for that kind of instrument would be to evaluate the voltage distributions within the windings on certain types of transformers during switching disturbances which may have a frequency of several thousand cycles.

This instrument has become a very necessary and useful complimentary tool to the magnetic oscillograph.

W. F. Skeats (General Electric Company, Philadelphia, Pa.): The authors are to be congratulated upon developing a conveniently portable tool for measuring switching transients of relatively low frequencies. The oscillograph has proved convenient to work with, while at the same time giving very satisfactory results.

It has for some time been appreciated that the cathode ray oscillograph together with a potentiometer could be expected to perform more accurately on high frequency transients than the magnetic oscillograph in combination with a potential transformer. The lower curve of Figure 6 serves as an indication that the same thing is true for very low frequency transients. It will be noticed that at the time of interruption a unidirectional voltage of about twice normal crest value remained on the line for a period of several cycles. This could not have been recorded with any degree of accuracy with a potential transformer, but it was recorded with negligible error by the potentiometer oscillograph combination which was employed.

G. W. Dunlap and **N. Rohats:** The authors wish to thank the discussers, W. F. Skeats and J. H. Hagenguth, for their contributions. The oscillograph described was originally designed for operation without attendance. Therefore, if built for ordinary use, some further simplification in addition to that mentioned by Mr. Hagenguth would be possible. The automatic film drum shifting devices could be eliminated in favor of hand shifting or a lighter and smaller single record film holder. Also a third tube could be added, if there were no objection to a fixed displacement along the time axis of this third tube record.

Cooling Power Transformers by Forced Circulation of Cooling Medium

Discussion and authors' closure of paper 43-56 by K. K. Paluev and L. H. Burnham, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, May section, pages 215–21.

W. C. Sealey (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The use of forced-oil cooling of transformers is old, but in order to save critical material it has been adopted for cooling a large number of transformers built recently. This increased use has stimulated development, so that modern forced-oil cooling overcomes many of the objectionable features of former systems. The most modern of these developments consists of a totally enclosed motor and pump in one housing to circulate the oil through an oil-to-air heat exchanger. This modern cooling unit is entirely self-contained and self-supporting by its piping, eliminating the usual plumbing and auxiliary fastening of the unit to the tank or separate supports.

The authors' attention is called to the fact that the advantages which they claim or infer as applying only to core-type transformers apply equally well to shell-type transformers, so that it is practicable to build transformers either with directed or nondirected flow core type or shell type. As a matter of fact, such transformers have been built and are being built both core type and shell type.

Henry M. Hobart (consulting engineer, Schenectady, N. Y.): Several transformer manufacturers at present are advocating a metal-sealed type of power transformer and forced circulation of the cooling medium and are supplying excellent power transformers whose designs embody the required features. A principal object is stated to be the assisting of the war effort by decreasing the amount of critical metal required. The precise arrangements employed by the different manufacturers differ from one another in some respects, such as in the extent to which the forced flow of the medium is directed. In all cases, however, the circulated medium is either a mineral oil or some synthetic liquid which is considered better than mineral oil, particularly with respect to being noninflammable and nonsludging. Several such synthetic liquids have been developed for power-transformer use. Among these may be mentioned: Asbestol, Chlorextol, Inerteen, and Pyranol.

The authors of the paper under discussion, Paluev and Burnham, do not even mention the use of compressed gas as the circulating medium. It must be presumed that they do not regard compressed gas as being a practical alternative. For many years, the writer has found it difficult to account for the extraordinary devotion displayed by experienced transformer specialists for mineral transformer oil and the synthetic noninflammable nonsludging alternatives. The ordinary, and widely employed mineral transformer oil requires that the temperature of the oil shall, in service, never be allowed to exceed 90 degrees centigrade at any part. Higher temperatures will occasion sludge formation, impairing the heat transfer, clogging the ducts, with consequent further temperature increase and accentuated sludging. The noninflammable synthetic liquids are rather expensive and they leave much to be desired in several important respects. They are solvents of some of the gums, binders, and other materials which it has been customary to use in transformers intended for immersion in mineral oil. Consequently, transformers destined for use with these synthetic liquids must be specially built with more suitable insulating ingredients. There is, however, one very important advantage, namely, that the use of a noninflammable synthetic liquid permits load-center location of the transformers, close to the load, in industrial premises, because the fire hazard is decreased greatly. Although sludging is absent in these synthetic liquids, higher temperatures than are permitted with mineral transformer oils are not recommended.

The pumps, fans, and heat-exchanger materials required for the forced-oil forced-air functions described by the authors require a greater capital outlay and also a greater outlay for the power consumed in the performance of these functions than would be the case were compressed gas the circulated medium. The total losses in the transformers are materially increased, with a consequent impairment of the efficiency. The noninflammable synthetic liquids, which have been mentioned previously, have about twice the specific gravity of mineral transformer oil, and about twice the dielectric constant. This much higher dielectric constant decreases the resistance to withstand impulse voltages in designs built with a given amount of shielding provision for lightning protection.

But in a *compressed gas* transformer we are dealing with a circulating medium whose dielectric constant is unity, so that in respect to the shielding provided, we obtain, with a *gaseous* medium, a *maximum* of immunity from lightning damage, so far as such immunity can be provided by designs with electrostatic shielding.

The use of *gas* instead of *oil* removes the need that, in service, a low operating temperature shall be maintained. Indeed, if, by employing suitable thermal barriers or screens, we arrange to maintain the transformer core at a temperature very much higher than the temperature of the windings, the *core loss* will be *decreased* by an important amount. This feature provides an extraordinary structural harmony if we employ certain gases which, when maintained at the high compressions corresponding to great dielectric strength, would condense, if the temperature were not maintained above their liquefaction point for that compression. SO_2 is an example of such a gas. At a compression of 25 absolute atmospheres, the condensation temperature of SO_2 is 98 degrees centigrade. Assuming a directed and upward flow of 25 absolute atmospheres SO_2, through the winding ducts and over the winding surfaces, it may, in a suitable location above the hot portions of the transformer, be condensed in the presence of a decreased temperature and then, by gravity, may be returned to join the reserves of 98-degree-centigrade liquid SO_2 in a low-level compartment, ready for its next round-trip journey, during which it will be again successively vaporized and condensed.

In the performance of these functions, pumps, fans, heat exchangers and directing and baffling elements may be employed to any required extent, in a manner similar to that described by the authors in their paper. But, as already stated, the cost of these materials and the amount of power involved in their functioning will, with the compressed gas, be far less than the amount required for the oil (or synthetic liquid) designs described by the authors.

For the air-blast type of power transformer (which was widely and successfully employed throughout a long term of years), some designers used the rough rule that there was required a flow, through the core ducts and winding spaces, of 1,000 cubic feet of air per minute, per 1,000 kva of the transformer's rated output. Corresponding to these conditions, the air rise between inlet and outlet was about 20 degrees centigrade. The corresponding temperature rise of the windings, as determined by resistance measurements, was some 55 degrees centigrade. If the circulated medium had been SO_2 at a pressure of 25 absolute atmospheres, instead of air at atmospheric pressure then, since SO_2 has 2.2 times air's density and a specific heat which is 0.64 times the specific heat of air, the SO_2 would have been $(2.2 \times 25 \times 0.64 =)$ 35.5 times more effective for heat transfer per unit of volume than was air. Instead of requiring a volume of 1,000 cubic feet per minute per 1,000 kva of rating, only 28 cubic feet per minute would have sufficed, for the same temperature rise. But since, in this *compressed-gas* design, we are free from any consideration for the temperature limitation imposed by the necessity of avoiding oil deterioration, we may operate the transformer at a *much higher temperature*, if all our solid insulating supports and spacers are of heat-resistant materials. For these purposes, plenty of excellent heat-resistant materials are nowadays available, such as asbestos, glass tapes and sheets, Mycalex, Micanite, and lava compositions. If, instead of circulating 28 cubic feet of compressed SO_2 per minute per 1,000 kva of rating, we reduce the amount to only one third as much (say, nine cubic feet per minute per 1,000 kva of rating), then the increase in the temperature of the SO_2 on its arrival at the outlet will be about 60 degrees centigrade, thus bringing the final temperature of the SO_2 up to (98 plus 60 =)158 degrees centigrade.

The dielectric strength of atmospheric-pressure air is only 79 crest volts per mil. This was so low that, in these air-blast transformers, the air could not be used as insulation. It served simply as a heat-transfer medium. Hence, it was necessary to provide solid insulation between the component windings and between windings and ground. That accounts for the inadequacy of these otherwise excellent power-transformers for use at voltages of more than some 20,000 to 30,000 volts. In other respects, such as light weight, economy of floor space, and low cost (during that period), they outclassed the oil type. As a fire hazard their status was low, but that was at least equally true of the oil type. Neither type was suitable for use at load-center interior locations in industrial premises, unless they were enclosed in expensive fireproof vaults or compartments.

But a *compressed-gas* power transformer should be an ideal type for interior load-center location in industrial plants.

SO_2, compressed to 25 absolute atmospheres, not only is a heat-transfer medium many times more effective than air, per unit of volume, but it has a 60-cycle dielectric strength seven times greater than that of transformer oil, and a $1^{1}/_{2}$-40 impulse strength 3.5 times that of transformer oil. If it is desired to provide a still greater degree of superiority over transformer oil, we can increase the compression to 50 absolute atmospheres or more, instead of using a compression of only 25 absolute atmospheres.

Paluev and Burnham include in their interesting paper, a number of curves which provide qualitative indications of trends which will be encountered if their recommendations are followed. But no quantitative information applicable for comparison purposes is made available as regards the net effects of their recommendations

1. Losses.
2. Efficiencies.
3. Weights.
4. Costs.

Fortunately, however, a limited amount of such information has been made available in the technical press, from time to time, by several of the manufacturers of the types of transformers with which the paper is concerned. Whereas this published information is rather obscure as regards losses and efficiencies, thoroughly worthwhile savings in critical metals are definitely announced. Considerable and important as are these savings, they fall seriously short of the corresponding savings which could have been effected by employing suitable *compressed-gas* designs along the lines advocated by the writer, and there would not be incurred any sacrifice by any increased losses or decreased efficiency. While the writer has not yet been successful in interesting any manufacturer in undertaking the development of large high-voltage compressed-gas power transformers, he has made a good many study designs with various fundamental types. His experience, up to this time, has been that (with compressed gas) the best results are attained with the shell type.

As an example, it is of interest to cite an exploratory *compressed-gas* design for a shell-type 60-cycle 20,000-kva, 80,000 (phase) volts 138,000 (system) volts single-phase power transformer for use in groups of three, on a three-phase transmission system. Because of the predominating importance of the war effort, particular attention was given to the maximum conservation of that critical metal which is stated to be of the greatest military importance, namely copper. The amount of copper was reduced to only one-third of the amount heretofore usual in oil-type transformers of this rating. The result was accomplished with no increase in the customary lamination weight. If desired, and employing the same stacking height, and with not more than 5 per cent increase in the gross cross section of the winding window, the copper windings could have been replaced by aluminum windings. The heat transfer from the numerous vertical thin pancake coils is effected by the circulation of the compressed gas over the entire extent of both flat sides of each pancake component.

The exploratory design already briefly reviewed is simply one example of many attractive approaches to the subject. When the inertia of present accepted design conventions is overcome, many skilled designers and constructors (among whom the writer expressly includes the able authors of the present paper) will conceive and embody in *their* compressed-gas power transformers their own ingenious and excellent ideas.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): The authors have done an excellent job in showing how it is possible to increase greatly the output of transformers as well as in showing how to conserve critical materials by forced circulation of cooling mediums. In fact, there are so many ways whereby the rating of transformers can be increased that there is likely to be considerable confusion as to which of the available methods of cooling is best suited for obtaining maximum output under the various conditions.

The purpose of this discussion is to outline the conditions for which each method of cooling appears to be best suited. There will, of course, be exceptions to the conditions outlined. Each application should receive careful consideration before the method of cooling to be used is selected.

Various methods of cooling conditions for which they are suitable are:

1. *External water coolers with nondirected oil flow through windings*

(*a*). Use limited to attended stations.

(*b*). Can be used for both new and old transformers.

The output can be increased to 125 up to 167 per cent of the rating, depending on design of old transformers and on size of cooler provided for new transformers.

(*c*). It should be used only where the cost of water is low.

2. *External air coolers with nondirected oil flow through windings*

(*a*). Use limited to attended stations.

(*b*). Use limited to large-size units.

(*c*). 167 per cent of rating for new units.

(*d*). 125 to 167 per cent of rating for old units depending on design.

3. *Directed oil flow with external air or water coolers*

(*a*). Use limited to attended stations.

(*b*). Used for locomotive liquid-filled transformers and suitable for mobile units (with air coolers).

(*c*). Best suited where critical materials must be conserved in new units.

(*d*). Best suited where desirable to ship large units assembled in own tank ready for immediate installation.

(*e*). Cannot conveniently be applied to old units.

4. *Cooling with large high-air-velocity fans (nondirected oil flow)*

(*a*). Only means available for outlying unattended stations. Can be used at attended stations.

(*b*). Best suited for obtaining maximum continuous output for small and medium-size units on account of low cost.

(*c*). Suitable and sometimes preferred for large units on account of flexibility in ratings ranging from 100 up to 167 per cent of the self-cooled rating depending on the number of fans in use.

(*d*). Suitable for both new and old units.

5. *Water-spray cooling (nondirected oil flow)*.

(*a*). Lowest costs for obtaining maximum output (of old units).

(*b*). Best suited for old transformers where rare high emergency overloads are likely to come on.

(*c*). Not suitable for continuous use on account of rusting of tank, stoppage of nozzles, and sloppy conditions around transformer, floor, and so forth.

F. J. Vogel (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): In discussing this paper, I would like to give the history of the first oil-circulation-cooled transformer, as far as I know it. Forced cooling has been used in Europe and in other countries, but no data were available as to its value, and it was considered generally that little benefit was gained from it. This was before 1929. Along about that time, railway-locomotive transformers had occasional insulation troubles. These transformers were mostly dry type with air-blast cooling. They were built shell type by most all manufacturers.

Oil-insulated transformers were thought to have several advantages. Moisture and dirt would be excluded from the windings, and the service records should be improved. However, oil-insulated transformers that had been designed previously were heavy and larger than could be permitted. These previous oil-insulated transformers had thermosyphon cooling within the windings. Data which were available, as for example, that in a paper, "The Cooling of Electric Machines," by George E. Luke, presented to the Institute in June 1923,[1] showed that a considerable gain in heat omission from coil surfaces was possible by forced cooling. For this purpose, the shell-type transformer was considered ideal. The transformer would than be similar to the air-blast transformer previously supplied except that it would be in oil. It was found that this transformer could be designed to be about as small and light as the previous air-blast transformer with the advantage of eliminating the dirt and moisture trouble previously experienced. As stated in the paper, these locomotive transformers were required to be of a design suitable to be built by both the General Electric and Westinghouse companies, but the design was initiated by the Westinghouse company. These transformers provided additional design data which could be useful for other applications.

For many years no application of these data was seen for other types of transformers. In the first place, operators thought that the reliability would be less. Several times the proposition was made to use modified forms of forced cooling, and they were always objected to. Another obstacle to their use was the fact that the benefit from forced cooling decreased size, increased the losses, and decreased short-time overload capacities. The first application of forced cooling other than for locomotive use was for portable substations mounted on trailers which could be moved from place to place. The first applications of this were by the company with which I am associated.

Times change, and in war materials may be of more importance relatively than other considerations. The forced-cooled transformer does use less material for a given rating, and this has stimulated its use.

In June 1941 a paper was presented to the AIEE in Toronto, by Philip Sporn and H. V. Putman,[2] describing a transformer very similar in design to the original locomotive transformers. A comparison of this paper with the paper by Paluev and Burnham shows many of the design features described, although the paper does not go into technical details. A discussion of this paper by myself at the meeting brought out the point that hot-spot temperatures are greatly reduced in transformers with forced circulation and could permit a higher temperature rise for this reason. Most of the technical points which are covered in the paper by Paluev and Burnham are obvious and were recognized at the time the first locomotive transformers were built. The principal contribution of this new paper is to call attention to the fact that core-type transformers as well as shell-type transformers can be forced-cooled. It is evident that changes in design of the core-type-transformer windings are being made to gain both better insulation properties and better cooling. These changes result in arrangements giving results more similar to those obtained naturally in shell type transformers. It is not conceded that zigzag oil flow through concentric windings, as shown in Fig. 15 of the paper, is a natural arrangement for forced-oil cooling.

Figure 4 and Figure 7 of the paper show

Figure 1. First portable forced-cooled transformer mounted on a truck as a complete substation

some of the original designs used for the locomotive transformers. The success of this design was greatly contributed to by Mr. Brockman, of the Westinghouse company, for the transformer, and Mr. Jungk, also of the Westinghouse company, for the design of the pump and motor. Figure 1 in this discussion shows what I believe was the first portable forced-cooled transformer mounted on a truck as a complete substation.

REFERENCES

1. THE COOLING OF ELECTRIC MACHINES, G. E. Luke. AIEE TRANSACTIONS, volume 42, 1923, pages 636–51.

2. A NEW TRANSFORMER FOR BASE-LOAD STATIONS, Philip Sporn, H. V. Putman. AIEE TRANSACTIONS, volume 60, 1941, October section, pages 916–18.

W. G. James (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): I have read this paper with considerable interest and wish to compliment the authors on their treatment, clarity, and complete handling of this subject which, indeed, is very timely because of the present-day necessity for conserving critical materials.

As a designer of power transformers for many years with the Westinghouse Electric and Manufacturing Company, my interest in this paper is twofold:

1. I wish to learn how other specialists in the field approach and solve such a design problem.

2. My interest has been heightened by the fact that the Westinghouse company has approached and solved this same problem for more than two decades in essentially the same manner. In fact, one of the main reasons why we selected the shell form of construction for large power transformers was that, in our opinion, thermal problems, both thermosiphon and forced-oil flow, could be treated more simply with this form of construction than with other forms of construction, such as described by the authors of this paper.

Thermal problems of this kind, although complicated, as is obvious from this paper, are well understood by the manufacturers of such equipment. The general design problem is one of evaluating and co-ordinating properly thermal problems with those of equal importance, namely, dielectric and mechanical features. A chain is no stronger than its weakest link. As a general rule, the less complicated the mechanical design, the simpler the solution of the thermal and dielectric problems.

In this connection I believe it should be permissible to point out that the transformer shown in Figure 4 of this paper is such a design and is of the shell-form construction. The oil ducts between coils are large, short, relatively straight, and oil flow is well balanced in parallel ducts. This assured adequate oil velocity parallel to both sides of each coil for a minimum pump capacity. Other forms of construction, I believe it will be conceded, were found to be thermally inadequate for this application, because of imposed space and weight limitations.

Also, I wish to point out that the oil-cooled glandless pumps shown in the two top views of Figure 7 of the paper were developed by the Westinghouse company for railway applications such as shown in Figure 4. These motor-pump assemblies have been so successful that, even though they were initially developed and applied many years ago, they are being applied extensively today by Westinghouse where forced-oil circulation is required for cooling transformers.

It should be pointed out also that as early as 1923 the Westinghouse company promoted the idea of forced-air forced-oil cooling and has steadfastly promoted it down through the years since 1923. The fact that the trade generally did not accept such cooling until the present emergency does not mean that the idea is a new one. As evidence of this fact I wish to refer to a paper entitled "The Cooling of Electrical Machines" as presented at the annual AIEE convention at Swampscott, Mass., June 26–29, 1923, by George E Luke. The material contained in this article serves equally well today in solving the intricate problems involved in forced-air forced-oil cooling such as outlined in the paper by Paluev and Burnham.

Briefly stated, the greatest saving of critical material can be obtained by the use of a structure in which the thermal drop through the conductor insulation, coil surface drop to oil, hottest-spot margin, and oil rise do not jointly exceed a hottest-spot copper rise of 65 degrees centigrade for the maximum watts per unit area of coil surface. Or, stating it another way, the faster the heat is carried away from the coils, the greater the kilovolt-amperes for given physical parts and copper rise.

I believe the so-called "Modern Concentric Winding" (reference 2 in this paper) described by the authors was proposed and used many years ago by C. L. Fortescue and J. F. Peters for dielectric rather than thermal reasons.

The last statement in this paper to the effect that "The core and coils of a typical self-cooled high-voltage 10,000-kva transformer, with 55 degrees centigrade average winding rise and 65 degrees centigrade hot-spot rise, when arranged for directed forced-oil flow, will carry continuously 16,667 kva and meet the same temperature limits" is of particular interest. This implies that the ratio of losses at the self-cooled rating must be very low—the thermal drop $(TS)_2$ through conductor insulation will, for directed forced flow, be 1.667 times that for thermosiphon cooling, and the coil surface drop (TO) will go up by the same amount reduced by the average ratio given in Figure 13 of the paper. We agree with this idea except, that, from our experience, the curve in Figure 13 appears to us to be quite optimistic as regards the advantage to be gained from relative oil flow—thermosiphon to forced circulation. Another feature which has not been discussed in this article is the effect of oil turbulence as well as direct flow. Oil turbulence is very effective in leveling off hottest spots due to reduced oil flow for the various reasons suggested in this paper.

I believe the data in Table I of the paper are misleading, because obviously the total losses and ratio of losses are not the same.

K. K. Paluev and **L. H. Burnham:** With reference to Sealey, Vogel, and James's discussions, it was not the intent of the authors to convey or leave the impression that forced cooling could be applied only to core-type transformers. Rather, the intent of the paper was not only to show how forced cooling could be applied successfully to the core-type transformer, but also to indicate some of the general problems involved in cooling the various kinds of transformers by this method. Forced cooling is not confined to core-type construction but is applied also to shell-type designs, as found in Figure 4 of the paper, which illustrates an electric-locomotive shell-type transformer. Shell-type construction will be used whenever that particular type is most applicable.

It is obvious that many methods of cooling transformers are now available and that each one has advantages under certain conditions of application and operation, but that the greatest economy in space and materials will be found in the forced-cooling method of directed flow of the cooling and insulating mediums over the windings and cooler surfaces.

After this war undoubtedly some of these various methods of cooling power transformers may be discarded. Simplicity of design, application, and economics will be the deciding factors.

Because of the war, the maximum saving in critical materials, such as copper and iron, became of paramount importance, and this very efficient method of cooling power transformers was quickly adopted. It is here to stay, especially in large capacity units, where the benefits to the user are great, as enumerated in the paper.

Mr. Luke's paper, referred to both by Mr. Vogel and Mr. James, is an excellent paper, but in general covers the application of forced cooling to rotating apparatus.

However, Luke himself made no original tests with oil but prepared his curve from results of tests made by S. E. Derby as reported in "Recooling of Lubricating Oil From Turbine Bearings and Gears."[1] Upon examination of this source, it is found that Derby's tests were made on "an oil cooler in which the hot oil is circulated around straight tubes through which cold water flows." Therefore, Luke's curve did not apply directly, since his data and calculations were obtained from an irregular flow around pipes, and not through them.

Prior to Luke, the best source of information which did apply directly to the subject of surface-heat-transfer coefficient for oil flowing in pipes, is "Heat Transfer by Conduction and Convection—II. Liquids Flowing Through Pipes," by W. H. McAdams and T. H. Frost, published in February 1923, by Massachusetts Institute of Technology.

More recent data were secured by our laboratory and in part, has been published by R. H. Norris.

In regard to priority in actual design and application of oil-air coolers to cooling of transformer oil, as pointed out by the authors, *one of the first, if not the first* installation for forced cooling of transformer oil by forced flow through tubular finned-tube oil-air radiators, is shown in Figure 3 of this paper. These oil-air coolers were designed and built by the General Electric Company in 1925. They were installed in 1926 and were used to cool the oil, by forced circulation, in several 25,000-kva transformers built by another manufacturer.

The heat-transfer data on which this particular cooler design was based, were obtained by test in 1924 and 1925, using finned-tube radiators as the oil cooler.

This efficient method of cooling was not accepted at that time, nor to any great extent in the years following, until the start of World War II, when the demand for great savings in the active material, copper and iron, became imperative. During the intervening years from 1924 and 1942, much developmental work was carried on and further heat-transfer data obtained on various kinds of cooler tubes and cooler and winding designs.

In answer to Mr. Vogel's discussion, the pumps shown in Figure 7 of the paper illustrate the various designs used to eliminate the standard stuffing box which always has been a source of trouble with either oil leakage or suction of air into the cooling system. Immersion of the motors in the cooling and insulating liquid is not a new idea, but the practical application of the various pump designs shown did solve several very troublesome problems.

Simplification of the pump design, service records, and economics finally will be the deciding factors in the choice of the most suitable pump design.

With reference to Mr. Hobart's discussion, the authors congratulate him on the thorough manner in which he treated the subject of compressed-gas-filled transformers. However, it should be pointed out that the scope of our paper was purposely limited to transformer construction already reduced to practice. On another hand, it appears that the problem of applying compressed gas in a practical way is far from being solved. It is of such proportions that much additional developmental work is required before an economical and practical solution is found which is the equal or superior to the liquid-filled transformer. The use of transformers insulated and cooled by compressed gas has been under consideration for many years, and undoubtedly this subject has a very wide and fertile field for future development.

Some of the problems to be solved are:

> The high cost of the tank to withstand continuous high internal pressure, satisfactory forced cooling of windings with elimination of local hot spots, control of the cooling equipment, prevention of total gas condensation, inferior dielectric strength of compressed gas under impulse voltage stress, ability of the windings to withstand impulse test, switching and lightning surges, satisfactory electrostatic shielding of the windings in compressed gas, and, finally, the ability to meet present-day requirements of practically "trouble-free" service.

We do not believe that Mr. Hobart's concern over the effective shielding of Pyranol transformers is justified. We have built shielded Pyranol-filled transformers and secured the same impulse voltage distribution as in shielded transformers filled with transformer oil.

The fact that the dielectric constant of Pyranol is approximately twice that of transformer oil simply caused the shape and the position of the shield to be modified accordingly. The effect of the dielectric constant of the insulating medium on the choice of shape and spacing of the shield is governed by the same simple law that governs the change in shape and dimensions of conductors designed to give the same total resistance when formed from materials of different resistivity.

In light of the preceding facts, the shape and proportion of the shield in three transformers of the same voltage, but filled with three different insulating mediums, like transformer oil, Pyranol, and gas, will have different shapes but will be equally effective, as far as impulse voltage distribution is concerned.

REFERENCE

1. RECOOLING OF LUBRICATING OIL FROM TURBINE BEARINGS AND GEARS, S. E. Derby. *Power*, August 2, 1921.

Equivalent Ambient Temperatures for Loading Transformers

Discussion and author's closure of paper 43-18 by W. C. Sealey, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, February section, pages 87–93.

C. E. Baugh (Pacific Gas and Electric Company, San Francisco, Calif.): Some concern has been expressed as to the applicability of the rules for overloading transformers on the basis of load factor and ambient temperatures to transformers installed in the interior valleys of California and in climatically similar locations under loading conditions such that maximum loadings are coincident with maximum temperatures.

Data are available in detail covering temperatures at Fresno, a city in the San Joaquin Valley, where climatic conditions in the summer are representative of the California valleys. The maximum temperature ever recorded at Fresno is 115 degrees Fahrenheit (46 degrees centigrade); the mean maximum for 53 years is 99.1 degrees Fahrenheit (37.2 degrees centigrade). In 1940 there were 34 days with maximum temperatures of 100 degrees Fahrenheit or higher; in 1911 there were 20 consecutive days with a maximum temperature of 100 degrees Fahrenheit or higher. The diurnal range of temperature in summer is high; while the average maximum in midsummer is about 100 degrees Fahrenheit, the average minimum is about 62 degrees, which is considerably lower than the average minimum of midwestern cities at the same time of year. Many substations in this area supply loads consisting largely of irrigation pumps, and peak demands are almost invariably coincident with the hottest days.

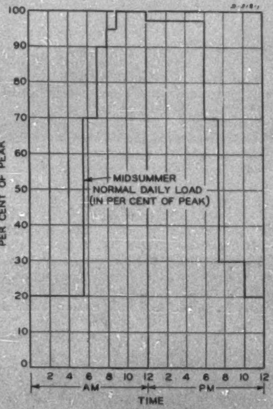


Figure 1. Normal daily load curve of a valley substation for a midsummer day

Pumping and incidental farm load

To check the application of the rules just mentioned, transformer temperatures were calculated for a bank of transformers carrying a peak load of 111 per cent of rating, with a daily load factor of 63 per cent, on a day having an average temperature of 30 degrees centigrade. The step-by-step method of calculating rises was used, based on the load curve in Figure 1, of this discussion, which is an actual load curve of a valley substation. These calculations showed a top-oil rise of 38.5 degrees centigrade and a hot-spot rise of 59.5 degrees centigrade above ambient. The top-oil rise determined by the factory test of these transformers was 39.1 degrees centigrade for full load continuous.

The air-temperature curve in Figure 2 of this discussion is drawn from United States Weather Bureau data and is representative of a midsummer day with a 24-hour average temperature of 30 degrees centigrade. The maximum temperature on such a day would be 41.1 degrees centigrade (106 degrees Fahrenheit), only five degrees centigrade under the maximum recorded temperature, and four degrees centigrade higher than the 53-year mean maximum. If the assumption is made that the top oil follows changes in ambient temperature exactly, the maximum temperature of the top oil will be only 78.4 degrees centigrade and that of the hottest spot only 99.5 degrees centigrade, with air temperatures as shown. Even with the highest air temperature ever recorded at Fresno, the top-oil and hottest-spot temperatures would be only 83.4 degrees and 104.5 degrees centigrade, respectively. The American Standards Association guide for loading transformers by temperature sets 105 degrees centigrade as the hottest-spot temperature for transformers carrying recurrent overloads for eight hours out of 24.

A maximum hot-spot temperature of 99.5 degrees centigrade is enough under the 105 degrees centigrade permissible temperature to conclude that the rules for overloading on a basis of load factor and average ambient temperature are conservative for transformers of modern design. Ordinarily, of course, there will be few days in any one year when the maximum temperatures shown will occur, and no noticeable reduction of expected transformer life should be expected.

The validity of the assumption that transformer temperatures will follow ambient temperatures exactly is questionable, of course. In the shade, even if protected from the wind, there will probably be considerable lag, and actual temperatures will be lower than those mentioned. In the sun, where considerable heat will be communi-

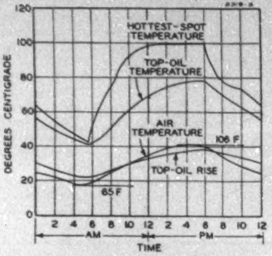


Figure 2. Transformer temperatures on a normal midsummer day having an average temperature of 50 degrees centigrade

Transformers loaded to 111 per cent of rating peak

Average air temperature—30 degrees centigrade

Equivalent ambient temperature—32.3 degrees centigrade

cated by radiation, the lag will be less, and it would not be impossible for these temperatures to be exceeded. However, there is ordinarily some wind where transformers are exposed to the sun, and the effects of direct radiation will be offset to some extent.

Attention should be directed to the fact that many older transformers were not built with temperature rises as much below guaranteed rises as are modern transformers, and care should be exercised if these older transformers are to be overloaded.

Another feature that cannot be neglected is that the rules cannot be applied to small transformers exposed to the sun and to high air temperatures. Measurements made on the oil in small unenergized pole-top transformers in the San Joaquin Valley show an oil temperature of 58 degrees centigrade (136 degrees Fahrenheit) with an air temperature of 45.5 degrees centigrade (114 degrees Fahrenheit). In such a case the ambient temperature used in calculating transformer temperatures should be taken as the higher figure and will result in considerably lower permissible loadings.

A careful study of the proposed American Standards Association guide and the interim report should convince utility engineers that power transformers can be operated at the correct maximum loading with assurance that their normal life will not be unduly impaired, only if they are equipped with devices for reading hottest-spot temperatures directly. Such devices are available at moderate cost, and their proper use will facilitate the full utilization of capital investment in transformers.

Operating engineers can gain much from a careful study of the many papers and reports on the subject at transformer loading. The benefits to be gained are of particular value during the war emergency, but operators should not forget that many of the principles set forth will have considerable economic value under normal conditions.

REFERENCES

1. ANNUAL METEOROLOGICAL SUMMARY, FRESNO, CALIF., 1940. United States Department of Commerce, Weather Bureau.

2. APPLICATION OF APPARATUS AND CONDUCTORS UNDER VARIOUS AMBIENT TEMPERATURE CONDITIONS, **R. E. Hellmund, P. H. McAuley.** AIEE TRANSACTIONS, volume 61, 1942, August section, pages 553–8.

3. EFFECT OF LOAD FACTOR IN OPERATION OF POWER TRANSFORMERS BY TEMPERATURE, **V. M. Montsinger.** AIEE TRANSACTIONS, volume 59, 1940, November section, pages 632–6.

F. J. Vogel (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): Mr. Sealey's paper shows a method of calculating an equivalent ambient temperature to allow for yearly variations and for different types of load. His work appears logical and well worked out, if the assumption "that the rate of aging of insulation doubles per each increase in temperature of eight degrees centigrade" is correct. Some data have been presented which are not in agreement with this and they particularly apply to some types of transformer construction.

As evidence of this statement, I would like to refer to Figure 1 in the paper, "Emergency Overloads for Oil-Insulated Transformers," by T. K. Sloat and F. J. Vogel, presented to the Institute in June 1942.[1] These data are also confirmed by Charles F. Hill's paper, "Temperature Limits Set by Oil and Cellulose Insulation."[2] Figure 1 would thus indicate that insulation could be operated at 105 degrees centigrade almost indefinitely without a decrease in mechanical strength below 70 per cent. If this statement is correct and the assumptions made in Mr. Sealey's paper are incorrect, some of the conclusions reached by his reasoning cannot be correct.

When Figure 1 and similar curves were submitted previously, they were criticized by some on the basis that the tests were not of sufficient duration. This duration was not thought to be long enough to indicate that further deterioration would not occur if the test had been continued longer. A difference in opinion is, of course, always possible, but Doctor Hill's data covered a period of over 22 months. I therefore feel that there is decidedly more than a reasonable chance that the conclusions reached from Doctor Hill's paper and from Figure 1 are correct. It is appreciated that, when previous opinion has been influenced in one direction, it is difficult to have newer data accepted and a new opinion substituted.

If the months of July and August alone are considered, the range in temperatures is not too great, as shown by reference to Table 2 of the paper. For example, take Boston. The average temperature of July and August is around 27 degrees, whereas the yearly equivalent is 20 degrees. On the other hand, Miami with an annual equivalent temperature of 28 degrees has an average temperature during July and August of 30 degrees. The annual figures tend to indicate that a given rating of transformer could carry considerably more load all year round in Boston than in Miami. This is not believed true, because there is only two degrees difference in July and August. Perhaps it would be well to consider Philadelphia, since this is the location used as an example in the paper. Figure 11 in the paper indicates that a transformer in Philadelphia could be operated at 108 per cent of its name-plate rating all year round. The months of June, July, and August are roughly 12 weeks. I estimate this would be equivalent to operating some transformers at about 105 degrees hot spot for a period of 12 weeks. Doctor Hill's paper indicated very little if any deterioration of insulation in an Inertaire atmosphere after 100 days, at temperatures over 100 degrees centigrade, and hence it may be assumed that Figure 3 of this discussion is indicative of the amount of insulation deterioration in a year and probably in an indefinite period of time. With this reasoning, it is quite probable that the ultimate insulation deterioration will be reached in one year, if the transformers are operated at 105 degrees centigrade hot-spot temperature for 12 weeks.

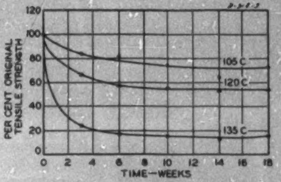


Figure 3. Decrease in tensile strength of Manila paper in oxygen-free oil protected by a nitrogen atmosphere

I agree that it appears safe to operate modern transformers at higher than their name-plate rating under wartime conditions. I think that this is particularly true of transformers protected with inert-gas blankets over the oil. Most such transformers, where necessary, could carry eight to ten per cent overload continuously without damage, where the ambient is not over 30 degrees centigrade. This also, of course, should be limited to either oil-insulated self-cooled transformers or transformers of self-cooled type with auxiliary air-blast cooling.

I think that the method used in Appendix I of the paper to determine the effect of the variation in daily ambient temperature is very effective and will be found useful.

REFERENCES

1. EMERGENCY OVERLOADS FOR OIL-INSULATED TRANSFORMERS, **T. K. Sloat, F. J. Vogel.** AIEE TRANSACTIONS, volume 61, 1942, September section, pages 669–73.

2. TEMPERATURE LIMITS SET BY OIL AND CELLULOSE INSULATION, **Charles F. Hill.** AIEE TRANSACTIONS, volume 58, 1939, September section, pages 484–7.

P. H. McAuley (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The author has developed a short-cut method for determining equivalent ambient temperatures from available Weather Bureau data. However, in the calculations he departs from his own definition of equivalent ambient temperature and thereby arrives at rather pessimistic values. Similar data were presented in a Hellmund–McAuley paper "Application of Apparatus and Conductors under Various Ambient Temperature Conditions." Our equivalent temperatures were obtained by analyzing the daily graphic thermograph records of the Weather Bureau for a period of years and applying the eight-degree-centigrade rule to the resulting curve of "temperature" versus "per cent of the time at or above." Comparing Table II of the present paper with Figure 2 of our paper, the present author obtains equivalent temperatures five to seven degrees centigrade higher for the five locations given in both papers. It appears that this approximate method gives too much weight to the maximum temperature values. The thermograph records show that temperatures within five degrees centigrade of the maximum at any given location persist for only a fraction of one per cent of the time, and the curve departs considerably from a sine-wave variation. Accordingly, because the time involved is so short, these higher values have a rather small influence on true equivalent aging temperatures.

It is agreed that use of equivalent temperatures permits the load capacity of apparatus to be utilized more fully under many conditions of application. There is considerable evidence, however, that the eight-degree-centigrade rule is conservative, and it does not appear desirable to add further safety factors in applying this rule to Weather Bureau data and thereby limit the usefulness of the procedure.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): Mr. Sealey has done a very thorough job in working up rules for using equivalent ambients.

While the method of deriving the equivalent ambient, by assuming that the temperature versus time follows a sinusoidal curve, is not rigorously correct, it is I feel accurate enough for all practical purposes. There is generally a small factor of safety in the one per cent rule which was first proposed in my 1939 (reference 4) paper. That is, if the hot spot is 95 degrees centigrade at 100 per cent self-cooled load in a 30 degrees centigrade ambient, it will generally be slightly under 95 degrees centigrade at 130 per cent load in zero degrees centigrade ambient.

On several occasions I have plotted both daily and annual ambients, and have found that they generally follow fairly closely a sinusoidal curve as shown in Figures 6 and 7 of Mr. Sealey's paper.

The question arises as to when should daily average ambients and when should annual equivalent ambients be used. Obviously both cannot be used.

The following procedure is suggested as a guide:

The conditions under which the daily average ambient and the annual equivalent ambient should be used to determine the overload capability, are:

1. For one day emergency overloads, use the daily average ambient.

2. When the load is constant throughout the year, or when the greater capacity requirements are in the summer months, use the equivalent annual ambient.

3. When the greater capacity requirements are in the winter months, determine the equivalent ambient for the heavy-load months and for the light-load months, and check the load capability for both periods. This permits loading up to normal transformer temperature in winter whenever the reduced summer load keeps the transformer temperatures to normal or lower on hot days.

Paul M. Lincoln (Therm-Electric Meters Company, Inc., Ithaca, N. Y.): I am not a transformer designer or operator as I assume the gentlemen presenting this series of papers to be. I do not pretend to know the details of the problems confronting these gentlemen. However, in listening to the discussion that has taken place, an idea has occurred to me that may be worth-while following up.

In going over these papers somewhat hastily, the only reference I can find to the type of insulation used is to Manila paper. I also know—as is general knowledge—that the usual insulations used in transformers are papers, cottons, silks, linens, and so forth. Now, it is well known that all of these materials are subject to being carbonized. That is, if they are subjected to temperatures high enough and long enough, they eventually will be reduced to almost pure carbon. The limiting temperatures and times that these insulations can withstand without becoming conductors are given quite accurately in the papers presented this morning.

Now, why is not the best line of attack on this problem to look for an insulation which, instead of being limited to a maximum hot-spot temperature of somewhere around 200 to 250 degrees, can safely go up to 500 or 600 degrees, or perhaps even higher? It is quite true that the copper losses will increase at these higher temperatures, because of the positive temperature coefficient of copper, but the saving in transformer weights and costs will vastly more than offset this increase. So far as I know, the only increase in loss that will occur is due to the increased temperature rise in the copper loss. So far as I know, iron losses do not increase at these higher temperatures. If, for instance, a transformer, designed as a 10,000-kw unit when using the usual papers, cotton, linens, silks, and so forth, can have its rating increased to perhaps 25,000 kw or 30,000 kw when using a special heat-resisting insulation, the saving in cost is obvious.

So far as I know, no such special insulations exist today. Also the problem of cooling oils or other cooling and insulating mediums must be attacked. But is not the best line of attack on this problem to start some research work to see whether or not insulations suitable for withstanding much higher temperatures cannot be developed?

W. C. Sealey: The procedure outlined in C. E. Baugh's discussion is a method of combining the effect of load variation with variations in daily temperature. This procedure is more accurate than approximate methods of correcting for load factor, since it more closely represents existing conditions. Whenever information on the load variation is available, together with information on the change in air temperature, a procedure such as outlined is desirable.

In reference to Mr. Vogel's discussion, the assumption that the tensile strength of the insulation reaches a constant value below which it does not decrease when subjected to constant temperature conditions is based on what appears to be insufficient data. The length of time for the test of Figure 1 is only 18 weeks. Even the points shown on these curves are such that curves showing continuing decreases can be drawn through them if less weight is given to the points at 18 weeks. In view of the fact that several other investigators have found a continuing decrease over longer periods, it seems evident that the curves of Figure 1 are drawn incorrectly for the points represented. For example, all the points up to 14 weeks have shown a continuing decrease in tensile strength without exception, and it is only the three points for the 18 weeks time which are level or above those of the 14 weeks. Considering these facts and experience with cellulose materials, it would appear that the curves of Figure 1 of the discussion should have been drawn with continuing decrease in strength.

With reference to the discussion on Table II, this table indicates that it should be possible to carry two per cent overload continuously during the year in Miami and ten per cent overload during the year in Boston, or eight per cent more load in Boston than in Miami. As given in the paper, the eight-degree rule is an approximation which provides a convenient basis for estimating insulation deterioration for comparative purposes. From a comparison of the climate of Boston and Miami the eight-degree difference in their equivalent temperatures as determined by this method appears reasonable, as does the result that a transformer should be able to carry eight per cent more load in Boston than in Miami.

With reference to Mr. McAuley's discussion, the objection to the lower values of equivalent temperature as given in the Hellmund–McAuley paper mentioned are that they are based on average values for a limited number of years, and that it is easily possible to have in one particular year or in a succession of years equivalent temperatures greater than those occurring in the particular years analyzed. The values obtained from averages are liable to be exceeded in some particular year, and unsafe conclusions would be drawn regarding permissible overloads. In dealing with any problem where maximum values have such a large effect, as in aging of insulation, they must be given proper weight. The method developed in the present paper is demonstrably conservative as any overload recommendations should be.

The rules given in Mr. Montsinger's discussion are convenient as a guide for determining the periods for the calculation of the equivalent ambient temperature for the loads to be carried, and they should be helpful when used for this purpose.

One of the objects of the paper was to develop a method for calculation of equivalent ambient temperatures which was demonstrably conservative. Even the discussions which do not agree with the method used admit that the values obtained are conservative (some say too conservative) and that transformers can be loaded safely in accordance with them.

With reference to Mr. Lincoln's discussion, the method which he proposes of increasing transformer overloads with a given amount of material is entirely logical. Various research laboratories, including our own, have been working on this problem for a considerable period of time, and we all feel that the final answer has not yet been obtained.

The Rating of Power Transformers as Determined by the War Emergency

Discussion and authors' closure of paper 43-36 by R. J. Salsbury and A. F. Phillips, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, June section, pages 255–60.

F. J. Vogel (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): The paper by R. J. Salsbury and A. F. Phillips shows in order the steps which should be taken where loads have grown beyond their previous values and the transformer capacity is getting close to the load.

Obviously, the first step is to take advantage of the increase in rating permitted by American Standards Association recommendations for ambient temperatures below 30 degrees.

The second step is also logical, since many times transformers as supplied by the manufacturer have a temperature rise less than 55 degrees. Also, if the transformer is operated on some taps, it may have a higher capacity than it would have when operated on the lowest voltage taps. I think that the method used in the paper is unduly conservative, since it has been shown that the difference between the hot spot and the top oil does not increase so rapidly as shown in Figure 5. Confirmation of this is given in the paper, "Hot-Spot Winding Temperatures in Self-Cooled Oil-Insulated Transformers," by Paul Narbutovskih and F. J. Vogel, presented to the AIEE in January 1942.[1]

The third step of using increased ratings because of thermal capacity is logical. A method allowing for short-time overloads was outlined in the paper, "Emergency Overloads for Oil-Insulated Transformers," by Sloat and Vogel,[2] which is simple and is not so conservative as the method outlined by Salsbury and Phillips. It is necessary, in that case, to assume that the hottest spot is at the operators' discretion. This will vary, depending upon the use of inert gas protection, conservators, or no protection at all.

I would like to refer to my discussion of the paper by Mr. Sealey. This discussion applies equally well to the method of "life units" used by Salsbury and Phillips. It is realized that the eight-degree rule has been in use for a relatively long time. The fact that deterioration becomes extremely slow in some cases after a certain period of time has passed was found out comparatively recently. This applies, of course, to transformers protected with inert gas. Therefore, it is natural, perhaps, that the eight-degree rule is used in most calculations.

I would like to call attention to the fact that transformers can be supplied with devices which permit loading by copper temperature. Where this is done, many of the calculations given before become unnecessary. Furthermore, these devices give suitable warnings and will save the transformer in case of excessive emergency overloading. This device is described in a paper by Putman and Dann in the AIEE TRANSACTIONS of 1939.[3]

REFERENCES

1. HOT-SPOT WINDING TEMPERATURES IN SELF-COOLED OIL-INSULATED TRANSFORMERS, **Paul Narbutovskih, F. J. Vogel.** AIEE TRANSACTIONS, volume 61, 1942, March section, pages 133–6.

2. EMERGENCY OVERLOADS FOR OIL-INSULATED TRANSFORMERS, **T. K. Sloat, F. J. Vogel.** AIEE TRANSACTIONS, volume 61, 1942, September section, pages 669–73.

3. LOADING TRANSFORMERS BY COPPER TEMPERATURE, **H. V. Putman, W. M. Dann.** AIEE TRANSACTIONS, volume 58, 1939, October section, pages 504–14.

W. R. Brownlee (Commonwealth and Southern Corporation, Jackson, Mich.): In considering the ratings of power transformers as influenced by the war emergency, the authors have mentioned that operating voltage sometimes limits the capacity of transformers on account of overexcitation. In order to develop maximum over-all system capacity, it may be necessary to operate transmission and subtransmission systems at voltages which would result in serious overexcitation of one or more transformer banks (usually older units).

Such a situation arose some seven years ago on a system with which the writer was associated. The maximum use of existing equipment required raising the voltage on portions of the transmission system. Although materials were not scarce then, the situation surrounding operations made it highly undesirable to rewind or replace the transformers which would be most seriously affected by the required operating voltage. On the other hand, the age of the units was such that it might be necessary to replace them within a few years. The situation was solved by the purchase of new booster transformers with windings designed to operate in parallel with one winding of a main unit and in series with its other winding. At slight extra cost, the boosters were purchased with series parallel connections of one winding, so that, whenever it should be found necessary to replace the main transformer units, the booster transformers would be used readily at one of various distribution substations by a simple rearrangement of terminal-board connections.

Recently the need of developing maximum over-all system capacity together with the necessity of making available all of the kilovar capacity of an older generating station has introduced the problem of overexcitation, and consideration is being given to the use of booster units. Forced cooling is not particularly applicable, since the total transformer losses would not be excessive, but the temperature rise at the surface of the iron at the higher excitation voltage would produce excessive oil sludging. If the authors have analyzed specific cases of overexcitation, the method used to determine the temperature rise of the iron would be of interest.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): The authors have given some very useful data based on operating experience for the overloading of transformers for the war emergency.

It will be noted that I did not say "rating" but rather "overloading." The authors speak all through their paper of giving transformers extra ratings. For standardization purposes a transformer should have only one rating. This does not mean that its output is limited to its rating. In fact, the new American standards are set up on the basis of each transformer having a single rating, or a double rating if of the self-cooled and air-pressure type, but with guides giving recommendations for obtaining increased output under different operating conditions. I believe the authors had in mind increased outputs rather than ratings.

With reference to setting up different limits for oil temperature, the authors are well justified in setting up a lower limit for free-breathing transformers than for transformers that are not free breathing. There is a question, however, whether there should be a difference of five degrees centigrade in the limit for conservator-type and inert-gas-type units. The General Electric Company has built many millions of kilovolt-amperes of both types of transformers, and, based on experience ranging over a period of 20 to 25 years, the same oil temperature limit can be used for both types of units.

R. J. Salsbury and A. F. Phillips: V. M. Montsinger questioned the use of the word rating and suggested that "overloading" was the proper term. The use of the latter term for a normal everyday rating is questionable from an operating standpoint, as it implies that the transformers are being stressed beyond a desirable value, whereas the ratings which are assigned do not permit abnormal rates of deterioration. It is agreed that only one name-plate rating is desirable for standardization purposes, but it is believed that the use of the word "rating" as applied to operating ratings conforms to the definition of the word as given in the *American Standard Definitions of Electrical Terms* (05.50.040).

Mr. Montsinger agrees with the setting up of different oil limits for free-breathing and nonfree-breathing transformer but questions the use of different limits for conservator-type and inert-gas-type units. In the conservator-type transformer there is a limited contact between air and oil, whereas in the inert-gas type of unit no air-oil contact is possible. It may be desirable to refer to an article by L. M. Limpus[1] on the experience of the Oklahoma Gas and Electric Company with these three types of units: "Based on the experience of our company an average annual increase in neutralization numbers of 0.1 mg. KOH for open type transformer, 0.05 mg. KOH for conservator type, and 0.01 mg. KOH for inert-gas-type equipment may be expected." Since the increase in the neutralization number is an indication of oil sludging, this experience indicates that inert gas transformers warrant preferential treatment.

W. K. Brownlee has asked what methods if any have been used to determine the temperature rise of the iron due to overexcitation. No attempt has been made to measure this temperature. In extreme cases of overexcitation, standard distribution transformers have been used as boosters in the neutral of star-connected banks, and in less severe cases the oil temperatures and oil-neutralization numbers are watched.

F. J. Vogel, in commenting on the second step in rating power transformers, stated that the results appeared to be unduly conservative, because of the method used in calculating the difference between the hot-spot and top-oil temperatures. The method used is supported by the conclusions drawn by V. M. Montsinger and P. M. Ketchum in their recent paper[2] on transformer overloading. This paper which discusses the results of about 50 heat runs on typical transformers indicated that the hot-spot over top-oil temperature varies as the 0.8 power of the load losses, and that the difference between the hot-spot and top-oil temperature at full load is about six degrees centigrade for the small transformers tested. The paper, however, recommends the use of ten degrees centigrade for larger and higher-voltage transformers where greater temperature differences would naturally be expected. Consequently, there is a possibility of some slight conservatism for this reason in the smaller sizes, but not necessarily in the larger units.

In regard to Mr. Vogel's reference to the eight-degree rule, it should be mentioned that this rule was not used as a basis for the life-unit method of rating. Test data[3] by F. M. Clark, which revealed that the rate of insulation deterioration doubles for a 4-degree increase in temperature at 70 degrees centigrade, doubles for a 5.5-degree increase at 100 degrees centigrade, and doubles for a nine-degree increase at 120 degrees centigrade, for 50 per cent insulation strength, and so forth, was used as a basis of the life-unit method.

REFERENCES

1. MAINTENANCE OF INSULATING OIL—V. OXIDATION INHIBITORS, L. M. Limpus. *Electrical World*, December 26, 1942, page 70.

2. EMERGENCY OVERLOADING OF AIR-COOLED OIL-IMMERSED POWER TRANSFORMERS BY HOT-SPOT TEMPERATURE, V. M. Montsinger, P. M. Ketchum, AIEE TRANSACTIONS, volume 61, 1942, pages 906–16.

3. FACTORS AFFECTING THE MECHANICAL DETERIORATION OF CELLULOSE INSULATION, F. M. Clark, AIEE TRANSACTIONS, volume 61, 1942, October section, pages 742–9.

Substation-Transformer Emergency Overloading Practice

Discussion and author's closure of paper 43-24 by L. W. Clark, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, March section, pages 126–32.

H. B. Wolf (Duke Power Company, Charlotte, N. C.): This timely paper covers comprehensively and clearly the subject of emergency overloading of transformers. It appears that, in evaluating the increased output of a transformer as a result of fan cooling, the author has been too conservative.

Figure 10 of this paper shows increased output of a self-cooled transformer by addition of supplementary cooling in form of fans, the increase in this case being quite small. Tests over many years and with various types of self-cooled transformers indicate an average increase in output of approximately 30 per cent.

Tests of output of three transformers of different manufacture are given in Table I of this discussion. All temperatures are on a basis of 35 degrees ambient.

Table

Transformer	Per Cent Load	Cooling	Hot-Spot Temperature
A	100	Self-cooled	77
	130	Fan-cooled	67
B	100	Self-cooled	78
	131	Fan-cooled	72
C	100	Self-cooled	76.5
	127	Fan-cooled	62.5

This increase in output is also recognized in the transformer subcommittee interim report of June 22, 1942,[1] in which it is stated that an increase from 25 to 33 per cent is to be expected by addition of fans.

It is quite possible in many cases to have transformers out of service for several weeks or more, while repairs are being made, without increasing copper or oil temperatures on the transformers remaining in service to the point where rapid deterioration begins, and without tying up emergency spare transformers, if advantage is taken of supplementary cooling in the form of spray or fans and of reduction of load by means of mobile capacitors.

REFERENCE

1. INTERIM REPORT ON GUIDES FOR OVERLOADING TRANSFORMERS AND VOLTAGE REGULATORS, AIEE transformer subcommittee. AIEE TRANSACTIONS, volume 61, 1942, September section, pages 692–8.

F. J. Vogel (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): The paper by Mr. Clark shows the method used by one operating company to determine safe loads for transformers during emergency conditions. The methods used are based on the so-called eight-degree rule for insulation deterioration and upon a conventional method of calculating the hottest spot.

I would like to refer to papers showing different points of view, as follows:

"Temperature Limits Set By Oil and Cellulose Insulation," by Charles F. Hill.[1]

"Emergency Overloads for Oil-Insulated Transformers," by Sloat and Vogel.[2]

"Hot-Spot Winding Temperatures in Self-Cooled Oil-Insulated Transformers," by Narbutovskih and Vogel.[3]

These papers indicate that using the same methods for all types of transformers may involve undue risk for some types of transformers and be too conservative for others. These other references show methods which can be used for transformers having inert gas protection and for transformers of modern construction.

I would like to refer to my discussions of papers by Sealey and Salsbury and Phillips, at this same meeting.

REFERENCES

1. TEMPERATURE LIMITS SET BY OIL AND CELLULOSE INSULATION, Charles F. Hill. AIEE TRANSACTIONS, volume 58, 1939, September section, pages 484–91.

2. EMERGENCY OVERLOADS FOR OIL-INSULATED TRANSFORMERS, F. J. Vogel, T. K. Sloat. AIEE TRANSACTIONS, volume 61, 1942, September section, pages 669–73.

3. HOT-SPOT WINDING TEMPERATURES IN SELF-COOLED OIL-INSULATED TRANSFORMERS, F. J. Vogel, Paul Narbutovskih. AIEE TRANSACTIONS, volume 61, 1942, March section, pages 133–6.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): Mr. Clark's paper gives a very useful and practical method of overloading transformers, for the guidance of operators.

I note that Mr. Clark has used 120 degrees centigrade for 24 hours, to take out approximately one per cent of the life of the insulation, which he states was based on Mr. Nichols' (reference 2) paper. While this figure is ten degrees higher than the temperature limit of 110 degrees centigrade recommended in AIEE interim report, the author has added sufficient safety factors which in most cases will result in a hot-spot temperature nearer 110 degrees and certainly not over 115 degrees for a 24-hour period.

One factor of safety that the author introduced was in assuming that the hot-spot rise over top oil varies as the square of the load current, whereas under most conditions it varies as the load current raised to approximately the 1.6 power. The other factor of safety was in assuming that the hot-spot rise over average winding is always ten degrees at 100 per cent load. As pointed out in AIEE technical paper 42-115, entitled "Emergency Overloading of Air-Cooled Oil-Immersed Transformers by Hot-Spot Temperatures," by V. M. Montsinger and P. M. Ketchum,[1] and presented at the AIEE convention last summer, the hot-spot temperature will range from five to ten degrees, depending on the design, the usual value being somewhere around six to seven degrees.

Furthermore, within the last year F. M. Clark in his AIEE technical paper 42-98, entitled "Factors Affecting the Mechanical Deterioration of Cellulose Insulation,"[2] presented at the summer convention in June 1942, has shown that the rate of aging varies over a wide range, depending on the degree of dryness. For example: insulation having one per cent moisture will deteriorate from five to ten times as fast as insulation having 0.05 per cent moisture. It can be seen readily, therefore, that there is no definite rate of aging except under specified moisture conditions.

I was very much interested in the nomographs (shown in Figures 2 and 3 of the paper) for determining allowable summer and winter emergency overload capacity of self-cooled transformers. I am sorry that the author did not explain just how he located the two sets of top-oil rises on the top and bottom lines, to make them give the correct loads. I have not integrated any of his load curves to see how well they agree with his nomograph curves. I assume that his calculations are correct.

My experience in increasing the capacity of large transformers with fans does not bear out the rather low overload values given in the paper. The 10 to 15 per cent increased rating apparently refers to ultimate conditions, since the author couples it with 120 degrees hot spot which he previously stated is for a 24-hour period.

We can usually obtain from 25 to 33 per cent overload with standard small fans. Recent tests have shown that by using high-air-velocity fans we sometimes can obtain as much as 150 to 167 per cent of the self-cooled rating with the same hot-spot temperature. The increased rating with fans, when based on 120 degrees centigrade hot spot, will of course be slightly less than when based on a 95 degrees or 105 degrees hot spot, although the difference is small. Perhaps the reason Mr. Clark did not obtain more than 10 to 15 per cent extra rating was that the tests were made outdoors with a breeze blowing. A very small breeze will reduce the oil rise sufficiently to obtain 10 to 15 per cent extra rating under self-cooled conditions.

Mr. Clark has given some very useful information on the time required to replace a defective transformer with a spare, namely, about 24 hours, and that it can be expected that a failure will occur once in approximately 16 years operation. He also gives some very useful information on potential difficulties to be avoided. It is data of this kind that are useful in setting up rules for overloading transformers under rare emergency conditions, similar to the AIEE interim report previously referred to.

I would like to see more papers presented along this line giving results of operating experience.

REFERENCES

1. EMERGENCY OVERLOADING OF AIR-COOLED OIL-IMMERSED TRANSFORMERS BY HOT-SPOT TEMPERATURES, **V. M. Montsinger, P. M. Ketchum.** AIEE TRANSACTIONS, volume 61, 1942, pages 906–16.

2. FACTORS AFFECTING THE MECHANICAL DETERIORATION OF CELLULOSE INSULATION, **F. M. Clark.** AIEE TRANSACTIONS, volume 61, 1942, pages 742–9.

L. W. Clark: Mr. Montsinger points out that, although a hot-spot temperature limit of 120 degrees for a 24-hour overload has been used, there are certain safety factors in the author's calculations which will reduce the actual hot-spot temperature to something in the order of 110 or 115 degrees, which is nearer the recommendation given in the AIEE interim report. One safety factor mentioned is that of assuming that the hot-spot copper rise over top oil varies as the square of the load instead of the 1.6 power. The square rather than 1.6 power was used because of the difficulty in determining with which transformers it is safe to use the 1.6 power. The choice of exponent does not greatly affect the results. In the 120-degree range, there is only a difference of one or two degrees in hot-spot temperature as calculated with the two different exponents. Furthermore, the test data given in Table II of the paper indicate that, at least for these particular cases, the use of the square exponent does not give too high a copper-rise value as compared with the test results.

The other factor of safety mentioned is very real and accounts for the major part of the difference beween 110- and 120-degree hot spot. If, as Mr. Montsinger suggests, a hot-spot differential above average copper rise of six or seven degrees instead of ten degrees were used, considerably higher overloads for the same calculated hot-spot temperature would result. However, until the various manufacturers agree to change the American standards which permit a ten-degree differential, it would seem unwise to use the lower value. The author would assume from Mr. Montsinger's comments that the tables and curves for allowable overloads given in the AIEE interim report are based on either six- or seven-degree hot-spot differential instead of the full ten degrees as specified in the American standards.

Mr. Montsinger asks how the two top-oil rise scales were selected for the nomographs of Figures 2 and 3 of the paper, in order to give the correct loads. For each nomograph the lower scale of top-oil rise was selected arbitrarily, so as to result in a good workable nomograph. The upper scale of top-oil rise in each case was then calculated for the 140 per cent load ordinate with $N=1$. This was calculated, using the first part of formula 1 given in the paper. For example, substituting in that formula for a top-oil rise at full load of 30 degrees, $N=1$ and $R=1.4$, the corresponding top-oil reading for the top scale is 41. On this basis, the 30 value of the top scale is located directly above the 41 value of the bottom scale. In the same manner, for instance, the 36 value of the top scale is plotted directly above the 49.3 value of the bottom scale.

Both Montsinger and Wolf suggest that an increase in allowable emergency loading of 10 to 15 per cent through the addition of fan cooling is too conservative. This low value of increased loading is based upon only one company's experience and tests on transformers equipped with fans, and it is entirely possible that other companies have had more favorable experiences with the fan cooling. Mr. Wolf cites three cases in which increased loadings of something in the order of 30 per cent are permitted through the addition of the fans. There is one essential difference between the figures cited by Mr. Wolf and those given in the paper; the former are at about name-plate rating of the transformers, and the latter are in the overload range of 125 to 150 per cent of name-plate rating.

With a standard transformer having 55-degree average copper rise, the usual increase in rating given by the manufacturers when fans are added is $33^1/_3$ per cent for the same average copper rise. Converting this to the overload level corresponding with 120-degree hot-spot temperature, and assuming in both cases that the performance just meets guarantee, the $33^1/_3$ per cent increase at name-plate level drops down to about 23 per cent at the overload level. In addition, this margin due to fans has shrunk still further in the case of installations tested by the author's company, because of the fact that there was a generous design margin over and above guaranteed values on the self-cooled basis, but practically no margin at the fan-cooled rating, with the result that the actual increase in the overload rating through the addition of fans was only 10 to 15 per cent as cited.

Insulation Testing of Electric Windings

Discussion and authors' closure of paper 43-59 by C. M. Foust and N. Rohats, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, April section, pages 203–06.

L. J. Berberich (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Foust and Rohats have described a new method of testing the insulation of machines which appears to have considerable merit in production line testing particularly of the smaller machines. Large and important machines, however, will probably continue to be tested at the factory by the older methods. In the large machines the turn-to-turn insulation is tested by applying high frequency (about 30,000 cycles) to each coil before it is wound into the machine. The ground insulation is similarly tested by applying 60-cycle overvoltage. This is done, not only on every coil before winding, but also on groups of coils during various stages of the winding procedure. Such careful testing is necessary, not only to insure that no defective coil is wound into the machine, but also to uncover any damage that may be inflicted during the winding process. Even though this new method does not appear very attractive for testing large machines at the factory, it should have application for testing such machines in the field. This would probably require a test set having higher voltage rating and capacity than the one described in the paper.

In the paper the statement is made that the new method can be applied with particular advantage in testing of completed windings of three-phase low-voltage machines. Since nothing more specific was said regarding the range of application, I would like to ask the following questions:

1. To what voltage and kilovolt-ampere ratings has the test so far been applied?

2. What is the capacity and voltage range of the present test equipment?

3. Can the test be applied to single-phase windings, or is it limited to multiphase windings?

4. Can the location of a turn-to-turn fault be determined in a winding with any degree of accuracy?

5. What are the possibilities of modifying the test equipment for testing large 13.8-kv generators in the field?

6. What turn-to-turn voltage can be obtained, and how uniform is this throughout the whole winding?

John L. Fuller (Reliance Electric and Engineering Company, Cleveland, Ohio): The paper indicates that a fault consisting of a grounded neutral will not be disclosed by the repeated surge tester because of the symmetry of the circuit. This would seem to indicate that the test cannot take the place of the standard 60-cycle high-potential test in its entirety. In comparison with a dummy rotor test, we note that the dummy rotor test can be applied to a stator prior to varnish impregnation, thereby simplifying corrections of any errors in connections. This test will also disclose a symmetrical misconnection which the repeated surge tester cannot find. On the other hand, the repeated surge tester will apparently disclose a shorted turn which would have a fine chance of being missed on the dummy rotor test. It will also stress turn-to-turn insulation and is presumably quicker to operate.

It will be interesting to see how usage of this apparatus may be adapted to testing of d-c armatures, field coils, and intercoils. Field coils and intercoils should be an easy test by comparing with a standard or coil known to be satisfactory.

In conclusion it appears that the repeated surge tester is a valuable addition to motor testing equipment, although it apparently cannot supplant the standard 60-cycle high-potential test and although it has not yet been adapted to d-c work. Its greatest usefulness, apparently, is in disclosing latent troubles which might not be disclosed otherwise prior to shipment of the a-c motor.

F. L. Lawton (Aluminum Company of Canada Limited, Montreal, Que., Canada): The authors are to be congratulated on their paper which describes a new method for testing the insulation of windings of electrical equipment. The new test equipment would appear to be the first forward move in the last ten years or so.

I would like to ask the authors what ratio of impulse peak voltages to normal or rated 60-cycle voltage is required for satisfactory indication by the new device? Also it would be interesting to have their advice as to the size of machine for which windings have been tested by this device. For instance, have they tested windings for generators of, say, 10,000- to 15,000-kva capacity, and for 13,200-volt service?

C. M. Foust and **N. Rohats:** Taking up the three discussions one at a time, we will first answer the questions asked by L. J. Berberich.

1. So far, the tester, as described, has been applied to motors in the 110- to 550-volt class up to about 100 horsepower and a larger unit is being used on high-voltage motors of several thousand horsepower.

2. The maximum voltage in the small tester of Figure 3 of the paper is 10 kv with a surge-generator capacitance of 0.05 microfarad. The large tester now in daily use has 0.25 microfarad and 15 kv.

3. Single-phase windings may be tested, if two at a time are available for comparing one with another.

4. Exact location of turn-to-turn faults from wave shapes on the oscilloscope screen involves comparisons of wave details which are somewhat impractical from a routine test standpoint. However, if the fault is outside of the slots and several kilovolts are being applied, sparking will be seen at the fault.

5. Test equipment has been considered for machines of higher kilovolt-amperes and voltage ratings with multiturn coils.

6. Maximum coil voltage ranges from 25 to 50 per cent of applied voltage and tapers off to about a ten per cent minimum as the surge travels through the winding. Assuming three kilovolts applied voltage, this means 750 volts to 1,500 volts on the first coil and 300 volts minimum further along in the winding.

Regarding J. L. Fuller's discussion, oscillogram 12 in Figure 4 of the paper shows how the wave is inverted by a ground at the neutral. A misconnection which occurs symmetrically in all phases is a rather remote possibility but would be indicated by some change in wave shape if not by wave divergence.

The tester already has been applied to individual coils of various kinds with some success. However, with coils or windings having high impedance because of a large number of turns of small wire, the stray capacitance network becomes a factor in determining wave shape, and this may vary from coil to coil so that some divergence may be found with coils having no insulation faults. This normal divergence will tend to obscure the indication of a single shorted turn and will require a minimum of four or five shorted turns to produce good indication.

We are glad to note, and we share Mr. Fuller's interest, in the use of this type of testing equipment for d-c armatures. While the winding insulation tester is designed particularly to stress turn insulation, it also stresses the major insulation between conductor and ground and really constitutes, therefore, a comprehensive high-potential test. However, this test is at present additional to other tests and it is not proposed to discontinue the normal high-potential test to ground.

In reply to F. L. Lawton's question, the test voltages for low-voltage equipments have ranged from 2,500- to 5,000-volt crest. Of course, where a positive fault is already present in a winding, a voltage as low as 500 volts is enough for its indication.

The winding insulation tester has been used for machine voltages ranging up to 3,000 volts rating and of several thousand kilovolt-ampere capacity. For large machines which may vary considerably as to circuit constants and insulation, test voltages must be selected in view of normal surge voltage distributions and to provide insulation stresses adequate to reveal weaknesses which might cause or contribute to service failures.

In closing the authors wish to thank the discussers for the interest they have shown in the paper and to declare their readiness to answer any further questions which may come up.

Measurements Pertaining to the Co-ordination of Radio Reception With Power Apparatus and Systems

Discussion and authors' closure of paper 43-57 by C. M. Foust and C. W. Frick, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, June section, pages 284–91.

Arthur Bessey Smith (Associated Electric Laboratories, Inc., Chicago, Ill.): We are very glad for the work which the authors have done in the measurement and suppression of radio interference from electrical apparatus and in making public these data.

Lord Kelvin stated that we have no true scientific knowledge until we can express what we know by means of numbers. By means of numbers George Stephenson confounded the rule-of-thumb "engineers" of his day and won the battle for the level railroad track, on which the steam locomotive proved its ability to give the cheapest hauling cost on land.

The expression of our knowledge by means of numbers is today the ardent desire of all engineers. Yet those who *do* venture to publish their numerical results are sure to receive comment and even criticism. This is especially true in the measurement of radio noise, which has proved to be apparently the most erratic quantity to measure. Yet a start *must* be made, and data *must* be published. We recognize the service which the authors have performed.

The relation between two sources of radio interference operating at the same time has long been observed by those working in this field. At first it was thought that the amount of reduction of interference was our objective. But it was soon discovered that this had little value to the public. That which seems most important in the public ear is the *remaining* interference, after the application of suppression devices. Perhaps this is the reason why so many years have passed before the publication of data on differing numbers of disturbers in parallel.

From the standpoint of residual noise, one offender was about as bad as two. For instance, while working on small automatic telephone exchanges, we found that to suppress either the calling device or the switch in the central office did not clear the complaint. Both had to be suppressed at the same time.

My attention has been attracted to the authors' Table X. Visual inspection shows that two insulators give not much more Radio-Influence Voltage than one. This agrees with past experience.

As to the mathematical expression which most closely represents the results of tests, I have applied variations of the root-sum-square expression proposed by the authors. The results are at least interesting.

First, I averaged the eight values of *RIV* for one insulator and regarded it as the true value for one insulator. The eight samples ought to come nearer the truth than two or three. Then I put this into the root-sum-square expression, causing it to become

$$V=\sqrt{nv^2}=v\sqrt{n}$$

in which

n = number of insulators operated at the same time
v = average *RIV* for one insulator
V = total *RIV* when n are operating

Then it occurred to me to question the index 0.5 applied to n. Might some other value be closer to the truth? Assuming that the values of V for $n = 1$ and $n=4$ are correct, I calculated the index which will pass a curve through those points. There came out a different index for each test voltage applied to the insulators.

There result four expressions:

$$V=v_1n^a \quad V=v_2n^b \quad V=v_3n^c \quad V=v_4n^d$$

These indexes are

$a=0.41478$ and $v_1=4{,}276$
$b=0.38310$ $\quad v_2=15{,}875$
$c=0.35169$ $\quad v_3=28{,}250$
$d=0.37759$ $\quad v_4=43{,}250$

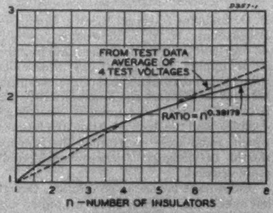


Figure 1

Table I. Radio-Influence Voltage of Insulators

Comparison of Test Data and Formulas

1		2		3		4	
	Test Data	Root-Sum-Square	Per Cent Error	Root-Sum-Square	Per Cent Error	Root-Sum-Square	Per Cent Error
10 Kv							
n				**Index 0.41478**		**Index 0.38179**	
1	1.000	1.000	0	1.000	0	1.000	0
2	1.3796	1.4264	+ 3.39	1.3331	− 3.38	1.3030	− 5.55
4	1.7772	1.8707	+ 5.26	1.7772	0	1.6997	− 4.47
8	2.6891	2.8528	+ 0.61	2.3690	−11.9	2.2120	−17.7
16 Kv				**Index 0.38310**		**Index 0.38179**	
1	1.000	1.000	0	1.000	0	1.000	0
2	1.2283	1.4803	+20.5	1.3041	+ 6.17	1.3030	+ 6.05
4	1.7008	2.1417	+26.	1.7008	0	1.6997	− 0.065
8	2.2047	2.8346	+30.	2.2181	+ 0.6	2.2120	+ 0.33
22 Kv							
n				**Index 0.35169**		**Index 0.38179**	
1	1.000	1.000	0	1.000	0	1.000	0
2	1.0619	1.5044	+27.6	1.2761	+20.2	1.3030	+22.6
4	1.6283	2.2124	+35.9	1.6637	0	1.6997	+ 4.38
8	2.2655	2.9026	+28.2	2.0778	+ 8.3	2.2120	− 0.24
28 Kv				**Index 0.37759**		**Index 0.38179**	
1	1.000	1.000	0	1.000	0	1.000	0
2	1.1329	1.4332	+26.5	1.2932	+14.1	1.3030	+ 6.18
4	1.6879	2.0649	+22.4	1.6879	0	1.6997	+ 0.70
8	2.2428	2.8527	+27.2	2.1928	− 2.22	2.2120	− 1.37

It will be observed that except for the 10-kv data the expressions which result in the figures in columns 3 and 4 agree fairly well with the test data. As to shape (curvature) the agreement is not as good. Each of the test data curves looks more like a straight line.

It will be observed that the value of the index tends to go down as the operating voltage rises. This makes it appear that different values of operating electromotive force on the insulator produce differing rates of increase as the number of insulators is increased.

These four expressions seem to agree fairly well with the test data, but not uniformly so.

Then I averaged these indexes, obtaining a value which I hoped might be valid for all electromotive forces. This average is 0.38179.

It will be observed that n^a is the ratio of any V value to the V for one insulator. This led to the comparison of the several expressions by means of their ratios to the *RIV* for one insulator. The complete comparison is given in the Table I of this discussion. No attempt was made to smooth out the curves of the test data, though for the 10-kv data, the *RIV* for eight insulators seems to be out of line. The per cent columns give the per cent which the calculated value is above or below the test data.

It is to be hoped that future work will clear away present uncertainties and reveal with clearness the essential relation between radio interference and the number of offending devices. The authors have done us a good turn by starting us on the way.

F. O. Stebbins (General Electric Company, Schenectady, N. Y.): This paper covers in an excellent manner the radio-noise problem as it applies to present day radio-noise measuring instruments and to the establishment of satisfactory radio-noise levels. The data show that radio noise is exceedingly complex, and the determination of a basis on which radio-noise measuring instruments should be designed requires considerably more research.

Radio-noise problems on aircraft, though not specifically mentioned in this paper, are, nevertheless, of considerable importance. In general there are two types of aircraft, the all-metal aircraft in which the body of the plane effectively shields the radio antenna (except for the antenna lead-in wire) from radio noise developed in the body of the plane, and the nonmetallic airplane body in which the radio antenna is not shielded from radio noise in the airplane.

Many aircraft today operate with d-c power systems. Sources of radio noise which are connected directly to the power systems may be aircraft generators, motors, and other commutating machines, contactors and relays, vacuum tube devices, and so forth. The most commonly used methods for minimizing the effects of this noise on the airplane radio receiver are:

1. The use of well-shielded and well-filtered radio receivers with shielding on the antenna lead-in wire.
2. The use of well-grounded shielding on the wiring in the airplane.
3. The use of radio-noise suppression filters on the devices in the airplane which develop radio noise.
4. Combinations of the above three methods.

A second source of radio noise on aircraft is the aircraft engine ignition system. The magneto and spark plugs develop high radio-noise levels and require shielding. The ignition wiring between the magneto and the spark plugs may develop radio noise because of the formation of corona at high altitudes where the atmospheric pressure is low. Since corona is harmful to the insulation on these wires, methods for suppressing this type of noise require that the corona formation be prevented. This can be accomplished by enclosing the ignition wires in a properly grounded radio shield and maintaining approximate atmospheric pressure inside the shield by supercharging, by completely impregnating the shield with gel, or by other means.

Other sources of radio noise on aircraft may be the arcs that result from static charges built up on different parts of the airplane during flight. In general these types of radio noise can be suppressed by properly grounding the parts in which these charges develop.

The radio-noise problem on each different design of airplane is different, because the electrical systems on any two designs are not the same. The most economical solution to this problem is obtained when radio-noise measurements are made on the airplane after the electrical installation is complete.

S. W. Zimmerman (General Electric Company, Pittsfield, Mass.): The authors of this paper have gone a long way in presenting a clear-cut story on the status and limitations of present standard noise-measurement technique and the relationship between noise generated and noise received.

Of particular interest is their study of noise meter operation over a period of time. The variations experienced in the calibration of the different meters occasionally leads to embarrassment where different individuals or organizations are called upon to check each other, unless fairly wide tolerances are allowed. From data which the authors present at least 20 per cent deviation should be allowed at 10 microvolts and as much as 80 per cent deviation at 10,000 microvolts.

In connection with the calibration of these noise meters, it has also come within my experience that there is a variation in calibration with frequency which of course is a function of the alignment of the tuned amplifiers of the measuring system and the method of internal calibration which causes a variation throughout the frequency range of the instrument. Such data have been obtained for three different noise meters (not those used by the authors).

I should like to point out then that our present standard equipments to make measurements have associated with their use three inherent sources of error, scale form, frequency, and wave form. For certain more or less precision work these factors must be taken into consideration. One method is to refer all calibrations to a dependable accurate signal generator.

The relation between constant output voltages obtained with a reliable signal generator producing a sine wave over a wide range of frequencies and corresponding radio-noise meter readings is plotted in Figure 2 of this discussion with frequency as abcissa and radio-noise meter readings as ordinate. It should be noted that for perfect agreement these "isovoltage" curves (constant input voltages) should be straight lines which join up at ends of the frequency ranges and have an ordinate value equal to the calibrating voltage. However, because of the three factors mentioned above, we expect to experience a deviation from this condition. Position and shape of the curves give some idea of what variation might be expected between signal generator calibration and internal calibration. Note particularly the position of the broken line curves which are upper scale readings obtained from a two range instrument and should check the solid line immediately adjacent, or lower, which was obtained on the lower scale.

In radio-noise filter work a sine wave source of voltage has been a helpful basis for development and test work. Data obtained in this manner agree very well with

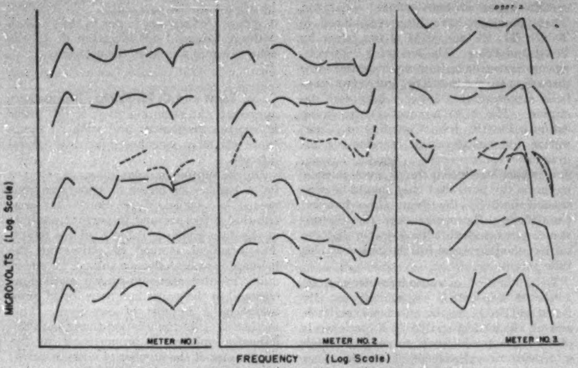


Figure 2. Meter readings for constant input to radio-noise meter

operation experience where the source of noise is due to brush sparking, and so forth, providing proper care is taken with measurements technique and proper correction factors applied.

The square wave generator has some possible advantages as a means of standardizing noise meters in that it approximates actual noise source conditions measured on equipment, but this type of standardization does open up another question, that of standardizing the square wave generator. Present oscillographic technique makes this possible but somewhat more complicated than sine wave testing and in my way of thinking perhaps is less desirable. We have trouble getting good signal generators to agree at 20 megacycles sine wave. Are we not asking for more trouble when we expect square wave generators to do this?

I believe that present noise-meter performance may be considered satisfactory in view of the complexity of the problem, but we must not lose sight of the fact that reasonable interpretations of radio-noise data must be made. If asked to produce electrical apparatus guaranteed to meet a certain specified limit of radio-noise performance, as expressed in microvolts, I believe that even our best radio-noise meters should be used only as transfer standards and should be referred to some type of reliable standard signal generator immediately upon making measurements if this limit of radio-noise performance is to be considered definite.

A. E. Anderson (General Electric Company, Philadelphia, Pa.): The paper by Foust and Frick indicates that considerable progress has been made in standardizing procedure and obtaining data. They have also indicated the desirability of obtaining additional data which will serve to confirm the original conclusions or suggest some modification.

Numerous measurements have been made on various types of circuit breakers, switches, insulators, and other apparatus found in switchgear, from which it has been possible to determine certain limits of radio-influence voltage that could be met in the course of manufacture. This part of the program has been facilitated chiefly by the relative simplicity of the insulating or insulated parts used in such apparatus.

When some of these devices are arranged in assemblies, the problem becomes somewhat more complex. First, there is the matter of several devices in parallel, in so far as testing is concerned, which may raise the level perceptibly. Secondly, the many and various items used in such assemblies make it difficult, at present, to establish a workable level. Not only may each item contribute appreciable amounts of *RIV* but the problem of determining their relative contributions, particularly if the measured *RIV* is high, may be exceedingly complicated. However, such (over-all) measurements have been made and will be continued as far as the present emergency permits, with the ultimate objective of establishing levels that can be applied to switchgear assemblies.

As a matter of some assurance, but not necessarily to show that the investigations have reached a final or semi-final stage, it may be said that a large majority of switchgear apparatus and switchgear assemblies have so far fallen in the classification of "No Complaints" as indicated in Figure 2 of the paper. In those few cases where difficulty was experienced in the field, it was found that the corrections were of a simple nature.

These remarks are given chiefly to indicate to the users of switchgear that while limits are not given for switchgear assemblies, and there have been no reported complaints, the subject is not resting here, but will be investigated further so that limits may be adopted ultimately for this type of equipment.

R. S. Tucker (Bell Telephone Laboratories, Inc., New York, N. Y.): The Foust and Frick paper is called herein the "Co-ordination paper" and the Aggers, Pakala, and Stickel paper the "Coupling paper."

I agree with the Co-ordination paper that it is desirable to improve the accuracy of radio-noise meters, and that the relatively small errors quoted in this paper are only attainable by experienced technical personnel carefully following the detailed directions in the manufacturers' information on these meters. In our experience the deviations will be considerably greater than these with ordinary use of such radio-noise meters. This situation is largely due to the relatively strict requirements on weight and cost of these meters. After the war it may well prove advisable to have a so-called "precision radio-noise meter" which could be used in development work by the various organizations and would be larger and more expensive than the maintenance-type meters which are at present available. In addition, developments in the art would very likely improve this latter type of meter. However, the accuracy required is by no means as great as that required for such instruments as ammeters; this is because of the variability in several of the factors governing radio interference.

In regard to these factors, two other sources of information will be of interest to people studying this subject: a British report on "Electrical Interference with Radio Reception" by A. J. Gill and S. Whitehead in the Institution of Electrical Engineers *Journal* 1938, pages 345 to 394 and an article in the Bell Laboratories Record for June 1942 entitled "Factors Controlling Man-Made Radio Interference" by R. A. Shetzline. Some of the material in the latter article bears directly on the work presented today. This material is given below in a form adapted to the present discussion.

First compute the signal-to-noise ratio—or better, the carrier-to-noise ratio—at the input of the listener's radio receiver. In order to do this, find the carrier voltage across the receiver input and the noise voltage at the same point. The carrier voltage is EH, where E is the free-field carrier field intensity in the neighborhood of the antenna and H is the ratio of the carrier voltage across the receiver input to this field intensity. (H has the dimension of height but is not the effective height of the antenna, which is the ratio of the open-circuit voltage to the field intensity.) The noise voltage across the receiver input is VAC, where V is the "on-line" radio-influence voltage at the source, A is the attenuation ratio from this point to a point on the line near the radio antenna, and C is the coupling ratio between the voltage at the second point and the voltage across the receiver input. The carrier-to-noise ratio is then the quotient of these two quantities, or $R = EH/VAC$. Hence $V = EH/RAC$. Now both H and C involve the voltage across the receiver input, which depends on the receiver input impedance; but the quotient H/C is independent of this impedance and is a convenient number to use to characterize the relative coupling to the radiated program field as compared to the coupling to the radio noise. This ratio H/C is called U. It can be described as the radio-influence voltage on the line near the antenna which would give a carrier-to-noise ratio of unity for a program field of one volt per meter. It is a property of the effectiveness of the antenna installation and the coupling to the disturbing system and is susceptible of statistical study. Thus $V = EU/RA$, from which the permissible "on-line" radio-influence voltage can be calculated. A limited number of measurements of U, for exchange-area telephone plant as the system carrying disturbance, gave a median value of about 30, and a value of about three which was exceeded in 90 per cent of the instances.

In the two papers under discussion, there appears to be included in limiting values for *V* no specific allowance for attenuation of the radio disturbance from its source to a point on the power system near the antenna, though certain particular cases of such attenuation are noted. This attenuation may be due to propagation down the line to which the interfering source is connected or due to coupling between this line and some other part of the power system which is closer to the antenna.

In the above formula the signal carrier and the noise were obtained by measurements across the radio receiver input and not by measurements of the field intensity underneath the antenna or of the voltage induced in a one-meter antenna at this point. This is a material distinction. The noise voltage at the receiver input often may be due chiefly not to direct radiation or induction into the antenna itself, but to coupling from the low-voltage circuits into the connection from the antenna to the radio set or the connection from the radio set to ground. This was the British experience, as stated in the report referred to above. Furthermore, the effectiveness of the antenna and lead-in in collecting radio program is measured by the ratio of the carrier input to the radio receiver compared to the field intensity of the radio carrier in relatively free space in the general neighborhood of the antenna. The field underneath the listener's antenna may be materially greater or less than this free-field intensity, depending on the effects of nearby wires and other metallic objects; and such local aberrations in the field necessarily are not included in the usual information on fields from broadcast stations.

The Canadian information on coupling and effective heights given in Part 4 of the Co-ordination paper is not presented in great enough detail to be sure whether it conforms to the above principles. It is understood that the data presented in the Coupling paper took into account noise induction into lead-in and ground connection, in a manner which is not clearly indicated in the preprint; and it seems that some further details along the lines of the above principles would be of value.

R. D. Evans (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Solution of co-ordination problems involving radio-receiving systems and power-supply systems has been undertaken by the Joint Co-ordination Committee on Radio Reception of the Edison Electric Institute, National Electrical Manufacturers Association, and the Radio Manufacturers Association. Progress to date has included

1. The development and adoption of instruments and methods of measurement for appliances and for high-voltage apparatus
2. The establishment of radio-influence voltage levels for appliances and small motors.

The corresponding characteristics for high-voltage apparatus have been discussed, but no levels have been adopted. To meet the commercial needs of this situation NEMA, after divergent discussions, adopted practices which set test voltages for radio-influence-voltage measurements and which define radio-influence-voltage levels for certain types of high-voltage apparatus. These practices have been reproduced in Tables III, VI, and VIII of the paper by Foust and Frick. The levels for high-voltage apparatus have, however, received some discussion in the industry, and views have been expressed that lower values were desirable. The NEMA practices are being submitted to the Joint Co-ordination Committee for consideration in adopting an industry-wide standard. Aggers, Pakala, and Stickel have not included levels in their paper on the basis that they should be considered first by the Joint Co-ordination Committee. However, under the circumstances, it is undoubtedly desirable that the subject should receive full discussion at this time.

Determination of radio-influence-voltage levels of apparatus, as pointed out by Foust and Frick, may be based on experience and on theoretical analysis. Experience is of limited value for such purpose, although it provides a valuable check on the other method. Presentation of the theoretical method was made by Mr. Aggers, and a statement of it is incorporated in the paper of which he is a joint author. Briefly, the method relates radio-influence-voltage (*RIV*) levels of apparatus and the field strength of satisfactorily received signals by means of the following factors:

1. Ratio of "on-line" *RIV* to test *RIV*.
2. Ratio of "under-line" *RIV* to "on-line" *RIV*.
3. Ratio of field strengths at antenna and at "under-line" locations.
4. Signal to noise ratio required for satisfactory radio reception.

Foust and Frick also present a similar analysis. The two presentations give closely the same result, except for the ratios of items 2 and 3. For the ratio of "on-line" *RIV* to test *RIV*, Aggers, Pakala, and Stickel give a ratio of 1 to 2.4, whereas Foust and Frick give a ratio of 1 to 1.5. A more important difference appears in the use of the coupling factor that gives the ratio of the field strengths at antenna and at "under-line" locations. Aggers, Pakala, and Stickel present coupling data from a survey in an urban area, while Foust and Frick present data taken in Canada by the radio division of the Department of Air Transport under the direction of Mr. H. O. Merriman. Both sets of data give closely the same percentage of antennas tested for coupling factors exceeding a given value. In the use of the Canadian data it is suggested that for 90 per cent of all antennas, a coupling factor of 1 to 3.2, corresponding to 30 per cent of antennas tested, should be used. Aggers, Pakala, and Stickel, however, found for the area surveyed that an average coupling-factor ratio of unity applied when distribution circuits were involved. The preceding discussion may be modified somewhat by a susceptiveness factor for antenna and receiving systems. This factor is listed in both papers, but the assumptions and manner of use are not clearly stated and should well be clarified.

The determination of the proper relative values for radio-influence-voltage levels of power apparatus, the broadcast signal strength to be considered, the percentage of listeners to be affected, and the areas in relation to the power line to be protected are complex questions which can be answered only by co-operative consideration by all the parties concerned. No standard can be set for any one factor in the problem without balanced consideration of all the other relevant factors in the problem.

C. M. Foust and C. W. Frick: The authors appreciate the attention given to this paper in written discussions and wish to thank those who have contributed for their helpful interest.

On the subject of radio-influence voltages for a number of pieces of apparatus connected in parallel, A. B. Smith has contributed a further and important analysis of the data given in Table X of the paper. The practical method of estimating the increase in radio-influence voltage for added units by the root-sum-square method as outlined in the paper has been found quite useful on a number of occasions. This method accepts the relation that the radio-influence voltage is proportional to the square root of the number of units in parallel, or in other words to the 0.5 power of n. Dr. Smith has shown by direct analysis of the measurements of Table X of the paper that even at the different voltage levels the exponent of n is nearer 0.38 for these insulators. The authors believe that this method of analysis should be extended to the other test samples.

The same question of radio-influence voltage of paralleled apparatus is mentioned in the discussion by A. E. Anderson, who calls attention to the difficulty of establishing over-all workable limits for switchgear assemblies. The authors appreciate this problem and recognize that where paralleled units are of different types and in addition affect each other, it is not possible to use a simple mathematical relation for combined radio-influence voltage. Further data, such as Mr. Anderson indicates, are now being collected and will clarify this situation.

F. O. Stebbins' comments on radio noise on aircraft constitute an interesting general view of a field of work which is particularly important at this time. The experiences of the authors in connection with aircraft apparatus measurements in both laboratory and field have been that, while helpful data can be gathered in the laboratory, as Mr. Stebbins points out, final performance checks are necessary on the airplane after the electrical installations are complete.

S. W. Zimmerman calls attention to a very important problem, that of the need for a basic standard of radio-influence voltage which would establish definite frequency and wave form characteristics. As he points out, the need for this is very urgent, particularly because of difficulties attendant to radio-influence-voltage checks in different laboratories and at different times. It is hoped that this problem will be given forthright attention because progress in radio-noise-meter improvements is closely related to such standardization.

In Mr. Evans' discussion of the general situation he agrees with the statements in our paper to the effect that the limits of radio-influence voltage for high-voltage apparatus as given in Table III and the test voltages as given in Table VI have been adopted by the National Electrical Manufacturers Association. It is hoped that the adoption of values by other committees will come in the course of time.

With regard to the determination of radio-influence-voltage limits of apparatus as based on experience and theoretical analysis, it seems to the authors that experience is always the final criterion. However, since we had data on both apparatus experience and circuit measurements in the field available for analysis, we included both in the paper. It is interesting to note that the limits derived from these two differing methods are in good agreement.

The discussion presented by R. S. Tucker is very helpful, and his suggestion as to the advisability of a "precision radio-noise meter" is worthy of consideration. However, it is important to bear in mind that even with such a noise meter at hand the question of its calibration on a wide range of types of noise voltage would still be present.

Mr. Tucker's analysis while not greatly different from the one given in our paper does seem to take attenuation, along lines, into consideration. Questions of wave shape and reflection possibly make this subject a difficult one to handle. Such measurements as we have made on field circuits show considerable complication and are, therefore, difficult to be used in a practical way.

With regard to the Canadian data, we did not reproduce this in detail, having in mind it was covered by items 11 and 12 in the bibliography.

Effect of Radio Frequencies of a Power System on Radio-Receiving Systems

Discussion of paper 43-58 by C. V. Aggers, W. E. Pakala, and W. A. Stickel, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, April section, pages 169–72.

R. S. Tucker: For discussion, see page 457

R. D. Evans: For discussion, see page 458.

An Instrument for the Determination of Contact Making and Breaking Time

Discussion and author's closure of paper 43-1 by Walther Richter and William H. Elliot, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, January section, pages 14–16.

H. L. Clark (General Electric Company, Schenectady, N. Y.): The "electronic time gauge" described is similar in many respects to another recently designed instrument which we have found useful for applications requiring a direct indication of time. This time-interval meter reads directly on a 0–10/100/1,000/10,000-millisecond scale with $\pm{}^1/_2$ per cent accuracy. The indication is maintained with a drift of 0.2 per cent per minute or less. This accuracy and stability is obtained by an inverse feedback scheme which serves to hold the capacitor charging rate constant to within 0.01 per cent and at the same time corrects for leakage from the charged capacitor. Normal variation in the vacuum-tube characteristics does not affect the accuracy. Coincidentally, the full scale indication of this instrument is determined for a change of 25 volts in the capacitor charge compared to 20-volt change required in Richter and Elliot's time gauge.

The original application of the time-interval meter was in measuring the timing of camera shutters. A phototube and amplifier operate an electronic trip circuit to allow the capacitor to charge during the time the shutter is open and passing light onto the phototube. For another measurement the timing is started by an electrical contact and stopped by light on the phototube. The circuit also has been adapted for many other measurements by arranging it to measure between almost any sequence of rise or fall of light or making or breaking of an electrical circuit.

Walther Richter: The essential difference between the "electronic time gauge" described by H. L. Clark probably is that he had to measure the duration of the presence of one quantity, such as light, or current, or voltage, while in our case the duration of the simultaneous presence of two quantities, that is, voltage and current, had to be determined. We believe that the circuit element accomplishing this is the distinguishing novel feature of the instrument. As to the accuracy mentioned by Mr. Clark, there are undoubtedly many cases where this high degree of accuracy is desirable and necessary; in our case, however, a portable, direct reading instrument was desired, and, since the small switchboard instruments available for this purpose have an accuracy of about two per cent of full scale deflection, nothing would have been gained by refining the electronic part of the circuit.

Rural Electrification Engineering and Electro-agricultural Engineering

Discussion and author's closure of paper 43-61 by M. M. Samuels, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, April section, pages 193–7.

Donald A. Henry (nonmember; Stone and Webster Service Corporation, New York, N. Y.): The matter of rural electrification is one which is very near to my heart, as all of my ancestors were farmers. I want to compliment the Rural Electrification Administration on having done a good job. It is stated that at the end of 1942 it has been responsible for extending rural electrical service to about 1,000,000 customers, of whom about 800,000 actually are farmers. Of course, this has been done through a considerable expenditure of funds of the Federal Government of which, through taxes, the electrical utilities contributed their part.

Rural electrification is not a new thing and did not begin in 1935. In 1920 I was the chief electrical engineer on the staff of the Illinois Commerce Commission, and as such I spent a great deal of time in investigation and study of the matter of rural electrification. On June 20, 1923, that Commission approved its general order 100 entitled "Rules and Regulations for Rural Electrical Service"—mind you, not rural *line extensions*, but *rural electrical service*.

I am sure that all of you have heard of the Committee on the Relation of Electricity to Agriculture of which my good friend, E. A. White, was the chairman for many years. This committee and the staff of the Illinois commission worked together with electrical utilities in Illinois for the promotion of rural electrification in Illinois for many years before 1935. Many electrical utilities had graduate agricultural engineers on their advisory staffs whose sole business it was to promote rural electrification.

A glance at statistics may be useful. In 1932 there were 700,000 farmers served with electricity; in 1935, 790,000; in 1941, 2,353,000 of which the REA had, by that time, attached 725,000. In 1942 the REA claims 1,000,000 customers, of whom probably 800,000 actually are farmers. These figures do not look as though rural electrification began in 1935 nor do they indicate that even in 1942 the REA was the only one in the field. In 1927 practically every electrical utility in Illinois had on file with the Illinois Commerce Commission special plans under which rural electrification was to be extended in the territories served by them.

The electrical utilities early found that it was not sufficient to have a plan of service and invite the farmers to come and take it. As is the case with every other comparatively new thing, it had to be sold. The electrical utilities spent large sums in selling electrical service and electric appliances for using the service to the farmers.

The early pronouncements in connection with REA were that, if the lines just could be brought to the farmer's door, he would do all the rest, and these large expenditures for popularizing the service were superfluous. I see frequently a small publication entitled *REA News*. Later copies of this publication state that the national organization of REA is sponsoring traveling circuses in order to popularize the service in rural areas and sell electrical appliances to the farmer. This does not seem to indicate that the farmers fall over themselves to use the service as soon as it comes to their door.

True, I think that the REA is doing a good job in selling electrical service to the farmer, but the utilities also did a good job, and they did the pioneer work which is the hardest job to do. We all remember when electric refrigerators sold at $500 apiece, and the manufacturer was lucky if he could sell a few hundred a year. Now, over a million are sold each year at $100 or less apiece. The fellows who did the pioneer work took the bumps, and, if they made any money after their development and promotion expenses were paid, it was a very small amount. Later on, after the pioneering was done, other manufacturers came in and reaped a share of the business in much easier fashion. Yes, the REA has increased to a considerable extent the rural areas served with electricity, but it has had the advantage of the pioneering work done by the electrical utilities. Electrical utilities found that they had to sell the service even after the lines were in. The REA has found it has to do the same thing, but together the REA and the electrical utilities are undoubtedly doing a better job than either could accomplish separately.

Hamilton Treadway (Rural Electrification Administration, St. Louis, Mo.): The author's point that before 1935 there was no rural electrification engineering needs emphasis. Table I summarizes consumer use of electricity on one of the rural electric cooperatives financed by REA.

Table I

Group	Appliances	No. of Users	Average Kilowatt-Hours Per User Per Month
A	Lights, refrigerators, small appliances	149	91
B	Lights, refrigerators, range, small appliances	25	149
C	Lights, refrigerators, water heaters, small appliances	23	353
D	Lights, refrigerators, range, water heater, small appliances	102	506
	Average all users	299	258

By our urban standards the users in Group *D* would be considered fully electrified. But actually only a small part of the total farm unit has been touched. Obviously a new standard for rural consumers is necessary. The farm must be considered as a production unit to which electricity can be adapted in every phase of the production process. This view must give rise to new concepts of service standards. For one thing greater continuity of service is required than was ever thought necessary a decade ago. An example will suffice to illustrate. Assume service is supplied to 500 chicken brooders, each handling 200 chicks. Under extreme conditions a power failure of any duration would result in the loss of 100,000 chicks which would mean a loss to the farmers of $25,000 in a few hours.

Considering the farm as a production unit, demanding a high standard of service will bring with it new methods of operation, as well as new tools and work equipment. The sale of energy will give way to supplying service, followed by new methods of metering and charging for the service performed by electricity. Furthermore, a closer relationship between the development engineers of the manufacturers and the distribution and operation engineers will be necessary.

That rural electrification before 1935 was thought of only as rural line extensions must be obvious to all who have been connected with the industry. In Illinois prior to this time all rural extension policies filed with the Illinois Commerce Commission contemplated construction of extensions to serve individual consumers with feasibility in each case considered separately. In fact it was not until 1937 that the first application by a private utility was filed with the Commission, covering an entire area and providing service to hundreds of farmers with the feasibility of the entire project considered as a whole. As late as 1940 only one other such application had been filed with the commission by a private utility in that state. None will argue that electrical service was not brought into the rural area prior to 1935. On the other hand, everyone will agree that nothing more than an extension policy existed. It was an extension policy, not only from the view of feasibility but also as to engineering. The primary distribution system of the urban community was extended into the outlying rural areas. There was no real rural distribution engineering as such.

As the author has pointed out, only the surface has been scratched in rural electrification engineering. Much new thinking needs to be done by the manufacturers of both utilization and distribution equipment. In this new thinking the distribution and operation engineers must lead.

H. A. Enos (American Gas and Electric Service Corporation, New York, N. Y.): Mr. Samuels has presented a very interesting paper in which he has listed many possible and desirable developments of electrified equipment which may be of considerable value to farmers. None of these items, however, is new or revolutionary, but Mr. Samuels does stress the fact that equipment developed for these purposes should be low in price, rugged, and simple in operation. He points out quite properly that only quantity production can bring about such devices. A few of the items mentioned by Mr. Samuels have been investigated in the past and found to be not technically nor economically feasible. That does not mean they should not be re-examined from time to time.

Mr. Samuels devotes considerable space in his paper to the discussion of distribution engineering, both in general, and with respect to that phase of distribution engineering concerned with rural distribution lines. But Mr. Samuels, unintentionally perhaps, has made quite a few statements with regard to various aspects of this subject which may be misleading to engineers and others who are not fully familiar with distribution engineering and its history.

Mr. Samuels states that distribution engineering is a very young branch of engineering. Naturally, it cannot be any older than electric distribution itself which began with Edison's first central station in New York City in 1882. Edison had to do some real pioneering in distribution engineering in order to put his first distribution system into operation. Very soon he made further steps in engineering his distribution system when he changed from two-wire to three-wire, and again when he instituted the low-voltage network. From that time until the present, distribution engineering has been continuously in existence, bringing about the slow, it is true, but nevertheless continuous improvements in distribution which is proving itself today a vital factor in the successful prosecution of our war effort.

Mr. Samuels quotes a paragraph from his article included in the 1937 report of the National Resources Committee, indicating an alleged almost total lack of distribution engineering previously. But I doubt if the record bears this out. It is quite true that most engineers solely concerned with generation and transmission ignored distribution engineering. It is hardly true, however, that distribution engineering was ignored by distribution engineers, of whom there have been a great many throughout the history of the central-station industry. In 1909 H. B. Gear and P. F. Williams published a notable book on distribution systems and distribution engineering which they state was prepared because of many requests from engineers engaged in distribution work for information bearing on the details of their work. This book was quite a thorough treatise on the subject of distribution engineering for the time when it was published. I myself started the practice of distribution engineering in 1915, following in the footsteps of such men as B. L. Huff and Paul Reyneau, and I know first hand that we were doing real distribution engineering at that time. I also know first hand that well-engineered large-scale rural distribution systems were operating in Indiana in 1922. It is true that some small power companies dispensed with the services of distribution engineers, not because distribution engineering did not exist, but because the managers of those properties mistakenly felt that they could get along without the assistance of distribution engineers. The result of that situation was that, when those small companies were incorporated into large integrated systems, the distribution engineers had to clean up the mess, which they certainly knew how to do, and in so doing they produced the well-designed distribution systems which are giving such a good account of themselves today.

Mr. Samuels also states that distribution engineering really started with the introduction of the a-c network. He does not give the date but implies that it was very recently. He may be referring to the introduction in 1922 of the automatic-network protector which in itself is only an auxiliary device and is not essential to the operation of a network. As a matter of fact, a-c secondary networks have been in successful operation for at least 40 years in various forms, including some three-phase four-wire wye networks.

Mr. Samuels relates at length the difficulties he and his engineers have been having in learning how to engineer rural lines but admits that they already have learned a lot they did not know before. The trial-and-error method had its advantages, but the distribution engineers of the country already by the time the REA came along had developed rural distribution designs that were technically and economically highly advanced and of which they might have taken advantage. Perhaps they did.

A. B. Campbell (Edison Electric Institute, New York, N. Y.): A review of the literature pertaining to the furnishing of electrical service to rural customers does not appear to bear out the author's statement that very few had spoken of rural electrification. This term appears frequently prior to 1930 in the literature pertaining to this subject.

It is not clear what significance the quoted statement on the first page bears to the purpose and intent of this paper, but it is in error in so far as it concerns urban distribution generally throughout the country.

Since urban distribution involves consideration of a wide range of load densities, it may be said fairly that the basic engineering and economic features of all distribution to consumers were developed in the course of these studies. Some of the most important features in modern distribution systems were developed prior to 1930 and are still considered good practice in the engineering of urban distribution systems. I refer particularly to such features as higher primary voltages, common primary and secondary neutral, interconnection of lightning arrester and secondary neutral, rapid feeder reclosure, joint use of poles by supply and communication companies.

Those interested in the attention that was given to distribution engineering prior to 1930 are referred to the references following this discussion. Furthermore a review of the reports of the overhead systems and underground systems committees of the former National Electric Light Association and other similar literature issued prior to 1930 shows that much consideration was given to the engineering and economic aspects of urban distribution systems, even long prior to that date.

During the years 1921 to 1934, inclusive, construction expenditures for generation, transmission, and distribution in the electric light and power industry were approximately:

Generation	$2,655,342,000
Transmission	$1,807,394,000
Distribution	$3,139,812,000

With construction expenditures for distribution during this period totaling more than either of the other two items, does it seem likely that engineering in distribution would have been neglected to the extent mentioned in the quoted paragraph of this paper?

With regard to rural distribution, this too has been given thorough consideration in both the engineering and economic aspects for at least the past 18 to 20 years. These phases of rural electrification were studied by many engineers years ago, and one needs only to review the technical literature on this subject to realize the extent to which engineering thought had been given to the development of electrical service in rural areas. In the late '20's, rural lines were being built with longer spans and vertical-type construction, particularly in the light-loading (climatic) territory.

An interesting history of rural electrification is given in three articles by Royden Stewart, published in the Edison Electric Institute *Bulletin* for September, October, and November of 1941. Also, "Some Problems and Opportunities in Rural Electrification" by Grover C. Neff, in the April 1939 issue of the Edison Electric Institute *Bulletin*, shows that the problems in rural electrification of major importance have been under consideration for many years.

Joint studies on inductive co-ordination of supply and communication circuits began in the early '20's, and, while these studies and investigations involved primarily urban distribution systems and transmission lines, most of the underlying principles and practices which apply today had been developed by 1926. The reports of the Joint General Committee of the NELA and Bell Telephone System of December 9, 1922, contains principles and practices for the co-ordination of electric supply and communication systems which still form the basis for studies on this subject.

Concerning the economics involved in giving rural service, a review of the literature of 12 to 15 years ago shows that in many areas of the country intensive studies were made and resulted in what probably forms the foundation of many present-day ideas on this subject. What is probably the greatest advantage to these early studies is that they resulted in the development of many sound ideas and good practices upon which to build. By the time the author became actively engaged in rural electrification, a good foundation existed on which development could go forward.

I have no desire to minimize the progress that has been made in rural electrification during the past few years. Much progress has been made, but, under the conditions and circumstances that existed, much progress should have been made. It might be pointed out that of the approximately 2,000,000 farms now receiving electrical service, nearly one half were receiving service prior to 1935.

References

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2. Electric Distribution Systems, Gear, Williams. 1911.

3. Overhead Systems Handbook, National Electric Light Association, 1914.

4. Overhead Systems Reference Book, National Electric Light Association, 1927.

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E. E. George (Ebasco Services, Inc., New York, N. Y.): This paper apparently attempts to portray the development and prospects of rural electrification but omits entirely any reference to the development work done by private utilities, particularly during the period from 1925 to 1935. Even a casual reference to the publications of the National Electric Light Association or of the Edison Electric Institute during this period will show that a large amount of development work was being carried out by utilities all over the country. For instance, the Alabama Power Company had a rural electrification engineer and a large staff engaged in this work many years before 1930.

In addition to the material omitted from this paper, there are quite a few statements in the paper which give an incorrect impression of what actually occurred.

Many readers will not be misled, but AIEE papers represent the state of the art to students, young engineers, engineers in foreign countries, legislators, and others not familiar with the electrical industry and its history.

The picture of inadequate distribution planning and construction as outlined by the quotation given on the first page of Mr. Samuels' paper really applies more closely to the small isolated municipally owned system than to those built by private utilities.

Even as early as 1927 there was much study given by relay engineers of the private utilities to oil circuit breakers for rural lines and to the development of more accurate fuses with better time characteristics.

To evaluate the possibilities of constant current transmission with constant potential load as proposed by P. H. Thomas, one need only look at the costs of rectifying and regulating equipment. Even the most optimistic prophecy as to future reductions in the cost of such equipment would not permit its application competitively with present equipment, especially for rural loads.

It is very difficult for an engineer familiar with general utility operation to see how rural distribution engineering has any relation to the engineering of a-c networks.

Mr. Samuels suggests that a small, cheap, and reliable motor-driven water pump should be developed. As long ago as 1915 such a pump was being produced in quantity by a specialty manufacturer in Dayton, Ohio.

Mr. Samuels refers to the present practice of the REA co-operatives in promoting sales and purchasing merchandise for their customers. It will be recalled that the private utilities were very successful in merchandising until their efforts in many states were stopped by legislation on behalf of the small electrical dealers. Mr. Samuels likewise refers to the savings possible under the "group purchase plan," a method long used by utility holding companies.

W. R. Bullard (Ebasco Services, Inc., New York, N. Y.): In this paper Mr. Samuels has called attention to a number of current engineering problems concerned with distribution of electric service to the farmer and the full utilization by the farmer of such service. He has pointed out that the solving of these problems requires extensive and competent engineering attention. I heartily agree.

Some of the problems mentioned in the paper are old problems modified by conditions specific to rural electric service. Others have claimed attention only within recent years. The term, rural electrification engineering, quite properly implies a specific branch of engineering which, however, as mentioned in the paper, is embraced in the more general distribution engineering. The underlying fundamentals of the latter are the same as those of the former and have been studied and utilized since the early beginnings of the electric-power industry but, of course, with increasing knowledge and technique as the industry developed. I cannot wholly agree with the statement in the paper that distribution engineering started with the introduction of the a-c network. True, that was an outstanding development and was largely coincident with a particular period of advances in the art. But the practice of distribution engineering is, at least, as old as Edison's first sizable string of incandescent lamps and the accompanying problems of making them burn at satisfactory brilliancy. Also outstanding developments were observable even in the very early days of electric distribution—including such as the introduction of the a-c transformer and the transition from d-c to a-c distribution, utilizing primary voltages in the latter; also the development of switching equipment of a great variety; the introduction of circuit voltage regulators; and the development of insulated cables, both high- and low-voltage, for underground distribution involving a very comprehensive technique of design and application. While sympathizing with the point of view that distribution engineering has not always kept abreast of its growing importance, I believe undue emphasis on this should not cause to be overlooked the very extensive developmental work of the past that forms the basis for present-day distribution engineering including that branch specific to rural service.

The specific problems mentioned in the paper are timely and well stated. I will comment on only one item, that of grounding. Mr. Samuels calls attention quite properly to the importance of this feature. He also stresses the need for adequate testing to insure its quality. Basically I do not disagree as to the value of and need for testing. However, under some conditions, I believe *periodical* testing can be overemphasized. Where the grounds are few in number, certainly their resistance should be checked as experience requires. But in the case of multigrounded neutrals with large numbers of ground connections, it frequently may be found difficult to justify periodical testing of individual grounds except at very infrequent intervals. This leads me to remark on a very interesting sidelight of design. Calculations of grounding impedance with various spacings of ground electrodes show that a neutral may be surprisingly well grounded, even though individual ground resistances are quite high, provided that they are reasonably closely spaced. For this reason a comparison of low-cost grounds closely spaced with a few high-cost grounds frequently may favor the former both as to cost and dependability: for instance, butt-wrapped grounds of scrap wire on every pole or every second pole or in whatever spacing may be considered necessary, as compared to driven rod grounds of sufficient number to give the same overall ground impedance. Of course, the butt-wrapped grounds have to be installed when the poles are set. Also the soil at the bottom of the pole must have at least a modicum of conductivity.

M. Kenneth Brody (McCall Corporation, New York, N. Y.): The following are comments on the paper presented by M. M. Samuels at the technical meeting in New York on January 29.

In Mr. Samuels' paper I noted the statement that 25,000 acres in the state of Oregon which were irrigated showed a gross increase of $40 per acre over nonirrigated acres. This is, of course, a tremendous increase in revenue for the farmer, but I wondered what the initial cost of irrigation per acre was and what the annual upkeep per acre cost the farmer. And I also wondered where a man in Oregon with a large farm could get the money initially to irrigate his lands.

Throughout the entire paper no mention was made of how much man power could be saved through the use of electrification, and I wondered if there were any figures available on this. I also wondered whether critical materials are now available for the production of electrified farm machinery and whether some method of group financing had been arranged to take care of getting these much-needed electrical aids to the farmer.

Mr. Samuels said that, even in dairy farms where electricity can be used for many many purposes, it was shown that ten per cent of the connected load is used for farm operations, and the other 90 per cent is used for lighting, household appliances, and so forth. If this is true, I wonder why the acceptance of electrical farm machinery for dairy farming was so low. This equipment has been on the market for many years, and, if the acceptance is as low as stated by Mr. Samuels, it would be interesting to know the reason for this low acceptance. Is it one of the following?

1. Is the initial cost of machinery too high?
2. Is the operation of machinery too complex?
3. Are these farms adequately wired for use of this machinery, or is it necessary for the dairy farmer to spend a rather large sum for adequate wiring to accommodate the machinery?

Mr. Samuels mentioned that one manufacturer has developed a self-contained quick freezer and cold-storage unit having a capacity of 20 cubic feet, and I notice that this could be produced in quantity to cost less than $300 per unit. It would be interesting, in connection with this, to know the following:

1. The lowest plate temperature obtainable for freezing.
2. The average temperature of the storage compartment.
3. The flexibility of the storage and freezing compartment and the mechanical arrangements for getting foods in and out of this locker.
4. The operation cost of this unit.

Various agricultural experiment stations have already proved that temperatures as low as −30 degrees Fahrenheit are desirable for freezing fruits, vegetables, and meats, and temperatures as low as −10 degrees Fahrenheit are necessary adequately to preserve these products. Poultry, for example, tends toward rancidity at the end of only 30 days' storage when stored at temperatures higher than −10 degrees Fahrenheit.

Mr. Samuels said that a flour mill weighing only 25 pounds and able to grind 20 to 40 pounds of flour per hour could be made a quantity product for $25 per mill. Since the average farmer for his personal use will use approximately 400 pounds of flour a year, if he bakes, will he invest $25 for a piece of equipment that he will use for only about ten hours a year? Of course, it is a different story if the farmer plans to sell whole-wheat flour and cereals already ground.

Regarding Mr. Samuels' comments on dehydration, I feel that a great deal in research has to be done in the field of vitamin retention in dehydrated foods before home dehydrators become accepted. The unstable vitamins such as A and C are very elusive, and prolonged exposure to heat and air may result in the loss of practically all these vitamins. I heartily agree that home dehydration would eliminate the waste of some products, but, unless the farmer can find some commercial use for these home dehydrated products, I can see no particular point in his dehydrating more than he and his neighbors can use.

I believe that the War Production Board recently ruled that some critical materials will be made available for the production of home dehydrators.

In reference to storage-battery-driven electric tractors, I can see no particular advantage in this type of equipment, and I am of the opinion that Diesel operation in the long run would be more economical. In line with storage-battery-driven tractors, however, it is quite possible and feasible that wind generators could be used to charge the batteries.

The development of a small pasteurizer for farm use seems to be an excellent idea, and I believe that it has been found recently that subjecting the milk to a temperature of 161 degrees Fahrenheit for about 15 seconds is equally as efficient as the older system of subjecting it to 143 degrees Fahrenheit for 30 minutes. Along this same line, I think the farmer could certainly use a bottle sterilizer that is cheap and efficient.

In its entirety, I think Mr. Samuels' paper was an excellent one and will start a lot of thinking along the lines he mentioned. I certainly think that it is part of the duty of every electrical engineer who is in a position to do so to make life easier for the American farmer. The farmer is one of the few people in America today who works a minimum of 12 hours a day, seven days a week. I think it is decidedly the electrical engineer's job to devise equipment for the farmer that he can buy and operate at a low cost and that will provide more leisure for him than he has ever had before. It is only very very recently that we electrical engineers began to appreciate the farmer's plight—and it took a war to bring on that appreciation.

M. M. Samuels: I am glad to see that the paper brought out such a lively discussion.

Mr. George's reference to rectifiers in connection with P. H. Thomas' proposed constant-current system is incorrect. P. H. Thomas did not propose d-c distribution. He proposed constant-current a-c distribution requiring neither rectifiers nor inverters nor regulators.

Mr. George's statement that there is a small, cheap, and reliable motor-driven pump manufactured in quantity in Dayton, Ohio, is indeed very interesting, and I would like to know more about it, if it is the kind of pump I had in mind, one to cost not much over ten dollars complete.

The answer to D. H. Henry's comments relating to the Illinois Commission will be found in the discussion by H. Treadway.

The most constructive contribution in the whole discussion is that by W. R. Bullard, relating to the important problem of grounding. I hope that everyone will appreciate Mr. Bullard's competent statements as I do. It would be helpful if Mr. Bullard would find an opportunity to elaborate some more on the subject, possibly in a special paper. I agree with Mr. Bullard that engineering analyses should be exhausted before elaborate tests are undertaken.

In this connection I am particularly disappointed in the discussion presented by H. A. Enos. Mr. Enos' discussion is not engineering; it is just polemics.

Mr. Campbell presents figures to show that in the years 1921 to 1934 more money was spent on construction of distribution than on construction of either generation or transmission, but he fails to report the corresponding amounts spent on engineering for each of the three branches.

Many of the questions relating to food, particularly those by M. K. Brody, are pertinent. It will not be possible within the scope of the paper to give competent answers to all of them. The authors of the questions not fully answered here are referred to authorities in the field of nutrition and home economics. Here I will have to limit myself to the three developments referred to in the paper. I am indebted to Eugene C. Meyer for the following statement referring to these three items.

1. Freezer and Storage Chest

The particular freezer and storage chest referred to has a plate temperature of approximately −10 to −15 degrees Fahrenheit during freezing and depends upon air movement by means of a small fan to secure a sufficiently fast freeze.

The average temperature of the storage compartment is 0 degrees Fahrenheit.

No special mechanical arrangements are now provided for getting foods into and out of the storage. Accessibility of foods of various kinds must be arranged for during loading. No doubt, there will be improvements developed to facilitate storage and removal of products.

Cost of operation for this unit, to compensate for heat leakage with an 80-degree differential will be approximately 110 kilowatt-hours per month. This will vary through the year, depending upon temperature conditions. Cost of freezing the food, stored in a chest of this size, would require approximately 240 kilowatt-hours annually.

Temperatures as low as −30 degrees Fahrenheit are not necessary in freezing products for home consumption, particularly when a fan is used. The small improvements in quality, which such a low temperature may effect, are not worth the extra cost. In the case of fruits and vegetables in particular, D. K. Tressler of New York State Agricultural Experiment Station states that other factors as variety, maturity, handling time, and conditions from harvest to the point at which sufficiently low temperature to stop enzymatic and bacterial action is attained are of most importance.

A storage temperature of 0 degrees Fahrenheit is adequate for storing farm products. Tressler, DuBois, and Fenton state in their paper, entitled "The Effect of the Rate of Freezing and Temperature of Storage on the Quality of Frozen Poultry," *Refrigeration Engineering*, August 1942, "At 0 degrees Fahrenheit, chicken fat shows signs of rancidity in ten months." Pork, another product difficult to store, was found to be of excellent quality after a year's storage in the REA-sponsored chest under test at the Beltsville Research Center by the Bureau of Home Economics, United States Department of Agriculture.

2. Dehydrator

Maximum retention of vitamins with any method of food preservation, whether it be canning, freezing, or dehydration, necessitates following certain procedures. Considerable work in establishing such procedures has been done, and much is now in progress to insure a good product. This method of preservation should naturally be limited to those foods best-suited, except during emergency.

Vitamin losses, regardless of the method of preservation, occur to a large extent during harvesting and preliminary treatment and also during the storage period. It is true that vitamins A and C may be destroyed easily during dehydration, but according to Cruess and Mrak (University of California), A is fairly well retained under controlled drying conditions and C loss can be minimized by blanching.

3. Flour Mill

The principal advantage afforded by the use of a home flour mill is through the production of a whole-wheat flour which contains the bran and germ of the wheat kernel, where most of the minerals and vitamins are stored. Quoting from the Department of Agriculture REA *Bulletin*, "Home and Community Milling": "In home-ground whole-wheat flour, the amount of vitamin B_1 (thiamin chloride) is approximately six times as great as in ordinary white flour; the amount of iron, five times; phosphorus, more than four times; calcium, more than twice; magnesia, eight times. The superiority of whole-wheat flour is even greater with respect to vitamin B_2 (riboflavin) and vitamin E. In addition to whole-wheat flour, whole-wheat cereals, corn meal, and grits are other nutritious and palatable products that can be milled at home."

Relative to costs, the following analysis may be made: John R. Murlin, professor of physiology, Rochester University, estimates the annual per capita consumption of wheat in the United States at 240 pounds. At this rate, the average family of $4^1/_2$ persons consumes about 1,080 pounds per year or 18 bushels. With wheat selling at one dollar per bushel, and considering that, on the average, a bushel of wheat (60 pounds) will buy only 20 pounds of white flour at the retail store, there would be an annual saving to the family of $36.

Mr. Brody also raises the question of manpower saving by electrification. Very little authentic data have been available up to now. I am presenting the first definite figures received from 41 farms in Ohio and Indiana.

Though few in number, these 41 farms collectively show an increase of 27 per cent in farm acres, 50 per cent in irrigated land, 176 per cent in acres of seed corn, 46 per cent in number of milk cows, 21 per cent in beef cattle, 35 per cent in market hogs; 250 western ewes were added, farm grown lambs show no total change, and 750 feeder lambs were discontinued. There was also an increase of 710 per cent in meat processing, 55 per cent in laying hens, 57 per cent in chicks brooded, 130 per cent in chicks hatched, 109 per cent in turkeys raised, 500 per cent in apples marketed, and 95 per cent in vegetables marketed. Included in the foregoing are 19 new enterprises. There was a total decrease of 6.3 per cent in labor, men (equivalent).

The current farm labor (men equivalent) reported, shows the labor to be approximately 40 per cent less than would be needed according to the approved War Manpower Commission schedule requirements for classification of a farm worker as essential.

The average monthly consumption of these 41 farms in 1942 was 466 kilowatt-hours per farm.

In closure I hope that this paper has

1. Demonstrated to the electrical engineer the need for more electrical activity in the field of rural electrification engineering and electroagricultural engineering and the opportunity in this specific field for creative engineering activity.

2. Demonstrated to the electrical manufacturer that a new market is now open waiting for a new industry to supply it. This industry can only be created by the electrical manufacturers.

Estimating Electric Loads for Military-Training Bases, Federal Housing, and Other Wartime Projects

Discussion and author's closure of paper 43-60 by Harold M. Potts, presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943, and published in AIEE TRANSACTIONS, 1943, May section, pages 246–54.

R. F. Hamilton (War Production Board, Washington, D. C.): In looking over Mr. Potts' paper before this meeting, there was some confusion in my mind in that there seemed to be such wide divergence between many of the figures given and those to which your speaker has had access in several government departments, responsible for these wartime loads. Mr. Potts' present explanation to the effect that his figures cover installations as proposed, but not actually built, has clarified this question.

I would like to make clear at once that in speaking here, I speak for myself and state my own views only as an engineer, and do not represent any organization or government group.

The responsibility for providing necessary and adequate electric facilities in military establishments is that of the Corps of Engineers for those of the War Department and that of the Bureau of Yards and Docks for those of the Navy Department. In both organizations there are competent engineers who have given much thought to the type of problems here discussed. Reports and performance records are obtained from each field location every month, and many tests have been made to obtain design information. I am not familiar with the procedure of the organizations which handle Federal housing projects, and so I shall not discuss these.

It was your speaker's privilege at the last winter convention, to offer a paper entitled "Synthetic or Equivalent Load Curves"[1] and, in the discussion, to present some typical curves covering maximum demands and load factors at temporary camps based upon results obtained at 65 locations. It was then pointed out that curves of demand or load factor versus connected load gave more consistent results than those of demand or load factor versus number of officers and men, but the latter were found to be more useful in the military organizations because

Table I

	Kilowatts
General lighting	6,500
Street lighting	60
Protective lighting	400
Power	6,000
Electric cooking	10,000
Electric heating	40
Miscellaneous	1,000
Total connected load	24,000

Consumption—3,171,300 kilowatt-hours for the month of December.
Maximum demand during the month—6,920 kw.

of the practice of considering many items in terms of the number of men involved.

Of interest in comparison with the figures in Mr. Potts' paper was an expected demand of 60 watts per man for a 35,000-man camp which had no electric cooking. For camps in the north, the demand would be higher usually than those in the south because of the motor applications on heating systems in the north. Also, the amount of power required for pumping water and for sewage disposal varies greatly, depending on local conditions. In the mountains of the Northwest, the power load is above the average because of the pumping involved.

The Corps of Engineers has not authorized electric cooking in temporary camps except for diet kitchens and hospitals. Experience with electric fat friers in some of the earlier camps, coupled with the increased need for saving power and critical materials, led to the discontinuance of these too.

Mr. Potts has specified a connected cooking load of 12,885 kw for an Army training base of 35,000 men. He has also shown slides of loads at a permanent fort in the Northwest from which many of his basic data are taken. This fort has the largest cooking load of its kind in the country—10,000 kw connected—but there are 58,500 officers and men stationed there. The connected load for this fort as of December 31, 1942 is shown in Table I of this discussion.

Table II

Officers and Men (December 31, 1942)	Connected Load Kilowatts: All Lighting	Connected Load Kilowatts: Heating and Cooking	Connected Load Kilowatts: Power and Miscellaneous	Connected Load Kilowatts: Total	Maximum 1942 Demand *	Demand in Watts Per Man
1. 36,830	8,670	980	4,520	14,170	1,770	48
2. 65,550	14,175	6,195	10,615	30,985	7,344	112
3. 29,470	6,890	1,280	10,290	18,460	3,000	102
4. 35,090	3,052	2,981	1,385	7,418	3,200	90
5. 32,676	7,430	2,020	3,100	12,550	3,500	107
6. 22,667	3,725	3,100	3,480	10,305	3,500	154
7. 22,487	3,448	1,350	6,740	11,538	3,000	133
8. 41,005	4,130	4,000	1,250	9,380	3,000	73
9. 46,323	4,329	1,630	5,629	11,588	2,600	56
10. 31,516	2,522	1,758	1,328	5,608	2,200	70
11. 48,469	4,222	3,972	2,575	10,769	4,200	87
12. 24,171	4,613	2,285	3,112	10,010	2,000	83
13. 82,036	13,865	5,000	4,574	23,439	7,580	92
14. 66,840	7,175	1,350	7 200	15,725	4,968	74
15. 32,136	5,450	2,920	3,275	11,645	3,150	98
16. 22,502	2,535	1,250	1,915	5,700	2,500	111
17. 18,888	3,314	2,208	1,166	6,688	2,300	122
18. 17,605	3,551	1,523	4,770	9,844	3,200	182
19. 52,443	12,596	1,050	9,250	22,896	6,300	120
20. 50,871	2,145	3,855	3,500	9,500	4,500	89
21. 58,500	6,960	10,040	7,000	24,000	6,920	118

The demand factor was 0.29 and the load factor 0.62. Demand in watts per man is 118.

Mr. Potts' figure of 7,726 kw demand for a 35,000-man training base is equivalent to 220 watts per man.

In Table II of this discussion are given December 1942 figures for 21 military establishments where electricity is used extensively for cooking or heating. The demand in watts per man varies from 48 to 182 with an average of 101 watts per man for the 21 locations. These cooking loads frequently occur at the time of maximum demand. Whether they occur off peak or on peak, they represent kilowatt-hours which are now needed in the Northwest for aluminum production. They also represent consumption of fuel or water. Fuel, especially oil, is rapidly becoming more critical for reasons well-known to all of you.

REFERENCE

1. SYNTHETIC OR EQUIVALENT LOAD CURVES, R. F. Hamilton. AIEE TRANSACTIONS, volume 61, 1942, pages 369–81.

Howard P. Seelye (The Detroit Edison Company, Detroit, Mich.): Mr. Potts' paper is a very interesting presentation of the analysis of load data obtained from actual tests on a composite load and of the prediction of loads for other similar situations by use of such basic data applied to their special conditions. Any new type of load must usually go through a trial-and-error period until sufficient experience has been gained with it to serve as a basis for more accurate estimates. The Army cantonment is a new type of load, since similar conditions are not found in ordinary civilian life, and no doubt Mr. Potts data will prove very valuable in future projects.

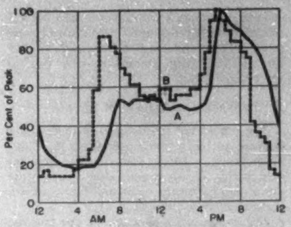

Figure 1. Comparison of daily load curves—lighting

A —— Civilian residential load (seven circuits in Detroit)

B - - - - - Army barracks lighting and kitchen power (Potts)

It may be interesting to make a few comparisons between the load conditions which Mr. Potts has found in Army barracks and those found on our civilian systems. Figure 1 of this discussion shows a characteristic daily curve for lighting load. *A* was taken from the total load of seven distribution circuits in Detroit in early December 1941, serving areas which were 94 per cent residential. *B* is Mr. Potts' characteristic curve for barracks lighting and kitchen power from the test of November 1941, as shown in his article in *Electric Light and Power* for December 1942, to which he refers in his paper. Both are on the basis of percentage of peak load for direct comparison of shape. It is evident that these are quite similar in the latter half of the day, except that the soldiers apparently go to bed rather suddenly at 9:00 p.m. The morning half has an interesting peak at 6:00 o'clock when reveille gets the soldiers out of bed, whereas civilians do their rising much more gradually.

Figure 2 shows a similar comparison for cooking load. *A* is a characteristic average curve for residential electric ranges in a metropolitan area. *B* is Mr. Potts' curve for Army barracks' cooking. There is no similarity here. The peaks on the civilian load coincide with the valleys in the Army curve. The civilian program has no such ambitious schedule as the Army's 4:00 a.m. heavy cooking period, but rather has the big meal at 6:00 o'clock at night when the Army cooks have pretty well quit for the day.

The difference in characteristics is of great importance when the cooking load is combined with the lighting load of Figure 1. Mr. Potts' combined load curve shows a most opportune co-operation between these two loads, one having its valleys at the time the other has peaks and vice versa, to produce a high load factor and three almost equal peaks at 4–6 a.m., 9 a.m., and 3 p.m. Civilian load is not nearly so accommodating. The *A* lighting curve on Figure 1 has a high peak at exactly the same time as the *A* cooking curve on Figure 2. Some of this peak is, no doubt, due to the fact that this load includes a seven per cent range saturation among these customers and some commercial load, but that would account for no more than 10 or 15 per cent of the 6:00 o'clock load. The remaining load, although having its actual peak an hour or so later, is still high at 6:00 o'clock. It is evident that the combination of cooking and lighting load in civilian residences is much less

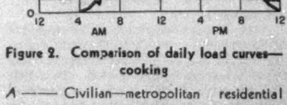

Figure 2. Comparison of daily load curves—cooking

A —— Civilian—metropolitan residential customers

B - - - - - Army barracks cooking (Potts)

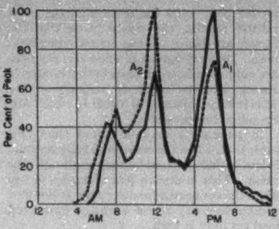

Figure 3. Characteristic daily load curves—cooking

A_1 —— Civilian—small-town residential customers

A_2 - - - - Civilian—farm customers

productive of efficient use of distribution facilities than Mr. Potts found in the Army.

Figure 3 shows some similar curves for cooking load in small-town residences, A_1, and on farms, A_2. These also have a 6:00 o'clock peak, although the farm load has a higher one at noon—they still eat "dinner" at noon in the country.

A few similar comparisons of the demand and energy consumption per man may be of interest. Mr. Potts shows an average demand for lighting and kitchen power of 0.062 kw per man from his test. His estimates for the other projects are similar, although varying somewhat. The seven Detroit circuits had 16,000 customers and a population of about 51,300, or 3.4 per residential customer. The total peak demand was 10,152 kw. If this is discounted for commercial and range load, the figure comparable to Mr. Potts' 0.062 per man is about 0.15 kw per person, or a little over twice as much. This proportion is also roughly borne out by some figures on other areas and other cities. The ratio is not surprising, in view of the greater population density in the cantonment.

For energy consumption, however, Mr. Potts' figures show an average of 287 kilowatt-hours per man per year from his test data and from 240 to 365 for some of the other projects. For the seven residential areas which have been used for illustration, the annual energy consumption per person, exclusive of cooking, was about 400 kilowatt-hours. The average consumption for residential customers on the whole Detroit Edison company system is approximately 1,000 kilowatt-hours per year, with an average of 4.4 persons per customer. This would indicate an average consumption of 225. Of course, consumption varies a good deal among different classes of residences. In general, we might judge from these figures, however, that people use something like the same amount of electricity, whether they are in the Army or at home, only they distribute the use a little differently throughout the day.

For cooking load, some Detroit tests showed a consumption ranging from 276 kilowatt-hours per year per person in families of three persons to 206 kilowatt-hours per year per person for families of five persons. Mr. Potts gives 207 kilowatt-hours per man for Army cooking in the tests, with somewhat more when applied to the larger projects. It is not surprising that the energy to cook a meal is about the same wherever you are. The kilowatt demand per person was again different, however, as it was for lighting. The civilian tests showed a range from 0.120 kw per person in small towns and 0.157 on farms, to 0.210 for metropolitan customers. Mr. Potts' test figure for soldiers is 0.063 kw per man, and his estimates for the 35,000-man project is 0.09. These are of the order of one-half the civilian demands.

These comparisons are not offered as being of particular significance or value in the estimating of cantonment loads. They do indicate the similarity of all such problems of load estimating and show how the habits of communities and other local conditions cause considerable variation in the actual figures and in the shape of the load curves in different situations. Preliminary estimates are, at best, only indicative of what may be expected. Subsequent load tests of actual operating loads on distribution transformers or services will show how accurate the estimates have been and how efficiently the system is being loaded.

Distribution System

The problem of providing service to a load such as the cantonments with which Mr. Potts is working is very similar to that of serving a small city of similar size. A cantonment with 35,000 men is equivalent in population to a fairly good-sized city. For example, the city of Port Huron, Mich., has about that population. The density of population is, of course, much greater in a cantonment than it is in a city; hence the area would be smaller and the peak load also less, because of the smaller unit demand per inhabitant. Port Huron, for example, covers some 7.5 square miles and has a total distribution circuit load of about 9,000 kw (which includes commercial and some small power). This doesn't include industrial power. The cantonment of 35,000 had a total estimated load of about 6,400 for the average day, and 7,700 for the maximum. Another city in our territory, Mount Clemens, has a population of 15,000, an area of 3.5 square miles, and a distribution circuit load of 4,500.

In supplying either the cantonment or the city there arises such questions as the most advantageous bulk supply by transmission lines; the proper number, size, and location of substations; the character of substation construction; provision of reserve supply in case of outages and voltage regulation. The best solutions to these questions are all governed to a large extent by local conditions, the type of supply available, prevailing practices on the supplying system, the geography of location, particular requirements of the load, and so forth. The cantonment differs from the city in being constructed under emergency conditions and being of a more or less temporary nature. These considerations would, no doubt, affect the quality of service thought necessary and the type of construction and materials used.

The problem of distribution from substation to loads is also similar. The same questions must be met; distribution voltage; number of circuits and the load on each, which has a bearing on whether circuit breakers are necessary or fuses will be sufficient; the type and arrangement of circuits, whether radial, loop, or network; distribution-transformer size and location; size and arrangement of secondary circuits; details and materials of line construction, and so forth. The cantonment problem differs from the civilian city in that the whole job is new and the individual loads are more concentrated, more definitely located, of less variety, and, in general, not so subject to uncertain and gradual future growth as they are in the ordinary city. This should simplify the problem considerably, once the characteristics of the load are known, and should allow the application of the best modern ideas for effecting efficiency and economy, such as, for example, use of the higher distribution voltages, high-strength wire and long spans, Edison Electric Institute and National Electrical Manufacturers Association standard distribution transformers, loaded to full capacity as determined by the characteristics of the load and the thermal capability of the transformer. In the civilian city, the distribution engineer is nearly always hampered to some extent by being obliged to work with an established system having all varieties of old construction and practices which cannot be changed immediately to conform with improved ideas. An entirely new job avoids at least some of these restrictions.

As was mentioned in connection with the bulk supply, the answers to these questions on distribution are necessarily affected strongly by local conditions, and also by the emergency and temporary nature of the project. These latter would indicate the desirability of using as simple and economical designs as are adequate, emphasizing economy and avoidance of critical materials rather than permanency and high reliability. Mr. Potts has indicated something of the varied nature of the loads at different cantonments. No doubt the method of supplying them must be equally varied in detail and perhaps in general character. It would be most interesting to have someone who has been intimately connected with the layout and construction of these projects tell how the problems have been studied and something of the details of their design.

Wade M. Edmunds (Office of War Utilities, Washington, D. C.): In general, estimating electric loads is fundamental to determine loads accurately so that no materials will be wasted in serving that load.

It has been traditional to estimate load and then provide generator, transformer, and conductor capacity to serve that load plus 50 or 100 per cent. In normal times, that procedure has proved itself to be good practice and has avoided the increasing of capacity. In many cases, load has outstripped capacity with resultant low-voltage troubles, calling for replacement of conductor, installation of regulators, and replacement of transformers for larger ones.

But we are not now in *normal* times—we are in a war up to our necks. Too few utility engineers realize that, as evidenced by designs of which I see a hundred or so a week in project applications in the War Production Board. Engineers still plan an extension to serve a war load with the future normal growth of the area in mind.

Unless the results of accurate load estimates are applied to design, then we are not doing a wartime job—if an extension is designed for more than the present known load, materials are wasted which means that copper, steel, and iron are withheld from shells, guns, planes, and ships. We are fast coming to an accounting on that, and we must not wait until the supply of materials for civilian use is exhausted, to fit design closely to loads. The work done at this conference on estimating loads will not have its full effect in the prosecution of the war if its relationship to the use of materials is lost sight of.

As an example of what I want to emphasize, I would like to mention a specific case. A project application came in to serve an important war load of 3,500 kw. The load had been accurately determined and there was no question on that. To serve it the company proposed to install a transformer bank of 9,000-kva capacity, build a line from that substation to a switching station in a direction opposite to that of the new load, connect to an existing line, and extend a new line from the end of that one to the new load. The layout was difficult for us to understand until the applicant sent his engineers to discuss it with us. We found that the routing of the lines was chosen, because the company wanted to serve other parts of the system which would need strengthening after the war. It is too soon to do postwar planning. We must win this war first to determine whether this country will do its own planning—or some one else will.

Conservation Possibilities

Possibilities of saving materials are unlimited if engineers are allowed to exercise their ingenuities, but in the past 20 years it appears that many have become slaves to standard specifications prepared for them. In reviewing a great many project applications every day, I stop occasionally to wonder if I should write a book for which so far, I have only the title, "Sad Commentaries on the Modern Engineer."

I doubt if it is the fault of the individual engineer, but his training, from the time he leaves school, in large companies particularly is very specialized and channeled. Unless he is an unusual man, he will follow along and do what he is told, not giving much thought to alternate ways of accomplishing the same result.

I have noticed that engineers who have worked for smaller utilities where economy of investment is essential and engineers who have worked for utilities in foreign countries where materials and equipment at hand have to be used are the most versatile and ingenious. In the two instances mentioned, deviations from standards of construction are made with little or no loss to the finished job.

Take for example a very modern installation in a large generating plant of a bank of metal-clad oil circuit breakers and metal-clad busses—all in duplicate of each piece of equipment. A new circuit was to be added, and the applicant insisted very vehemently that the same metal-clad dual equipment be installed to match up with the previous installation. Many arguments were presented by long-distance telephone and telegraph, and all of them had little to do with adequate service or recognition of wartime scarcity of material. Profit had not been supplanted by patriotism. We came to an agreement, however—an existing set of oil circuit breakers was divided so that the new circuit was connected to one of them.

Valves for Steam Plant. The applicant insisted on use of nonflanged valves. We found *flanged* valves in excess stock, and only after much argument would the applicant accept these, although the valves were suitable in all other respects.

I have commented on one or two examples of what could have been done, but was not, until we pointed out other ways. That does not mean that many engineers are not doing splendid work in substituting materials and methods not ordinarily used, such as direct-pole mounting of transformers, rehabilitation of oil-circuit-breaker contacts, type-*A* meters in lieu of socket-type, salvaged crossarms, and so forth. A lot of them have done a splendid job in saving new and critical materials.

Harold M. Potts: In times such as these it is one of our duties as engineers to make the most economical use of the materials we have. Overbuilding in peacetime may not be serious, since it can often be justified as building for future growth. With the shortage of many materials, it now becomes most wasteful to provide, for instance, a 1,000-kva substation to serve a military base where one of 500 kva would be ample. It is hoped the information and method of analysis that was presented in the paper will prove of value to engineers in estimating accurately the loads for military bases, so that more economical building may result.

In estimating military-base loads, demand factors have been found to give the most reliable results, the reason being that the factors are based on the actual equipment used. Derived figures of kilowatts per man are quite erratic when used indiscriminately among various types of military bases. This is very apparent in Table II which Mr. Hamilton has presented.

In general, it may be noted from this table that, when the connected load per man is low, higher demand factors result and vice versa. This is due to less diversity of use when the connected load per man is low and greater diversity of use when the connected load per man is high.

The table reveals wide divergence in the connected loads per man for the various classes of use. The connected lighting loads vary between 40 and 240 watts per man, the heating and cooking loads from 20 to 140 watts per man, and the connected power loads from 30 to 350 watts per man.

Inspection of the connected lighting loads, items 1, 2, 3, 5, and 19 in this table, reveals the possibility of more not fully utilized equipment than necessary at the military bases. The bases noted as items 4, 8, 9, 10, 11, 14, 16, 20, and 21 have the lowest connected lighting load per man and are an indication that an economical amount of lighting equipment is provided at these particular bases. These bases are all fairly large ones, and all of them have only minor amounts of electric cooking equipment. The demand factors for these bases are in general 30 to 40 per cent, which coincides generally with estimated demand factors for the bases presented in the AIEE paper.

Mr. Hamilton comments that a demand factor of 29 per cent, for the Northwest fort at which test data referred to in the paper were taken, seems to bear out the annual demand factor estimated in the paper for the 35,000-man Army base, after consideration is given to the relative amounts of electric cooking equipment. The 35,000-man Army base had a planned connected cooking load of about one-third kilowatt per man, whereas the 58,500-man fort, at which the test data were made, has a connected cooking load of only 10,000 kw, or approximately one-sixth kilowatt per man. In this fort only part of the masses have electricity for cooking; the majority of them use either gas or coal for fuel.

The comments of Mr. Hamilton, Mr. Seelye, and Mr. Edmunds were both welcome and constructive, and I wish to express my appreciation for their interest and co-operation.

Overvoltage Protection of Current-Transformer Secondary Windings and Associated Circuits

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Synopsis: It has long been recognized that excessive potentials may be developed in current-transformer secondary windings under unusual conditions, such as open circuits. Recent experience discloses that dangerous overvoltages (several thousand volts) may be produced as a result of normal switching operations on circuits containing lumped capacitance.

A simple procedure for circuit analysis and evaluation of approximate voltage magnitude for the switching transient case is reported. For easy reference, there are included tables of calculated secondary voltage magnitudes covering a broad range of application.

Under certain conditions, overvoltage protection is desirable and important. Aside from the potential hazard to life, current-transformer circuit insulation may be damaged, yet not be evident immediately. Performance at normal rated current may not be noticeably impaired, yet serious failure may occur in the presence of fault-current flow, thus nullifying the action of current-actuated protective relays.

The characteristics of a new overvoltage protector expressly designed for current-transformer protection is presented. With this device current-transformer secondary voltages are limited to moderate values. The protector is small and compact, and easily applied to existing as well as new current-transformer installations. The characteristics are permanent, not affected by repetitive operation, and result in negligible ratio error in the normal operating current range.

Sources of Excessive Voltage

EXCESSIVE potentials in current-transformer secondary circuits may appear as a result of:

1. Open-circuited secondary winding.
2. Switching transients in the presence of lumped capacitance in the power circuits.
3. Fault current flow.
4. Lightning impulse currents.
5. Steep front current transients of other origin.

Open-Circuited Secondary Winding

The circulation of rated current in the primary winding of a current transformer with its secondary winding open-circuited will give rise to a persistent excessive voltage, which may be high enough to be dangerous to apparatus and circuit insulation, as well as to any person who may come in contact with the circuit.

In the absence of secondary current flow, all of the current-transformer primary current is exciting current. Although only a small portion of normal rated primary current is sufficient to saturate the core, there is a short interval each half cycle as the current passes through zero that the magnetic flux is very rapidly whisked from saturation value in one direction to saturation value in the other direction. It is the exceedingly rapid rate of change of flux during this short interval which is responsible for the high open-circuit voltage. The secondary open-circuit voltage magnitude is a function of current-transformer magnetic design, is increased by higher operating frequency, is increased by reduced secondary current rating, but is limited to a maximum value of 1.41 (0.866) (E_L) (R). Potentials in excess of 2,000 volts, with rated primary current flow, may be frequently expected. Figure 2 typifies the resulting peaked voltage wave.

Switching Transients in the Presence of Lumped Capacitance in the Power Circuit

Excessive overvoltages in current-transformer secondary circuits have been observed in connection with the switching of power circuits with which are associated lumped capacitance connected either line to line or line to neutral. A description of the manner in which such voltages are generated, together with a simple procedure for evaluating the probable voltage magnitude, will be of interest.

A de-energized capacitor when switched on to an energized power circuit initially assumes the appearance of a short circuit since the capacitor terminal potential is incapable of being changed instantaneously by any finite amount. It follows that, at the instant the switch is closed, the entire system voltage must be absorbed in the circuit impedance between the capacitor and the source of supply. Current-transformer circuits constitute a part of this interconnecting impedance.

The transition between the initial short-circuit behavior and the normal steady-state normal frequency performance takes the form of a high-frequency oscillation which is rapidly damped by circuit resistance. The physical size of the lumped capacitance has relatively little influence on the transient-voltage magnitude unless small enough to become comparable with distributed capacitance in the interconnecting circuit impedance but does influence directly the frequency of the transient oscillation.

A simple procedure for circuit analysis for evaluating expected current-transformer secondary voltages is contained in Appendix A. It will be noted that the high transient frequency which invariably accompanies the production of excessive current-transformer voltages permits circuit resistance to be ignored. It will be

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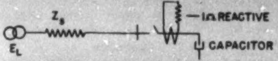
observed that the current-transformer secondary voltage is influenced prominently by power-circuit operating voltage, power-circuit inductive reactance, current-transformer secondary reactive burden, and current-transformer ratio. It is of particular interest to note that the presence of energized lumped capacitance electrically close to the particular capacitor circuit being switched will usually greatly accentuate the resulting current-transformer secondary voltage.

For quick reference, a tabulation of calculated current-transformer secondary potentials covering a fairly broad range of application conditions has been included in Appendix B. Voltage values to be expected when switching a single lumped capacitance electrically remote from energized lumped capacitance appear in Table I and Figure 8. Calculated voltage magnitudes for the case in which the electrical system contains nearby energized lumped capacitance appear in Table II and Figure 9. It is the purpose of the figure in both instances to portray graphically the relative effect of variation in the predominant influencing factors.

Table I. Calculated Current-Transformer Secondary Voltage e_S With One-Ohm Reactive Burden

Case 1. Single Circuit Containing Lumped Capacitance*



Current-Transformer Primary Amperes	System Short-Circuit Kilovolt-Amperes	Primary Operating Potential (E_L) Volts			
		13,800	6,900	4,160	2,400
1,200	500,000	87	175		
600		174	348		
300		347	689		
150		684	1,320		
50		1,820	2,750		
1,200	250,000	44	88	145	
600		87	174	289	
300		174	347	574	
150		346	680	1,115	
50		974	1,665	2,360	
1,200	150,000		52	87	150
600			105	173	298
300			208	345	590
150			411	680	1,123
50			1,000	1,710	2,180
1,200	75,000		26	43	75
600			52	86	149
300			105	173	297
150			208	341	581
50			583	938	1,360
1,200	50,000				51
600					100
300					199
150					392
50					990
Current-transformer voltage class		15,000	7,500	5,000	5,000

*These voltages will be produced under short-circuit conditions.

Fault-Current Flow

In the presence of primary-circuit fault-current flow, a corresponding current will flow in the current-transformer secondary winding, which in turn will produce a high current-transformer secondary voltage. Since in the previous case it is reasoned that a lumped capacitance initially appears as a system short circuit, it follows that the same reasoning may be used in judging probable voltage magnitudes in the case of fault-current flow. In general, however, the system impedance will be so large in proportion to that of the current transformer that the voltages produced are not excessive for the insulation of the secondary or the devices connected to it.

It is, however, possible that the presence of energized lumped capacitance may accentuate the resulting transient current-transformer secondary voltage, in the same manner as outlined under the topic of switching transients. It is probable that such accentuated over voltages would be realized only if the character of the electrical fault were such that the transition between a good insulator and a very good conductor was extremely abrupt, an example of which would be the closure of a bolted fault circuit by means of a power-circuit switch.

Test Verification

To establish the fact that excessive overvoltages could be generated in current-transformer secondary circuits in the manner described in this paper a test setup was made using facilities which could be made available for this purpose. The test circuit arrangement is defined in Figure 4. Closure of the circuit breaker in this test setup produced current-transformer secondary potentials, measured by a calibrated sphere gap, of 9,750 volts, with a reactive burden of 2.96-ohms and 5,150 volts with a reactive burden of 1.74 ohms.

The circuit configuration unfortunately does not simulate closely that encountered in practical circuit operation and is therefore not suited for checking quantitative agreements with theoretical analysis. The tests do serve to establish the following points:

1. Excessive current-transformer voltages may be generated in the manner described in this paper.

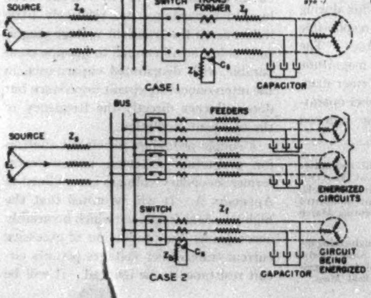


Figure 1 (left). Fundamental circuit arrangements which give rise to high switching transient voltages in current-transformer secondary circuits

Case 1. Single circuit containing lumped capacitance

Case 2. Multiple circuits containing lumped capacitance

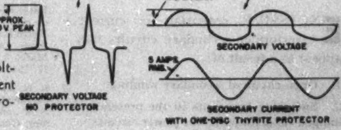


Figure 2 (right). Voltage oscillograms of current-transformer secondary voltage with burden disconnected

Case 1. Secondary voltage, no protector

Case 2. Secondary voltage and secondary current with one disk thyrite protector

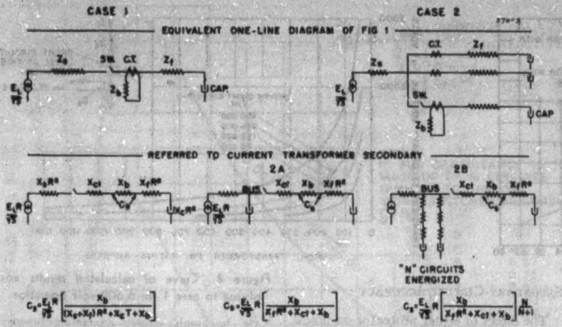

CASE 1
CASE 2
EQUIVALENT ONE-LINE DIAGRAM OF FIG 1
SW.
C.T.
CAP.
REFERRED TO CURRENT TRANSFORMER SECONDARY
2A
2B
BUS
"N" CIRCUITS ENERGIZED
Cs = EL/√3 R [Xb / ((Xs+Xf)R² + XcT + Xb)]
Cs = EL/√3 R [Xb / (XfR² + Xct + Xb)]
Cs = EL/√3 R [Xb / (XfR² + Xct + Xb)] N/(N+1)

Figure 3. Equivalent-circuit diagram corresponding to Figure 1

2. The generation of such excessive voltages is not contingent on the presence of a high capacity power supply system.

3. Variation in the physical size of capacitor units does not significantly change the magnitude of secondary voltage generated.

Need for Overvoltage Protection

Unless restricted in a suitable manner, in applications involving lumped capacitance, excessive voltages may appear in current-transformer secondary circuits which are dangerous to current-transformer and secondary-circuit insulation and to operating personnel. Secondary-circuit insulation failure assumes particular importance in the case of circuits containing current-actuated protective relays which may be rendered inoperative. Secondary-circuit insulation which has been punctured by excessive transient voltage may not be evident at normal rated current levels but may fail completely when subjected to increased voltages attendant with fault current flow. There is, therefore, a distinct need for an effective reliable overvoltage protector for current transformer circuits.

Table A

Burden	Per Cent Normal Current	Test Without Protector		Test With Protector (One Disk)	
		Ratio	Phase Angle	Ratio	Phase Angle
Z (50 volt-amperes at 50 per cent power factor)	10	1.0045	+10	1.0046	+10
	20	1.0025	+ 5	1.0027	+ 5
	40	1.0005	− 2	1.0006	− 3
	60	1.0000	− 4	1.0002	− 5
	100	1.0017	+ 2	1.0019	+ 1
	150	1.0033	+ 5	1.0036	+ 4
	200	1.0043	+ 7	1.0048	+ 5

Protective Equipment

Many overvoltage protective devices which have been applied to current transformer secondary circuits, such as film cutouts, vacuum-tube devices, restricted core designs, and secondary filters, leave much to be desired with respect to initial performance, reliability, simplicity, and application flexibility. In view of the fact that unprotected current-transformer circuits commonly may be subject to recurrent transient overvoltages, it is evident that an adequate protective device must be capable of repetitive operation without change in characteristics, small, compact, and easy to apply, and at the same time be low in first cost.

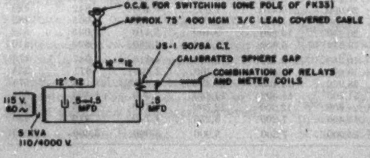

O.C.B. FOR SWITCHING (ONE POLE OF FK33)
APPROX. 75' 400 MCM 3/C LEAD COVERED CABLE
JS-1 50/5A C.T.
CALIBRATED SPHERE GAP
COMBINATION OF RELAYS AND METER COILS
12"-12
12"-12
115 V. 60~
.5—1.5 MFD
.5 MFD
5 KVA 110/4000 V.

Figure 4. Diagram of test circuit used to verify production of high switching-transient voltages

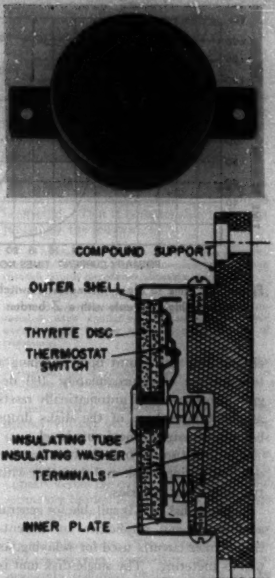

COMPOUND SUPPORT
OUTER SHELL
THYRITE DISC
THERMOSTAT SWITCH
INSULATING TUBE
INSULATING WASHER
TERMINALS
INNER PLATE

Figure 5. Illustration of Thyrite secondary protector

New Overvoltage Protector

A secondary protector has been developed which meets these requirements. The complete unit is illustrated in Figure 5. The active element embodies one or two disks made of material having nonlinear resistance characteristics and of such character as to secure adequate control of overvoltages, yet avoid objectionable error in secondary currents throughout the operating overcurrent range.

To avoid excessive temperature of the disks in applications which may be subject to secondary open circuit, an auxiliary thermostatic switch is used which short-

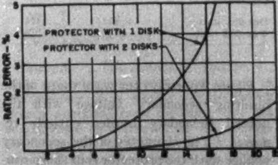


Figure 6. Ratio error caused by Thyrite protector at high currents

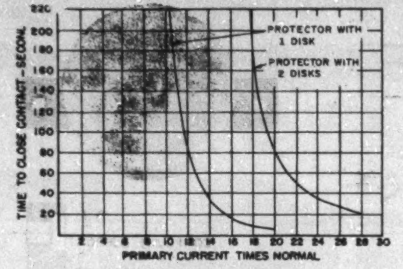


Figure 7. Time to close thermostatic switch contacts at high currents with a Z burden

circuits the active unit upon reaching a temperature of approximately 100 degrees centigrade and automatically resets as the temperature of the disks drops below the assigned maximum level.

The protector is built in two forms—one with a single disk and the other with two such disks in series.

The two-disk unit is suitable for general application and applicable to current-transformer circuits used for relaying, as well as metering. The single-disk unit is designed for the very minimum secondary overvoltage and accepts a sacrifice in accuracy at high currents. It is intended to be used on current-transformer circuits for metering only. The single-disk unit is particularly adapted to operating conditions in which operating personnel frequently contact portions of the current-transformer secondary circuit and current-transformer circuits may easily be inadvertently open-circuited.

Pertinent performance characteristics of the secondary protector are as follows:

OVERVOLTAGE

With rated current flowing in the current-transformer primary winding and the secondary burden disconnected, the protector will limit the secondary voltage to the following:

	Rms Volts	Peak Volts
One-disk unit	135	175
Two-disk unit	270	350

Figure 2 portrays the wave shape of the resulting secondary voltage with the normal secondary burden entirely removed. The character of the secondary voltage with the protector is in sharp contrast with that which would be obtained without protector.

SECONDARY-CIRCUIT ACCURACY

The effect of either protector on phase angle and ratio accuracy with a Z burden (50 volt-amperes at 0.5 power factor) is negligible under normal operating conditions between 5 per cent and 200 per cent of rated load current as evidenced by the test information in Table A.

At high currents, the ratio accuracy of the current transformer with the protector connected across its secondary terminals and with a Z burden is shown in Figure 6. Substantial ratio error occurs with currents of 15 times normal and higher with the single-disk protector, which may be objectionable for relaying service.

THERMAL-SWITCH ACTION

The thermal switch is needed only for the purpose of limiting the temperature to a safe value in the presence of an open secondary circuit. Under normal operation, including the temporary flow of fault current, excessive temperature will not be realized and a thermal protective switch if present would not operate. Figure 7 defines the time required for the thermal switch contacts to close as a function of current magnitude, again based on the presence of a Z secondary burden.

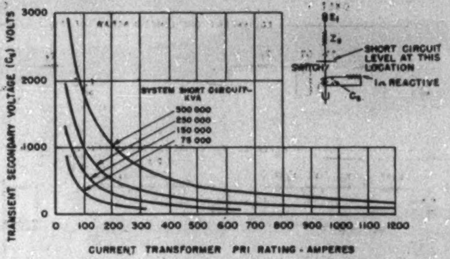


Figure 8. Curve of calculated results applying to case 1 for 6,600-volt operation

Case 1. Single circuit containing lumped capacitance

E_1—6,600-volt three-phase 60 cycles

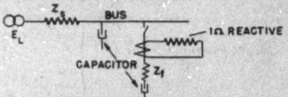
Current transformer—Instrument-type, five-ampere secondary winding

X_b—One ohm

The recycling characteristic of the protector with thermal switch in the presence

Table II. Calculated Current-Transformer Secondary Voltage e_s With One-Ohm Reactive Burden

Case 2. Multiple Circuits Containing Lumped Capacitance



Z_f	Current-Transformer Primary Amperes	Primary Operating Potential—Volts					
		13,200	6,600	4,000	2,300	400	220
0.03 ohm per phase Approximately 500 feet Three-conductor cable	1,200	1,053	527	319	183	35	18
	600	2,090	1,045	640	368	70	35
	300	4,040	2,020	1,250	721	138	69
	200	5,770	2,890	1,840	1,060	203	101
	100	8,940	4,460	3,170	1,830	350	233
	50	9,530	4,770	4,200	2,420	462	232
0.012 ohm per phase Approximately 200 feet Three-conductor cable	1,200	2,620	1,310	795	458	88	44
	600	5,130	2,560	1,575	905	173	87
	300	9,460	4,730	3,030	1,740	334	167
	200	12,600	6,300	4,270	2,450	470	234
	100	15,500	7,750	6,310	3,620	695	346
	50	12,300	6,150	6,230	3,590	690	345
0.006 ohm per phase Approximately 100 feet Three-conductor cable	1,200	5,180	2,590	1,585	912	175	88
	600	9,950	4,980	3,100	1,785	342	171
	300	17,200	8,590	5,750	3,300	632	317
	200	20,900	10,470	7,650	4,400	842	420
	150	22,000	11,020	8,770	5,050	965	482
	100	20,550	10,290	9,400	5,410	1,035	518
	50	13,650	6,810	7,460	4,280	822	410
0.003 ohm per phase Approximately 50 feet Three-conductor cable	1,200	10,250	5,130	3,150	1,810	347	173
	600	19,080	9,550	6,100	3,500	671	335
	300	28,900	14,450	10,400	5,970	1,140	574
	200	31,200	15,600	12,650	7,300	1,400	698
	100	24,600	12,300	12,500	7,180	1,375	686
	50	14,400	7,200	8,250	4,740	910	455
Current-transformer voltage class		15,000	7,500	5,000	5,000	5,000	5,000

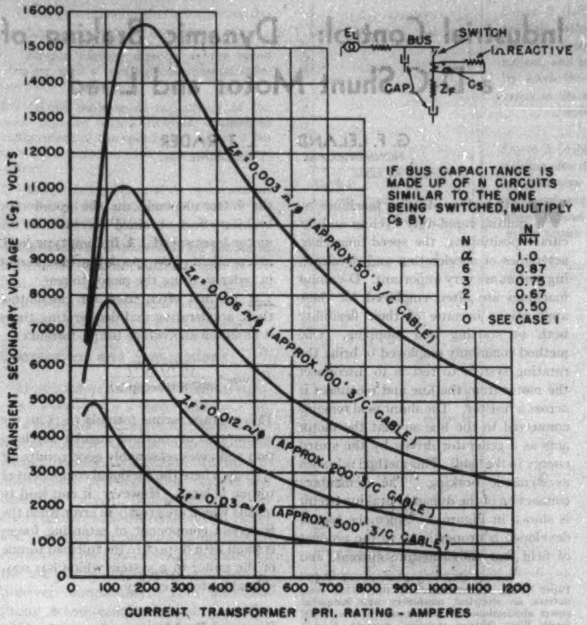


Figure 9. Curve of calculated results applying to case 2 for 6,600-volt operation

E_L—6,600-volt three-phase 60 cycles
Current transformer—Instrument-type, five-ampere secondary winding
X_B—One ohm

of a permanent open-circuited secondary winding and normal rated current flowing in the primary winding is:

	Time to Close Contacts	Time to Reset
One-disk unit	10 seconds	8 minutes
Two-disk unit	14 seconds	11 minutes

OUTSTANDING ADVANTAGES

The new secondary protector constitutes a simple, reliable, inexpensive, overvoltage device for general application. It is small, compact, and suitable for direct mounting on the current-transformer structure. The outstanding features are:

1. Permanence of characteristics—not influenced by repetitive operation.

2. Effective voltage protection provided irrespective of the voltage source or the possible oscillation frequencies involved.

3. No operating time lag.

4. Negligible influence on accuracy within the normal range of operating current and burden.

5. Simple, compact, and applicable to existing as well as new current-transformer installations.

6. Active element completely enclosed—mechanical protection provided and dust and other foreign materials excluded.

Appendix A. Current-Transformer Switching-Transient Overvoltage Circuit Analysis

The fundamental circuit arrangements illustrated in Figure 1 are first reduced to an equivalent line to neutral one-line diagram as shown on the upper part of Figure 3.

Refer to Current-Transformer Secondary Winding

A better physical conception of circuit behavior will be gained through analysis in terms of the current-transformer secondary winding. In referring primary circuit quantities to the secondary, voltages are multiplied by R and impedances by R^2. Current-transformer internal impedance is generally available expressed in terms of the secondary winding.

The effective primary circuit voltage is taken as $E_L/\sqrt{3}$ which would correspond to simultaneous closing of the three poles of the switching unit. It might be reasoned that the transient would be controlled by the first two poles to close, in which case the effective primary circuit voltage would be $E_L/2$. As there is no significant difference between the two ($0.577E_L$ and $0.5E_L$), only one (the more pessimistic) will be used.

The resulting equivalent circuits referred to the secondary winding appear in the lower portion of Figure 3.

Evaluation of Secondary Voltage

By neglecting circuit resistance, a tremendous reduction in complexity of analysis is effected. That it is reasonable to do so is judged by the following:

1. At fundamental frequency, resistance generally plays only a minor part in influencing voltage drop through circuit impedance elements.

2. Invariably switching transients which will give rise to dangerous secondary voltages will be associated with oscillating frequencies far above normal which further reduce the significance of resistance.

3. A high degree of accuracy in secondary-voltage evaluation is of slight import. Simplicity in analysis is far more important.

Immediately following circuit closure, system voltage becomes distributed along the inductive reactance between the voltage source and the de-energized capacitor switched on the line. That portion which appears across the current-transformer total secondary burden X_B represents the voltage which appears at the secondary terminals.

Case 1. A single circuit containing lumped capacitance. The expression for current-transformer secondary voltage immediately following switch closure can be directly written.

$$e_S=\frac{E_L}{\sqrt{3}}R\left[\frac{X_B}{(X_s+X_f)R^2+X_{CT}+X_B}\right]$$

Case 2. Multiple circuits containing lumped capacitance. The treatment of this case will be treated in two steps, *A* and *B*. Case 2*A* assumes the presence of lumped capacitance directly at the distribution bus, while case 2*B* considers a number of similar feeder circuits each containing lumped capacitance, only one of which is de-energized and to be switched on the line.

It will be apparent that an energized capacitor will resist a change in its terminal voltage in the same manner as a de-energized unit. The initial transient distribution of system potential will thus occur across circuit impedance elements intervening between the energized and the de-energized capacitance blocks.

Case 2A. With lumped capacitance at the distribution bus, the supply system impedance Z_S is effectively shunted out of the transient oscillating circuit. The expression for current-transformer secondary voltage becomes

$$e_S=\frac{E_L}{\sqrt{3}}R\left[\frac{X_B}{X_fR^2+X_{CT}+X_B}\right]$$

Case 2B. The secondary voltage, e_S, when switching one deenergized circuit in the presence of N similar circuits which already energized from the same bus, can be expressed as a fraction of the voltage which would result in case 2*A*. A certain portion of the total system voltage will appear across inductive reactance elements contained in the energized circuits, which portion will diminish as the number of energized circuits increases. With N energized circuits, the switching of a similar circuit will result in a secondary voltage on the switched circuit of—

$$e_S = \frac{E_L}{\sqrt{3}}R\left[\frac{X_B}{X_f R^2 + X_{CT} + X_B}\right] \times \frac{N}{N+1}$$

$$= e_S \text{ (case 2}A\text{)} \times \frac{N}{N+1}$$

Values for the modifier $\frac{N}{N+1}$ are:

N	$\frac{N}{N+1}$
Infinite	1.0
6	0.87
3	0.75
2	0.67
1	0.5
0	See case 1

Nomenclature

Z_S—Primary supply circuit impedance $r_s + jX_s$ ohms per phase (Y)
Z_f—Primary feeder circuit impedance $r_f + jX_f$ ohms per phase (Y)
Z_c—Primary capacitance block impedance $-jX_c$ ohms per phase (Y)
Z_{CT}—Current-transformer internal impedance $r_{CT} + jX_{CT}$ ohms (referred to secondary)
Z_B—Current-transformer secondary burden impedance $r_B + jX_B$ ohms
E_L—Primary circuit-potential—line-to-line volts
e_S—Current-transformer secondary terminal voltage
R—Current-transformer ratio.

Appendix B. Current-Transformer Switching-Transient Overvoltage Calculated Results

For quick estimating purposes, calculations have been made using the methods outlined in Appendix A, covering a fairly broad range of application conditions both for case A and case B.

All calculated results apply to a five-ampere rated secondary current.

All calculated results are based on a secondary reactive burden of one ohm. For practical purposes, the resulting secondary voltage may be considered directly proportional to X_B.

All results apply expressly to 60-cycle operation. The switching transient is independent of operating frequency, and results here tabulated may be used for other operating frequencies if carefully converted.

Results included under case 1 may be used to judge the secondary voltage magnitude resulting from fault-current flow in a system free of near-by lumped capacitance.

Specific Results

CASE 1

Table I. Tabulated transient secondary voltage for current-transformer primary current ratings ranging from 50 to 1,200 amperes, operating voltages of 13,800, 6,900, 4,160, and 2,400 volts, short-circuit kilovolt-ampere levels from 50,000 to 500,000-kva.

Figure 8. Calculated values for 6,900-volt operation plotted in curve form to illustrate the influence of various factors.

CASE 2

Table II. Tabulated transient secondary voltage of the same character as in Table I.

Figure 9. Values for 6,900-volt operation plotted in curve form to illustrate the influence of various factors.

Typical Circuit Constants

Current-transformer internal series impedance (instrument type):

5,000 volts class 1.+j1.5 ohms (60 cycles) referred to five amperes secondary windings

7,500 volts class 1.3+j4.0 ohms (60 cycles) referred to five amperes secondary windings

15,000 volts class 1.3+j4.7 ohms (60 cycles) referred to five amperes secondary windings

Secondary burden (devices—60 cycles):

Ammeter wattmeter watt-hour meter (*AD*-7 five amperes) 0.05+j0.05 ohm

Power-factor meter (*AD*-7) 0.11+j0.11 ohm

Recording ammeter (*CD*-3 five amperes) 0.12+j0. 5 ohm

IAC relay (4–15 amperes on 4-A tap) 0.14+j0.34 ohm

IBC relay (4–15 amperes on 4-A tap) 0.2+j0.45 ohm

Power-circuit cable reactance—60 cycles:

7,500 volts and less 0.026–0.033 ohm/1,000; number 1/0 and larger

15,000 volts 0.031–0.039 ohm/1,000: 500,000 cular mils—number 1/0

References

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2. ANALYSIS OF FACTORS WHICH INFLUENCE THE APPLICATION OPERATION, AND DESIGN OF SHUNT-CAPACITOR EQUIPMENTS SWITCHED IN LATER BANKS, J. W. Butler. AIEE TRANSACTIONS, volume 59, 1940, pages 795–800.

Industrial Control: Dynamic Braking of a D-C Shunt Motor and Load

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WITH high-production machines requiring rapid-duty cycling and accurate positioning, the speed-time characteristics of accelerating and decelerating motors are very important. D-c shunt machines are often employed for these applications because of their flexibility both in starting and stopping. One method commonly employed to bring the rotating system to rest is to disconnect the motor from the line and reconnect it across a resistor. The shunt field remains connected to the line so that the motor acts as a generator driven by the stored energy in the load. This method is known as dynamic braking. The elementary connection of the dynamic-braking circuit is shown in Figure 1. Since the torque developed is proportional to the product of field flux and armature current, and the latter depends on the speed, the braking effect obtained decreases as the motor loses speed. A friction-type load, independent of the speed, must be present in order to bring the motor to rest.

A method often used for calculating these accelerating and decelerating times is based on an average torque formula.[1]

$$T_{\text{average}} = \frac{(WR)^2\,(N)}{307.8\ (t\ \text{seconds})}$$

This average torque formula rests on the assumption of constant angular acceleration and gives reasonably good results for systems where the frictional component of torque is large. However, it can lead to results which are greatly in error when the frictional component of retarding forces is small with respect to the full load torque of the motor in a system which has considerable stored energy.

Scope of Problem

This paper presents an analysis which leads to an effective and accurate method of calculating the performance of a d-c shunt motor under dynamic braking when connected to various types of load.

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The authors acknowledge the assistance of R. R. Lang in checking the equations and making calculations.

Three general solutions are covered depending on the type of load.

1. Torque varying directly as the speed together with a frictional component independent of the speed.

2. Torque varying as the square of the speed together with a frictional component independent of the speed, such as for a fan or blower operating with constant discharge orifices.

3. Torque independent of the speed such as a crane hoist operating against gravity, or a wire drawing machine.

This analysis is made only for the shunt motor with constant field strength at full field. The more common applications such as the variable speed d-c motors and the series d-c motors are not discussed.

Conventions and Assumptions

Most authors use the well-known equations $V=e+ir$ for a motor and $e=V+ir$ for a generator so that the direction of current is positive for either machine under normal operating conditions. In order to differentiate between motor current and generator current, subscripts must be used together with the relation that motor and generator current flow in opposite directions. This convention, predicated on the use of the above two equations is, however, unnecessary. It is also confusing when considering one machine which alternately acts as a motor or a generator. The difficulty is eliminated by using only the motor equation $V=e+ir$ together with the following conventions. Positive motor current causes positive motor torque. The direction of rotation of the machine when running as a motor is considered as the positive direction, so that positive torque and acceleration will cause an increase in speed. These relationships are shown in Figures 2 and 3.

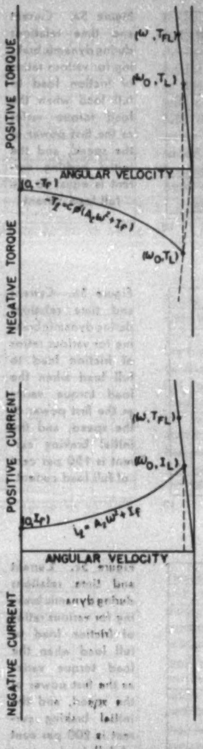

Figure 2 (left). Relations existing before dynamic braking between torque, current, and angular velocity when the load torque varies as the square of the speed

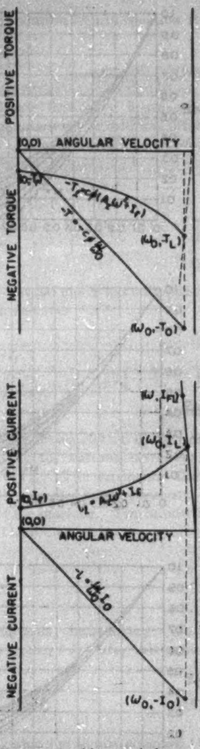

Figure 3 (right). Relations existing after dynamic braking between torque, current, and angular velocity when the load torque varies as the square of the speed

The following assumptions are made:

1. Armature reaction is neglected. This assumption is justifiable since dynamic braking currents are usually limited to about 200 per cent full load current and armature reaction is negligible in this range.

2. Frictional forces are assumed constant throughout the entire speed range.

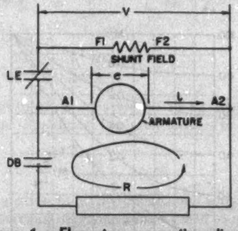

Figure 1. Elementary connection diagram showing dynamic braking circuit

LE—Line contactor
DB—Dynamic braking contactor
R—Total resistance in braking circuit

Results

The curves shown in Figures 4 to 8 give the performance of the shunt motor under dynamic braking for the assumed loads. Both coordinates are dimensionless quantities, so that the curves are applicable to any motor and system. The ordinate is the ratio of the current at any instant as a fraction of the initial braking current. When the value of the braking resistor remains unchanged, this ordinate is also a ratio of the speed at any instant to the initial speed. The abscissa is given as a function of the initial watts in the braking circuit, the total mechanical stored energy in the rotating system, and the time.

Although the curves are given for a constant braking resistance, they may be applied when the resistance is decreased in steps during the braking period, provided only that the speed at which the switching occurs is considered full speed, and the new KE_0 is then used.

The parameter, M, is the constant torque component of the armature current as a function of the full load current. It should be noted that when $M=1$, case b

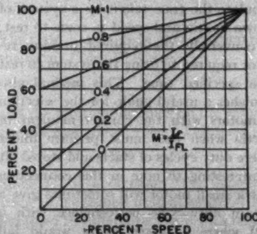

Figure 4. Load torque versus speed for various ratios of friction load to full load when the load torque varies as the first power of the speed

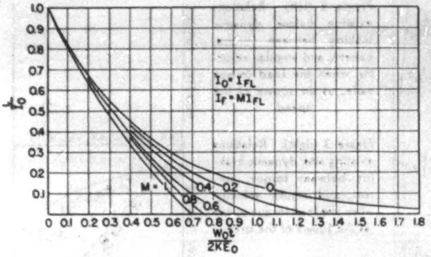

Figure 5a. Current and time relations during dynamic braking for various ratios of friction load to full load when the load torque varies as the first power of the speed, and the initial braking current is equal to the full load current

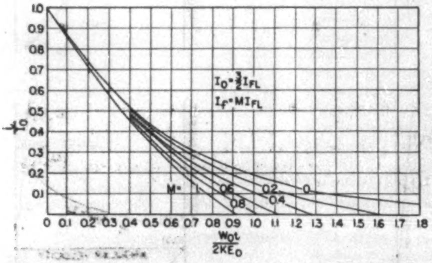

Figure 5b. Current and time relations during dynamic braking for various ratios of friction load to full load when the load torque varies as the first power of the speed, and the initial braking current is 150 per cent of full load current

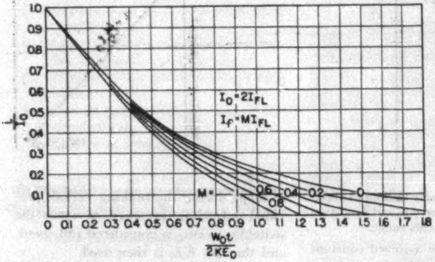

Figure 5c. Current and time relations during dynamic braking for various ratios of friction load to full load when the load torque varies as the first power of the speed, and the initial braking current is 200 per cent of full load current

and case *c* are identical and reduce to case *a*.

Using this method then it is possible to calculate the time taken by a system of known inertia and speed to come to rest; or conversely the required braking resistor required to bring the system to rest in a specified time. This method also furnishes a useful guide for the selection of motors with the correct moment of inertia when they must perform under severe duty cycles of starts and stops. It is interesting to note in this connection that a small motor will sometimes give a certain duty-cycle performance with a load where a larger motor would fail under the same conditions.

The cases treated cover many of the usual loads encountered in industrial work, but the curves can be extrapolated for other speed torque conditions, or the equations can be set up for any conditions and solved using graphical integration if necessary.

The curves presented for case *b* and case *c* are for dynamically braking a fully loaded motor. Similar curves can be drawn for any other load condition by assigning different values to *b* in equations 15 and 17.

Analysis

(For nomenclature see Appendix I.)

The equation for generated voltage is

$$e = \frac{ZP}{m} \frac{\phi n}{(60)10^8} \text{ volts,}^2 \text{ and since } \omega = \frac{2\pi n}{60}$$

$$e = \frac{c\phi\omega}{2\pi 10^8} \text{ volts} \qquad (1)$$

where $c = ZP/m$. The torque equation[3] is

$$T = \frac{ZP}{m} \frac{\phi i}{20\pi} \text{ dyne-centimeters}$$

and since 10^7 dyne-centimeters = 1 watt-second, this becomes

$$T = \frac{ZP}{m} \frac{\phi i}{2\pi 10^8} = \frac{c\phi i}{2\pi 10^8} \text{ watt-seconds} \qquad (2)$$

The load torque, which is negative, causes a positive armature current to flow (Figure 2), and since positive current has been defined as the current that produces a positive torque, then

$$T_L = \frac{-c\phi i_L}{2\pi 10^8} \text{ watt-seconds} \qquad (3)$$

Now,

$$T + T_L = J \frac{d\omega}{dt} = J\alpha \qquad (4)$$

At time $t = 0$, the line contactor *LE* shown in Figure 1 is opened and the dynamic braking contactor *DB* is closed, so that

$$e = -iR = \frac{c\phi\omega}{2\pi 10^8} \qquad (5)$$

Since $KE = {}^1/_2 J\omega^2$ then

$$J = \frac{2KE}{\omega^2} = \frac{2KE_0}{\omega_0^2} \qquad (6)$$

Combining equations 4 and 6 gives

$$T + T_L = \frac{2KE_0}{\omega_0^2} \frac{d\omega}{dt} \qquad (7)$$

Expressing the torques in terms of currents, equation 7 becomes

$$\frac{c\phi i}{2\pi 10^8} - \frac{c\phi i_L}{2\pi 10^8} = \frac{2KE_0}{\omega_0^2} \frac{d\omega}{dt} \qquad (8)$$

From equation 5

$$\omega = -\frac{iR 2\pi 10^8}{c\phi} \qquad (9)$$

and since $i = -I_0$ at $t = 0$ then

$$\omega_0 = \frac{I_0 R 2\pi 10^8}{c\phi} \qquad (9a)$$

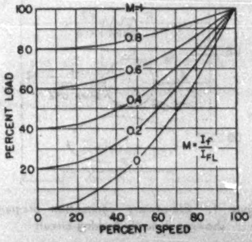

Figure 6. Load torque versus speed for various ratios of friction load to full load when the load torque varies as the square of the speed

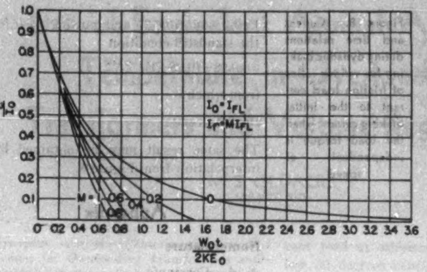

Figure 7a. Current and time relations during dynamic braking for various ratios of friction load to full load when the load torque varies as the square of the speed, and the initial braking current is equal to the full load current

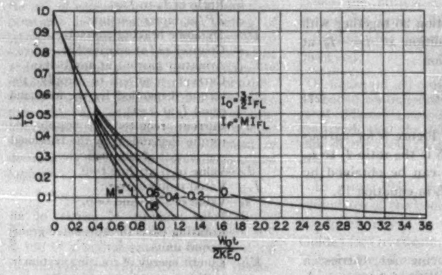

Figure 7b. Current and time relations during dynamic braking for various ratios of friction load to full load when the load torque varies as the square of the speed, and the initial braking current is 150 per cent of full load current

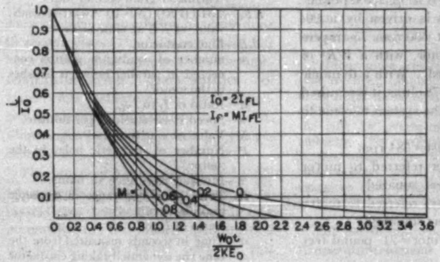

Figure 7c. Current and time relations during dynamic braking for various ratios of friction load to full load when the load torque varies as the square of the speed, and the initial braking current is 200 per cent of full load current

so that

$$\omega_0^2 = \left[\frac{I_0R2\pi10^8}{c\phi}\right]^2 = W_0R\left[\frac{2\pi10^8}{c\phi}\right]^2$$

and

$$\frac{1}{\omega_0^2}\frac{d\omega}{dt} = \frac{-c\phi}{W_0^2\pi10^8}\frac{di}{dt} \qquad (10)$$

Substituting equation 10 in equation 8 and simplifying, the latter reduces to

$$i - i_L = \frac{-2KE_0}{W_0}\frac{di}{dt} \qquad (11)$$

Equation 11 is the fundamental equation governing dynamic braking and can be solved when i_L is known as a function of speed.

Case I

$i_L = A_1\omega + I_f$

Immediately after switching, $i_L = I_L$, for the speed of $\omega = \omega_0$. Therefore, $I_L = A_1\omega_0 + I_f$. The load current can be expressed in terms of I_{FL} by letting $I_L = bI_{FL}$ where b is the ratio between the two currents.

Therefore, $A_1 = (bI_{FL} - I_f)/\omega_0$ so that

$$i_L = (bI_{FL} - I_f)\frac{\omega}{\omega_0} + I_f \qquad (12)$$

After switching to the dynamic braking connection, the armature current at any speed is obtained from equations 9 and 9a yielding, $\omega/\omega_0 = -i/I_0$ so that equation 12 becomes

$$i_L = \left(\frac{I_f - bI_{FL}}{I_0}\right)i + I_f \qquad (13)$$

Substituting equation 13 into equation 11 yields

$$\frac{2KE_0}{W_0}\frac{di}{dt} + \frac{(I_0 + bI_{FL} - I_f)}{I_0}i = I_f \qquad (14)$$

This is a linear first-order first-degree differential equation whose solution, after substitution of the boundary condition, $i = -I_0$ at $t = 0$ is

$$\frac{i}{I_0} = \frac{I_f}{I_0 + bI_{FL} - I_f} - \left[\frac{I_f}{I_0 + bI_{FL} - I_f} + 1\right]\epsilon^{-\frac{W_0t}{2KE_0}\left[\frac{I_0 + bI_{FL} - I_f}{I_0}\right]} \qquad (15)$$

For the fully loaded motor b is unity, and the equation becomes

$$\frac{i}{I_0} = \frac{I_f}{I_0 + I_{FL} - I_f} - \left[\frac{I_f}{I_0 + I_{FL} - I_f} + 1\right]\epsilon^{-\frac{W_0t}{2KE_0}\left[\frac{I_0 + I_{FL} - I_f}{I_0}\right]} \qquad (15a)$$

Equation 15*a* is shown plotted in Figures 5*a*, 5*b*, and 5*c* for various percentage values of I_f to I_{FL}. The maximum value of I_0 is usually governed by the commutating ability of the machine. Two hundred per cent full load current is a representative value.

Case II

$i_L = A_2\omega^2 + I_f$

For conditions immediately after switching where $i_L = I_L = bI_{FL}$ this equation reduces to

$$i_L = \frac{(bI_{FL} - I_f)}{I_0^2}i^2 + I_f \qquad (16)$$

Substituting equation 16 into equation 11 yields

$$\frac{2KE_0}{W_0}\frac{di}{dt} - \frac{(bI_{FL} - I_f)}{I_0^2}i^2 + i = I_f \qquad (17)$$

Substituting b equal to unity, the solution of equation 17 is

$$\frac{W_0t}{2KE_0} = I_0^2\int\frac{di}{(I_{FL} - I_f)i^2 - I_0^2i + I_0^2I_f} + \text{constant} \qquad (18)$$

The right-hand side of this equation is of the form $\int(dx/X)$ where

$$X = ax^2 + bx + c \qquad (19)$$

and the solution[4] depends on the value of the discriminant $b^2 - 4ac$. This term is

$$I_0^2[I_0^2 - 4I_f(I_{FL} - I_f)] \qquad (20)$$

Since I_{FL} is a constant, the maximum value of the second term in equation 20 is obtained by differentiating it with respect to I_f which yields $I_f = {}^1/_2I_{FL}$. Equation 20 then becomes

$$I_0^2[I_0^2 - I_{FL}^2] \qquad (21)$$

Considering only the case where $I_0 \geqq I_{FL}$

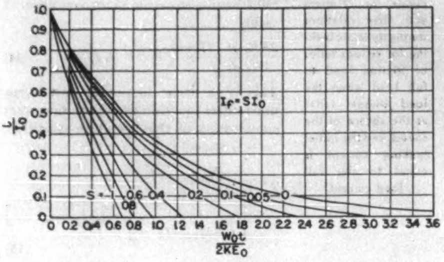

Figure 8. Current and time relations during dynamic braking for various ratios of friction load current to the initial braking current when the load torque is independent of speed

so that the discriminant is positive, the solution of equation 18, after substituting the boundary condition that $i=-I_0$ at $t=0$, is

$$\frac{W_0 t}{2KE_0}=\frac{I_0}{B}\log\left[\frac{2(I_{FL}-I_f)i/I_0-I_0-B}{2(I_{FL}-I_f)i/I_0-I_0+B}\right]\times \left[\frac{2(I_{FL}-I_f)+I_0-B}{2(I_{FL}-I_f)+I_0+B}\right] \quad (22)$$

where

$$B=\sqrt{I_0^2-4I_fI_{FL}+4I_f^2}$$

Letting $I_f=MI_{FL}$ and $I_{FL}=I_0$ equation 22 reduces to

$$\frac{W_0 t}{2KE_0}=\frac{1}{(2M-1)}\log\left[\frac{2(1-M)i/I_0-2M}{i/I_0-1}\right] \quad (23)$$

which is plotted in Figure 7*a* for various values of M. Substitution of $I_f=MI_{FL}$ and $I_0=3/2\ I_{FL}$ into equation 22 gives

$$\frac{W_0 t}{2KE_0}=\frac{3}{\sqrt{16M^2-16M+9}}\times \log\left[\frac{(4M-7+\sqrt{16M^2-16M+9})i/I_0+4M+3+\sqrt{16M^2-16M+9}}{(4M-7-\sqrt{16M^2-16M+9})i/I_0+4M+3-\sqrt{16M^2-16M+9}}\right] \quad (24)$$

which is plotted in Figure 7*b* for various values of M. Substitution of $I_f=MI_{FL}$ and $I_0=2I_{FL}$ into equation 22 gives

$$\frac{W_0 t}{2KE_0}=\frac{1}{\sqrt{M-M^2+1}}\times \log\left[\frac{(M-2+\sqrt{M^2-M+1})i/I_0+M+1+\sqrt{M^2-M+1}}{(M-2-\sqrt{M^2-M+1})i/I_0+M+1-\sqrt{M^2-M+1}}\right] \quad (25)$$

which is plotted in Figure 7*c* for various values of M.

CASE III

$i_L=i_f$

For this constant-torque load condition, equation 11 reduces to

$$i-I_f=\frac{-2KE_0}{W_0}\frac{di}{dt} \quad (26)$$

Integration of equation 26 together with the boundary condition of $i=-I_0$ at $t=0$ gives the solution

$$\frac{i}{I_0}=\frac{I_f}{I_0}-\left(\frac{I_f}{I_0}+1\right)\epsilon^{-\frac{W_0 t}{2KE_0}} \quad (27)$$

which is plotted in Figure 8 for various values of S, where S is the ratio I_f to I_0. This same solution can be obtained by substituting $bI_{FL}=I_f$ in equation 15.

Example

A Thearle balancing set carries a smooth rotor with a WR^2 of 2,400 pound-feet squared. This is driven by a 20-horsepower 230-volt 500-rpm 75-ampere full-load-current motor, with a WR^2 of 21 pound-feet squared. With a dynamic braking resistor of 3.78 ohms, it is required to find the time for the system to come to rest.

Motor speed at test = 584 rpm

WR^2 of test rotor referred to motor shaft = 552 pound-feet squared

WR^2 of two pulleys referred to motor shaft = two pound-feet squared

WR^2 of motor rotor = 21 pound-feet squared

Total WR^2 referred to motor shaft = 575 pound-feet squared

Internal resistance of motor $r=0.3$ ohms

Therefore, total dynamic braking resistor $R=4.08$ ohms so that $I_0=230/4.08=56.4$ amperes.

The steady-state load on the motor is 4.5 amperes and the speed is low enough so that windage can be considered negligible. Then

$$\frac{I_f}{I_0}=\frac{4.5}{56.4}=0.08$$

$$W_0=I_0^2R=(56.4)^2(4.08)=12{,}900 \text{ watts}$$

$$KE_0=(2.31)WR^2N^2 10^{-4} \text{ watt-seconds}$$
$$=(2.31)(575)(584)^2 10^{-4}=45{,}200 \text{ watt-seconds}$$

Therefore,

$$\frac{W_0 t}{2KE_0}=0.143t$$

Using equation 27 and putting $i=0$ for the standstill condition

$$0=0.08-(0.08+1)\epsilon^{-0.143t}$$

from which

$t=18.2$ seconds

The same result may be obtained by interpolation from Figure 8.

Appendix

Nomenclature

A_1, A_2—Constants.
b—Ratio of I_L to I_{FL}.
c—ZP/m, motor constant.
DB—Dynamic braking contactor.
e—Counter *emf* at any instant.
i—Armature current at any instant.
i_L—Current required to supply the torque demanded by the load and equal I_f at $\omega=0$.
I_f—Current required to supply the torque demanded by the frictional component of the load.
I_L—Value of i_L at time $t=0$.
I_{FL}—Rated motor current.
I_0—Value of i at time $t=0$.
J—Polar moment of inertia of all Rotating parts in centimeter-gram-second units.
KE—Kinetic energy of rotating system in centimeter-gram-second units.
KE_0—2.31 (WR^2) $(N)^2$ 10^{-4} watt-seconds, value of KE at time $t=0$.
LE—Line contactor.
m—number of conductor groups connected in parallel between brushes in the motor.
M—Ratio of I_f to I_{FL}.
n—Speed in revolutions per minute.
N—Value of n at time $t=0$.
P—Number of magnetic poles in the motor.
r—Internal resistance in ohms.
R—Total ohms resistance in dynamic braking circuit.
S—Ratio of I_f to I_0.
t—Time in seconds measured from the time the dynamic braking contactor closes.
T—Developed torque in watt-seconds.
T_L—Load torque in watt-seconds.
V—Line voltage.
W_0—I_0^2R in watts.
WR^2—Inertia in pound-feet squared.
Z—Number of armature conductors.
α—Angular acceleration in radians per second per second.
ϕ—Useful flux per pole crossing the air gap.
ω—Angular velocity in radians per second.
ω_0—Value of ω at time $t=0$.

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Use of Equivalent Annual Ambient Temperature in Overloading Transformers and Voltage Regulators

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MEMBER AIEE

Synopsis: In the "Interim Report on Guides for Overloading Transformers and Voltage Regulators,"[1] reference is made to the use of equivalent annual ambient temperatures in determining the loading of transformer equipment. This paper is to show a method of calculating the equivalent annual ambient temperature from readily available climatological data to avoid difficulties in other methods that have been proposed. An outline is given also of conditions under which the equivalent annual ambient temperature may be applied in determining permissible overloads for transformers.

IT has long been understood that the life of a transformer is dependent on the operating temperature of its insulation. A great amount of experimental work on aging of insulation has been done in recent years.[2-6] Although there has been no complete agreement on the time-temperature-aging characteristics of transformer insulation, the eight-degree-centigrade rule formulated by V. M. Montsinger has been given general acceptance.

The proposed American Standard *C*-57[7] recommends 95 degrees centigrade as the hottest-spot temperature that will give normal life for continuous operation of a transformer. For rating purposes this is restated as follows:

> ". . . transformers of usual design having a winding temperature rise (by resistance) of 55C at rated load . . . may be operated continuously at rated load . . . provided that . . . the temperature of the cooling air at no time exceeds 40C and the average temperature of the cooling air during any 24-hour period does not exceed 30C."

For the average transformer, an increase of one per cent in load above 100 per cent increases the hottest-spot temperature rise approximately one degree centigrade. Operation above 100 per cent load at ambient temperatures below 30 degrees centigrade will still not cause the hottest-spot temperature to exceed the limit of 95 degrees centigrade if the following American Standard rule is observed:

> "Oil-immersed self-cooled transformers may be loaded continuously one per cent above rated kilovolt-amperes for each degree centigrade that the daily average temperature of the cooling medium (air) is below 30C."

In June 1942, the transformer subcommittee of the committee on electrical machinery issued an "Interim Report on Guides for Overloading Transformers and Voltage Regulators." This report gave a rule for overloading based on low ambient temperatures which included the following statement:

> "Under some conditions greater permissible overload capability can be obtained by using 'equivalent annual ambient' instead of the daily average ambient when applying the rule for overloads due to change in ambient. The equivalent annual ambient is the temperature which, if maintained continuously, would result in the same aging as that occurring under the actual ambient temperature throughout the year."

The use of equivalent annual ambient temperature instead of average annual temperature takes into consideration the fact that the average rate of deterioration of insulation for a temperature range is higher than the rate of deterioration for the average temperature in the same range. This is in accordance with the eight-degree-centigrade rule mentioned above.

To calculate an equivalent annual ambient temperature would require the averaging of the aging for all the different ambient temperatures throughout the year. For general use this would have to be done for a period of years in order to arrive at a usable mean condition. Such lengthy calculations have been the chief difficulty in the application of equivalent ambient temperature. This paper presents an approximate method of determining equivalent annual ambient temperature from readily available United States Weather Bureau data. The results are compared with those obtained from complete summaries for a few cities to justify the general use of the approximate method.

Use of Climatological Data

Based on climatological data issued by the United States Weather Bureau, the range of ambient temperature during the year may be divided into three parts, as follows:

1. *Range during the year of monthly mean temperatures.* These are summarized nationally and are readily available.
2. *Range during the month of daily mean temperatures.* These are not summarized nationally, except on a normal basis, but are available locally from annual reports.
3. *Range during the day of hourly temperatures.* These are available nationally as the difference between the normal daily maximum and the normal daily minimum temperatures.

The daily mean temperature is the average of the maximum and minimum temperatures for the day, not the weighted average. The monthly mean temperature is an average of the daily mean temperatures during the month.

For use in calculating equivalent annual ambient temperature, there has been general agreement on the use of items 1 and 2 in the preceding list. There has been disagreement on the use of item 3 on two points, as follows:

(*a*). Present American Standard *C*-57 states: "A transformer may be operated continuously at rated load . . . provided that . . . the temperature of the cooling air at no time exceeds 40C, and the average temperature of the cooling air during any 24-hour period does not exceed 30C." To

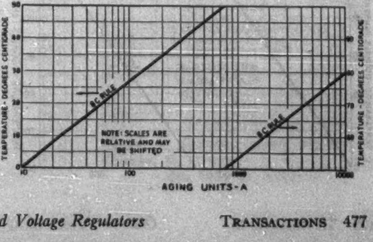

Figure 1. Effect of temperature on aging of transformer insulation

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Table I. Calculation of Equivalent Ambient Temperature for Kansas City, Mo.

(Considers Only the Monthly Mean Temperature)

Month	Monthly Average Temperature Centigrade (2)	Monthly Average Temperature Fahrenheit (3)	$\epsilon^{0.0866T}$ Centigrade (4)	$\epsilon^{0.0481T}$ Fahrenheit (5)	A_C Figure 1 (6)	A_F Figure 2 (7)
January	−1.3	20.7	0.89	4.17	9	42
February	−0.7	30.8	0.94	4.40	10	44
March	6.2	43.2	1.71	8.00	17	81
April	12.7	54.9	3.01	14.00	30	140
May	18.1	64.6	4.78	22.40	48	225
June	23.3	73.9	7.50	34.95	75	350
July	25.6	78.2	9.15	43.00	91	432
August	24.9	76.8	8.60	40.20	86	405
September	20.8	69.4	6.06	28.20	60	285
October	14.5	58.1	3.51	16.30	35	166
November	7.0	44.6	1.83	8.55	19	86
December	0.7	33.3	1.06	4.96	11	50
Total	151.8	657.5	49.04	229.13	491	2,306
Average	12.7	54.8	4.09	19.1	41	192
Equivalent temperature			16.3	61.3	16.3	61.3

consider equivalent daily temperature instead of average daily temperature is contrary to this provision of the American Standard.

(*b*). On a daily temperature cycle the thermal lag of the transformer flattens out the range of hot-spot temperatures to make it less than the range of ambient temperatures.

For these reasons it is believed that the range during the day of hourly temperatures should be ignored in calculating equivalent annual ambient temperature and only items 1 and 2 considered. Furthermore, it was found that the use of item 3 affected the calculated value of equivalent annual ambient temperature by only a small fraction of a degree.

Calculation of Equivalent Ambient Temperature—Direct Method

Accepting as valid the eight-degree-centigrade law formulated by V. M. Montsinger,[2] the life of transformer insulation is halved for every eight-degree-centigrade increase in temperature and, conversely, is doubled for every eight-degree-centigrade decrease. This is expressed by the equation

$$L = K\epsilon^{-0.0866T} \tag{1}$$

where

L = life of the transformer insulation
T = temperature in degrees centigrade
K = constant chosen to give rated life at rated temperature

Conversely, the rate of aging of transformer insulation per unit time of operation may be expressed by the equation

$$A = K_1\epsilon^{0.0866T} \tag{2}$$

where

A = rate of aging or aging units consumed per unit time of operation
T = temperature in degrees centigrade
K_1 = a constant chosen to give rated life at rated temperature

For convenient use with climatological reports on a Fahrenheit basis, equation 2 may be rewritten as

$$A = K_1\epsilon^{0.0481T} \tag{3}$$

where T = temperature in degrees Fahrenheit.

For an example of the use of these formulas, only monthly mean temperatures will be used. Account will be taken of range of daily mean temperatures later. The data for Kansas City, Mo., are given on attached Table I. Column 2 gives monthly mean temperatures. Column 4 gives aging units for these temperatures obtained by substituting the temperature values in equation 2. Since the aging data are to be reconverted to temperature data, the constant K_1 may be taken as unity. The average of column 4 reconverted to temperature by equation 2 gives the equivalent annual temperature, that is, the temperature giving an average monthly aging that would result in the same total aging for the year as for the temperature cycle of column 2. Calculations with equation 3 are shown in columns 3 and 5 of attached Table I.

A graphical solution of the same data may be obtained by plotting equations 2 and 3 as shown in Figures 1 and 2, respectively. Figure 1 is plotted for the eight-degree-centigrade rule by doubling the aging unit abscissa for each eight-degree-centigrade increase in the temperature ordinate. The curve of Figure 2 is plotted in the same manner for 14.4 degrees Fahrenheit. As in the mathematical solution, the value chosen for the constant is unimportant.

Columns 6 and 7 show the use of these curves in obtaining the equivalent ambient temperature graphically. It will be noted that the use of varying constants gives different aging units, but that the temperatures check.

Calculation of Equivalent Ambient Temperature—Approximate Method

A survey was made of climatological data issued by the United States Weather Bureau for 23 cities in the United States (see Table II). Equivalent annual ambient temperature was calculated by the direct graphical method described above from normal monthly mean temperatures. These data were plotted as divergence K_8 from the average annual temperature as a function of range R during the year of monthly mean temperatures. The results are shown as the K_8 curve on Figure 3.

The effect of range during the month of daily mean temperatures was not calculated nationally because of the prodigious quantity of work required. However, a complete check was made for Chicago and had already been made for six other cities in a recent paper by Hellmund and McAuley.[8] It was found that the true equivalent ambient temperature could be approximated by using the curve K_8 of Figure 3 and adding ten de-

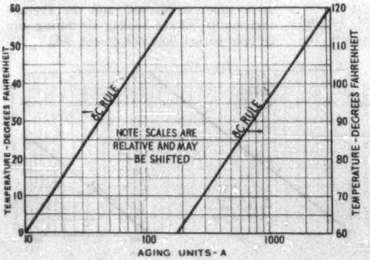

Figure 2. Effect of temperature on aging of transformer insulation

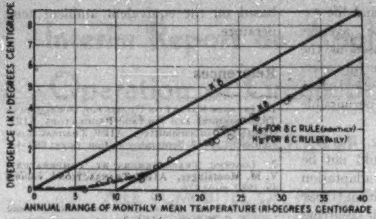

Figure 3. Divergence K between equivalent annual ambient temperature T_e and annual average ambient temperature T_a as a function of annual range R of monthly mean temperature

grees centigrade to the range over the year of monthly mean temperatures. The curve K'_8 is the K_8 curve moved up ten degrees centigrade so that divergence K'_8 between equivalent annual ambient temperature and average annual ambient temperature may be read directly as a function of annual range R.

This adjustment of ten degrees centigrade is no exact value taken from climatological summaries. Reference to Figure 4, in which the data for the Chicago five-year summary are given in curve form, will show the daily mean temperature curve to be very close to the hourly temperature curve. This is because the recorded daily mean is usually higher than the weighted mean, especially in the summer. The difference between the curve of daily mean temperatures and that of monthly mean temperatures is a matter of only a few degrees except at the end points. Any adjustment must take care of both the average difference of a few degrees and this end-point difference. The ten-degree-centigrade adjustment was used because it closely approximates calculated data, and because it gives a curve that is easily and conveniently used with the readily available monthly mean data. Thus, if

1. July average temperature = 25 degrees centigrade
2. January average temperature = −5 degrees centigrade
3. Annual average temperature = 10 degrees centigrade

then annual range

R = 30 degrees centigrade
K'_8 = 6.5 degrees centigrade
T_e = 16.5 degrees centigrade

For six cities[6] the equivalent ambient temperature had already been calculated by the direct method from summaries of hourly temperatures for the six-year period of 1932–37, inclusive. Similar calculations were made for Chicago for the period of 1937–41, inclusive. The results were then compared with the approximate values of equivalent temperature from Table II, column 8. A preliminary check-up had shown a difference between normal annual mean temperature and mean temperatures for the period covered by the summaries. It was necessary to adjust the direct calculations for this difference. The results are shown in Table III. It can be seen that the correlation between results by the two methods is within the accuracy of the problem as a whole.

Table II. Equivalent Annual Ambient Temperature

City	Based on Monthly Mean Temperatures					Based on Daily Mean Temperatures Using K'₈ of Figure 3	
	Annual Average Temperature (T_a) (2)	Annual Range (R) (3)	Equivalent Annual Ambient Calculated (T_e) (4)	Divergence (K_8) Calculated (5)	Divergence (K_8) From Figure 3 (6)	(K'_8) (7)	(T_a) (8)
Bismarck	4.7	35.0	10.0	5.3	5.4	7.6	12.3
St. Paul	6.8	33.1	11.8	5.0	5.0	7.1	13.9
Duluth	3.3	31.1	7.8	4.5	4.5	6.7	10.0
Des Moines	9.7	30.7	14.0	4.3	4.5	6.6	16.3
Chicago	9.5	27.1	13.0	3.5	3.7	5.8	15.3
Kansas City	12.7	26.9	16.3	3.6	3.7	5.8	18.5
St. Louis	13.3	26.5	16.9	3.6	3.6	5.7	19.0
Helena	6.2	25.3	9.6	3.0	3.3	5.5	11.7
Boston	9.8	24.3	12.8	3.0	3.1	5.2	15.0
New York	11.3	23.8	13.9	2.6	2.9	5.1	16.4
Denver	10.0	23.0	13.0	3.0	2.8	5.0	15.0
Santa Fe	9.3	22.2	11.7	2.4	2.7	4.8	14.1
Memphis	16.4	22.1	19.2	2.8	2.7	4.8	21.2
Dallas	18.4	21.6	20.7	2.3	2.5	4.7	23.1
Phoenix	21.1	21.5	23.5	2.4	2.5	4.6	25.7
Raleigh	15.6	20.9	17.9	2.3	2.3	4.5	20.1
Galveston	20.9	16.5	22.3	1.4	1.4	3.5	24.4
Portland, Oreg	11.7	15.2	12.9	1.2	1.2	3.3	15.0
Jacksonville	20.7	14.8	21.8	1.1	1.1	3.2	23.9
Seattle	10.6	13.1	11.1	0.5	1.0	2.8	13.4
Los Angeles	16.8	9.2	17.3	0.5	1.0	2.0	18.8
Key West	24.9	7.8	25.2	0.3	1.0	1.7	26.6
San Francisco	13.4	6.1	13.4	0	1.0	1.3	14.7
Average	12.9	21.6	15.5	2.6	2.7	4.7	17.6
National mean	12.3	22.8	14.9	2.6	2.8	4.9	17.2

All temperatures are in degrees centigrade.

The methods described above for obtaining equivalent annual ambient for air-cooled transformers may be applied to water temperature for water-cooled transformers. This is especially applicable when the cooling water is obtained from a lake or river so that there is a considerable annual range in water temperature. For Lake Michigan water used in Chicago, the averages of the monthly maximum temperatures over a 15-year period were used to give the annual temperature cycle. The equivalent temperature obtained by using Figure 1 checked very closely with that obtained from use of K_8 in Figure 3.

Application to Transformer Loading

For constant load over the year or for an annual load cycle that has its maximum in summer, the maximum permissible load for normal life can be based on the equivalent annual temperature of the cooling medium obtained by the methods herein described. This is applicable both to water-cooled and to air-cooled transformers. The increase in loading for reduction in ambient temperature for

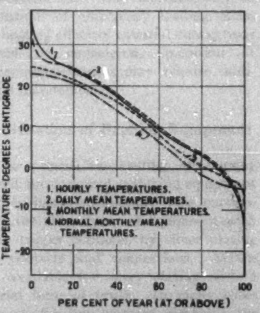

Figure 4. Outdoor ambient temperatures, Chicago, Ill. (From U. S. Weather Bureau data, 1937–41, inclusive)

Table III. Equivalent Ambient Temperature—Comparison of Direct and Approximate Methods

		Equivalent Annual Temperature		
		Direct Method		Approximate Method from Figure 3
City	Annual Average Temperature	Calculated*	Corrected to Normal*	
Bismarck	4.7	14.7	13.6	12.3
Helena	6.2	12.9	12.1	11.7
Kansas City	12.7	20.1	19.2	18.5
Denver	10.0	16.2	15.0	15.1
Dallas	18.4	22.0	21.6	23.1
Santa Fé	9.3	14.0	13.4	14.1
Chicago	9.5	15.2	14.3	15.3
Average	10.1	16.4	15.6	15.7

All temperatures are in degrees centigrade.

* Calculations are based on six- and five-year summaries. Correction is made for difference in mean temperature between summary period and normal.

self-cooled transformers according to American Standard *C*-57 is one per cent for each degree that the temperature is below 30 degrees centigrade. For the temperature data given above, the equivalent annual ambient was 16.5 degrees centigrade, or 13.5 degrees below the standard ambient temperature. Therefore, the permissible constant load throughout the year would be 113.5 per cent of rated load, with normal life expectancy. However, if the average monthly temperature were used as the basis of increased loading, the July average ambient temperature of 25 degrees centigrade would limit the permissible overload to 105 per cent.

For other types of loading, the procedures herein described would not be directly applicable without adaptation to each specific loading condition.[9] For example, for a constant load occurring only a few hours each day, it would be feasible to consider the mean ambient temperatures for those hours rather than for the entire day. When winter and summer loads are not the same, it would be feasible to calculate an annual aging curve based on a combined load and temperature cycle in order to fix the maximum level of the annual load curve as a whole.

Conclusion

The approximate method herein described may be used to obtain equivalent annual temperature from readily available climatological data. For loading on an annual basis the maximum permissible load for normal life can be based on the equivalent ambient temperature.

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Interim Report on Application and Operation of Circuit Breakers and Switchgear

AIEE COMMITTEE ON PROTECTIVE DEVICES
Subcommittee on Circuit Breakers, Switches, and Fuses

THE present war emergency makes it necessary that maximum use be made of existing equipment and that a minimum amount of critical materials be used for new equipment. The following guides have been prepared as an aid to those involved in the application and operation of circuit breakers and switchgear.

This report is intended to cover equipment of the following types:

1. A-c power circuit breakers above 600 volts, both indoor and outdoor.
2. Air disconnecting switches.
3. Switchgear assemblies.

Basis of Design of Apparatus

The name-plate rating of apparatus does not necessarily indicate the load which can be carried safely under all conditions. It specifies the load which may be carried without exceeding the specified temperature rise. However, it is the total temperature and the duration of such temperature which determine the life of apparatus. Obviously the total temperature depends not only on the loading but on the ambient temperature as well.

The present standards for circuit breakers and switchgear provide that the temperature rise of the contacts shall not exceed 30 degrees centigrade at rated load and that the apparatus shall be suitable for continuous operation at rated load, provided the ambient temperature does not exceed 40 degrees centigrade for apparatus with plain copper contacts or 55 degrees centigrade if the contacts and other connections are silver or equivalent. This means that the total temperatures should not exceed 70 degrees centigrade for plain copper or 85 degrees centigrade for silver contacts.

It should be noted that the temperature rise of contacts may increase as a result of oxidation of the contact surface, and, therefore, the standards are based on sufficient maintenance to keep the temperature rise within specified limits.

Effect of Ambient Temperature

It is apparent that, if the ambient temperature is higher than these values, the loading must be less than the name-plate rating to avoid exceeding the permissible total temperature. It is also apparent that if the ambient temperature is lower than these values, the loading may be greater than the name-plate rating. The loading should be reduced approximately two per cent for each degree that the ambient temperature is above these values, and in some cases it may be increased as much as one per cent for each degree that the ambient temperature is below the values specified in the standards. See Figure 1 for relation between loading and ambient temperature for apparatus with silver contacts. These factors should not be used to increase the loading more than 30 per cent without special consideration.

The foregoing recommendations may be applied for average conditions, but they do not represent necessarily the maximum in all cases. This is due to the variations in thermal characteristics of different designs and sizes of switching equipment and variations in types of installation. Therefore, to determine the maximum carrying capacity of individual installations, studies or tests should be made to establish the proper relation between loading and ambient temperature. This is particularly true in the case of heavy current installations.

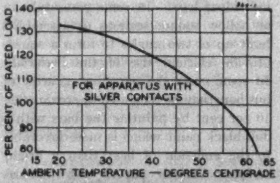

Figure 1. Relation between loading and ambient temperature

Artificial Cooling

Artificial cooling may be used to increase the permissible loading in some installations. This may take the form of forced ventilation of the room in which the apparatus is located, or it may involve forced ventilation through the equipment itself. For oil-filled equipment there may be some possibility of artificial circulation of the oil to assist in the transfer of heat and thus increase the permissible loading. Extreme care must be used in any application of forced cooling to be sure that there are no parts of the equipment which do not benefit by the forced cooling and might reach excessive temperatures. It should be recognized that forced ventilation may tend to increase the danger of fire spreading to other parts of the installation.

Emergency Loading

Under emergency conditions the permissible loading may be higher than under normal conditions, particularly if the duration is not too long and if the condition does not recur frequently. It is impracticable to assign specific values for all types of switchgear. The characteristics of the specific apparatus involved should be thoroughly investigated in connection with the determination of limitations of emergency loading. Also, the heating effect of external connections should be considered. Operation at higher temperatures may require additional maintenance.

Modification of Existing Equipment

The permissible loading of existing circuit breakers and switching equipment may be increased in many cases, particularly the older types of equipment with plain copper contacts, by silver plating the contacts and connections. Means are available for silver plating with a special form of brush. In many cases this can be done without removing the parts or even disturbing the adjustment of circuit breakers and switches. If the loading is increased by resorting to such

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Personnel of subcommittee on circuit breakers, switches, and fuses: R. T. Henry, *chairman;* A. E. Anderson, H. D. Braley, O. E. Charlton, F. W. Cramer, W. Deans, W. S. Edsall, F. R. Ford, H. W. Haberl, H. J. Lingal, L. R. Ludwig, J. B. MacNeill, H. H. Marsh, Jr., J. R. North, H. V. Nye, M. S. Oldacre, H. H. Rudd, R. M. Smith, H. P. St. Clair, H. E. Strang, H. R. Summerhayes, J. D. Wood.

treatment, care must be used to insure against excessive temperatures in other parts of the apparatus. Various papers[6,7,8] indicate some of the possibilities along this line.

The interrupting capacity of existing circuit breakers can be increased in many cases by the installation of new contact systems involving oil blast, deion grids, ruptors, and so forth, together with other parts as necessary, depending upon the type of breaker and its manufacturer.

Current transformers should be carefully considered to determine their actual carrying capacity. Many current transformers are capable of carrying more than their rated load continuously, and most of them can carry short-time emergency loads higher than their rating. "The Interim Report on Transformers"[5] gives useful information in this connection. Studies or tests should be made to determine the maximum carrying capacity of individual installations.

Fuses generally do not have excess current-carrying capacity corresponding to most other devices in that they are fundamentally a thermal overload protective device and accordingly cannot be given overload ratings. In the application of fuses where selective operation is involved, consideration should be given to the effect of ambient temperature on the characteristics of the fuses.

Simplification of New Installations

In selecting equipment for new installations every effort should be made to use the minimum amount of critical materials. Probably the greatest saving can be made by choosing a simple scheme of connections using the minimum amount of equipment consistent with the requirements. Duplication of busses and switching equipment may be avoided in many cases, at least for the duration of the present emergency. The margin between the rating of apparatus and the actual load may be less than might be considered satisfactory under normal conditions.

Where additions are to be made to existing installations of two-bus design, it may be feasible to connect the new circuits to only one of the busses, at least during the emergency. Equipment for such additions may even be obtained by removing some of the existing connections to one of the busses.

The short-circuit current may be kept low in many cases by sectionalizing or other means to permit the use of smaller circuit breakers or existing circuit breakers which might not be satisfactory under normal conditions after the emergency is over.

A large circuit breaker of adequate interrupting capacity may be used in some cases as a backup for a group of smaller circuit breakers, with the protective relay system designed to permit automatic operation of the individual small breakers on faults within their interrupting capacity but locking out and transferring the operation to the large circuit breaker on faults exceeding the interrupting capacity of the small circuit breakers. In such cases the momentary rating of the small circuit breakers should be carefully considered.

In some cases it may be possible to open one or more of the supply circuits in order to reduce the interrupting duty to a value which the otherwise inadequate circuit breakers are capable of interrupting.

In transformer substations with a single transformer bank it may be permissible to omit the circuit breaker on either the high-voltage or the low-voltage side.

In laying out new switching structures the cross section of the busses may be reduced for part of their length by grouping the heavier circuits close to each other and placing the lighter circuits toward the ends of the bus in some cases.

It may be possible in other cases to locate the main incoming circuit near the center of the bus to permit tapering the bus toward the ends.

In outdoor structures it may be possible to use galvanized iron pipe for conductors instead of copper tubing or to substitute other forms of steel for copper conductors in many cases.

In the cases of busses designed to carry heavy current, appreciable amounts of material may be saved by proper consideration of the configuration of the material used. When flat bars are used, the heat dissipating qualities will be considerably improved if the bars are placed on edge rather than flat in order to improve the ventilation. When multiple flat bars are used, consideration should be given to the proper spacing to improve the division of current between the individual bars. In some cases tubular or hollow square sections or assemblies built up of two angles to form a square provide efficient use of the materials. In some cases the carrying capacity of bus bars may be increased approximately 10 per cent by painting the bars with a dull black paint which is more favorable to the emission of heat than a bright surface. Various papers and publications[1,2,3,4] cover the design of busses for heavy current.

Tinning or silver plating all contacts will permit satisfactory operation at higher temperatures than would be the case with plain copper contacts.

Where multiple cables are employed in high-capacity circuits, careful attention should be given to the physical arrangement in order to divide the current evenly between the various cables. It should be noted that the configuration of multiple cables in a flat spacing may result in the outer cables carrying more than their share and the inner ones carrying less than their share of the total current. In some cases this condition can be greatly improved by the installation of small "doughnut type" reactors around the cables which tend to carry the most current.

Wood structures may be used instead of steel. These may be permanent structures in some cases and even greater savings may sometimes be made by using temporary wood structures with a bare minimum of equipment which might not be considered satisfactory for normal conditions after the present emergency is over. Obviously simple temporary structures should be used where there is a reasonable possibility that the use may be limited to the duration of the emergency.

In the interior wiring of new plants the more general use of higher distribution voltages may result in appreciable savings of critical materials. Consideration should be given to using 600 volts instead of 480, and 480 volts instead of 240.

Reactive current is a very important consideration both in industrial plants and in utility systems. Appreciable savings may be made in many cases if the reactive current is supplied at the load rather than from the utility system. Supplying the reactive current at the load results in less voltage drop, less energy loss, and releases system capacity which may be needed to carry other useful load. In many cases the reactive current can be supplied at the load by using synchronous motors, synchronous condensers, or capacitors.

Capacitors may be used to make appreciable savings of critical materials, particularly if they are scattered and connected as near as possible to the various loads. In fact, capacitors connected directly to the leads of each motor should be considered, at least for all but the very small motors. Considerable savings may often be made in the plant wiring in this way. It may be desirable or necessary in some cases to make provision for disconnecting the capacitors during light load periods.

New Developments in Potential-Transformer Design

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Synopsis: This paper describes certain design ideas applied to potential transformers to accomplish substantial reductions in volume and weight, with improved reliability and with better accuracy than the best requirements of the American Standards. The size of these units is such that they appear mostly as bushings.

Perhaps the most striking feature of construction is the arrangement and the insulation of the high-voltage winding. Liquid-impregnated porous paper has been substituted for the combination liquid and solid used in the conventional designs. Another notable improvement incorporated in these transformers is the elimination of all gaskets. The amount of liquid insulation required is extremely small, and therefore premium insulating liquids (such as the Askarels) can be used without much additional expense.

In the past, the design of liquid-filled potential transformers, perhaps with the exception of the cascade connected units[1] has followed very closely that of power transformers. Thus, until very recently, a 69-kv potential transformer was designed and built essentially according to the same principles and practices as followed in the design of a 69-kv small distribution transformer, with the result that the units were bulky, and their size and weight were out of proportion with their operating duties. A comparison of the new with the old is given in Table I in which it will be seen that the new units are a little over 40 per cent of the weight of the older ones.

Principles of Design

High-Voltage Winding

The high-voltage winding of the new potential transformers is the concentric layer-wound type, shown in Figure 1 for a transformer for connection between line and ground.

This type of construction consists of a plurality of layers of winding and shields so disposed as to give uniform voltage distribution. The innermost layer adjacent to the low-voltage winding starts from the neutral and the successive layers are connected in series progressively, the outermost layer being connected to the line. This winding is placed between two cylindrical shields; one is connected to the innermost layer and eventually to ground, while the other shield surrounds the last (outermost) layer and is connected to the line.

With this arrangement properly proportioned, the relative position of each part of the winding in the electrostatic field between the two shields will not deviate greatly from its actual low-frequency position. Thus, when an impulse voltage is imposed on the line end, it will be substantially uniformly distributed throughout the winding, regardless of the steepness of the oscillation of the applied wave. It is obvious, therefore, that the insulation between the shields and the adjacent layer and between successive layers is only that required to withstand the voltage developed within the individual layers.

Scheme of Design for Cascade Connection

One practical scheme of reducing the size and weight of those transformers which are normally used for operation between line and neutral of a solidly grounded system is by means of the so-called cascade connected units.[1] In this design the insulation can be distributed to the best advantage.

Figure 2 shows the multilayer type of winding construction as is ordinarily used in a cascade potential transformer.

The deciding factors between the single-stage and cascade potential transformers are the economy and accuracy.

In general, the accuracy of the cascade potential transformer is not as great as that of the single-stage type. For voltages higher than 115 kv, it appears that the cascade type is somewhat more economical than the single-stage.

Scheme of Design for Isolated Operation

Figures 3 and 4 show the multilayer type of construction which could be used for a fully insulated transformer for connection from line to line of an isolated system.

In Figure 3 only one high-voltage coil section is used. This type of construction may be applied only to relatively low voltages because of difficulty which is

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Conclusions and Cautions

From the foregoing it is apparent that the loading on existing equipment may be increased in many cases and that new equipment may be selected with a view to using a minimum of critical material during the present war emergency. Other suggestions and more details may be found in the papers and publications listed at the end of this report.

It must be recognized that the loading on equipment should not be increased without a thorough study of the limitations of the apparatus involved and of associated equipment such as cables, terminals, current transformers, soldered connections, and so forth. It must also be recognized that increasing the loading on equipment is almost certain to require more maintenance and may shorten the life of some apparatus, but it is undoubtedly true that appreciable savings of critical materials can be made without sacrificing life unduly and without requiring excessive maintenance.

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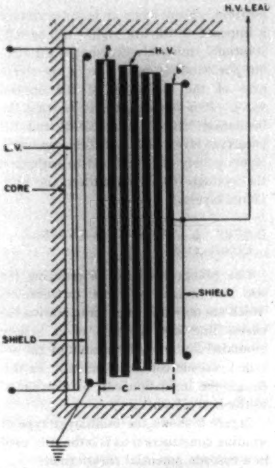

Figure 1. Transformer for operation between line and grounded neutral

encountered in bringing out the high-voltage terminal at the beginning of the high-voltage winding.

For this reason, the scheme shown in Figure 4 is preferred. In this case, the high-voltage winding is divided in two equal sections which are connected together at starting ends of the windings. Each section is of the concentric multilayer type similar in all respects to the arrangement shown in Figure 3, except that the spacing between the inner shield and the low-voltage winding is, of course, designed to withstand a test voltage equivalent to the full line-to-line voltage.

Novel Method of Insulating the High-Voltage Winding

The new method of insulating the high-voltage winding is shown, schematically, in Figure 6. To make the matter more clear, let us compare the new with the old. In potential transformers of the conventional type, the distance between the high-voltage and low-voltage winding of, say, a 138-kv line-to-line connected potential transformer has been essentially the same as the distance between the high-voltage and low-voltage windings of a power transformer of the same voltage rating. Furthermore, in a power transformer this insulating distance in general consists of one or more liquid ducts interleaved with solid insulating barriers. This same type of insulation (liquid ducts plus solid barriers) is used between the high voltage and the core. In a power transformer the liquid ducts have a useful function, the cooling of windings. Such duty is not required in a measuring transformer, because the losses, under ordinary conditions, are negligibly small. A simple calculation of I^2R loss in the high-voltage, or primary, winding of a 138-kv potential transformer may be of interest to those not familiar with the problem. Let us assume that the resistance of such winding is 10,000 ohms. The copper loss at 500 volt-amperes (which is the normal rating of a potential transformer) is then

$$\left[\frac{\text{Output volt-amperes}}{\text{Primary winding volts}}\right]^2 R = 0.13 \text{ watt}$$

which is a very negligible quantity. It is apparent then that the cooling of the high-voltage winding is of secondary importance, and it is, therefore, not necessary to have any liquid ducts. It is well recognized that, in an insulating structure comprising oil operating in series with solid insulation, the dielectric stress is inversely proportional to their dielectric constants, and ordinarily the solid insulation is not effectively utilized.

In the new design, the complete insulating structure consists of solid insulation which is impregnated with an insulating liquid. This combination may be considered a uniform insulating mass having a dielectric constant intermediate between that of the liquid and solid insulation, and its dielectric strength approaches the impregnated paper of an oil-filled cable.

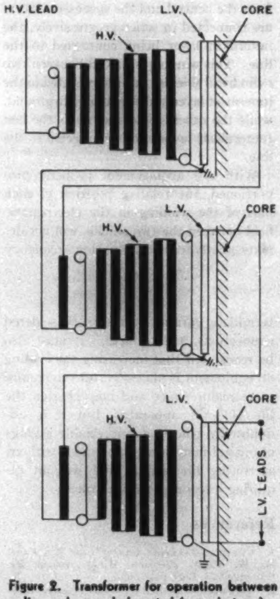

Figure 2. Transformer for operation between line and grounded neutral (cascade type)

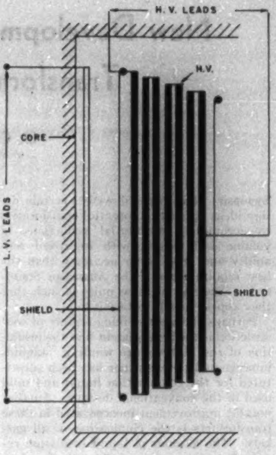

Figure 3. Transformer for operation between lines

Referring now to Figures 4 and 5, the insulation between layers is easily provided for. This may consist of several sheets of paper having folded edges with the layers located between the folds. Such a construction leads to a coil having a small radial thickness. The shield and successive layers give a very good distribution of voltage stress so that the insulation between layers is well proportioned to the stress. This type of construction would be very desirable if the dielectric stresses at the ends were not distributed between the liquid and solid insulation. As has been discussed previously, this condition gives rise to inefficient use of insulation.

Referring to Figure 5, one method of insulating the ends of the layer of the coil would be by the use of very wide folds in the layer insulation and by the use of stepped pressboard barriers as shown. Such an arrangement, however, is not very efficient since the path *A* from the shield to ground is, in effect, outside the uniform voltage distribution dictated by the high-voltage winding and therefore to avoid creepage failures must be made unnecessarily long.

The good advantages of the cylindrical layer-wound coil, therefore, would be lost unless some more efficient method is found to insulate the ends of the winding. The

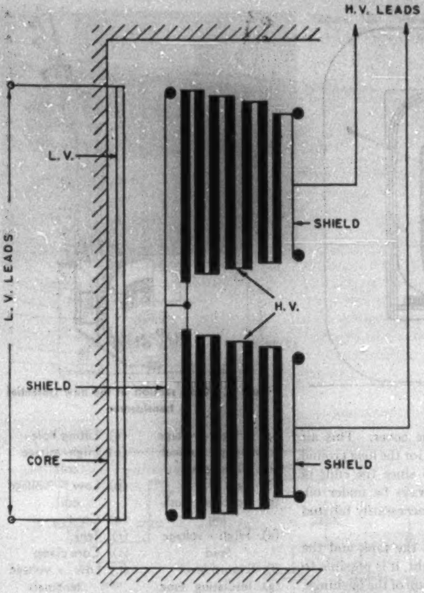

Figure 4. Transformer for operation between lines

scheme shown in Figure 6 seems to offer a good solution to this problem. In this method, the layer insulation consists of paper with folded edges and additional layers of paper. The width of the latter is much longer than the width of the winding layer. After the coil is wound, the extensions of the paper are folded over the shield as shown in the illustration. It will be noted that such a method of insulation not only solves the problem of insulating the ends of the coil, but automatically provides the necessary solid insulation between the line shield and the core (ground) (path *B* of Figure 5).

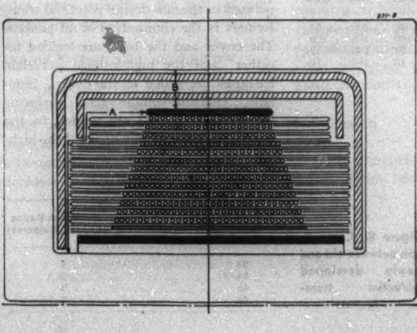

Figure 5. Method of insulating the high-voltage winding (for grounded neutral transformer)

In using the method of insulating shown in Figure 6, the coil design is so arranged that the insulation at the ends of the winding cuts the electrostatic line of force at right angles and at approximately equipotential steps, thereby realizing all the benefits of cascading the voltage. The application of this type of insulation to transformers with both bushings isolated is shown in Figure 7. The high-voltage coil is divided in two sections *A* and *B*. Each coil is insulated in the same manner as illustrated in Figure 6; pressboard barriers are placed between the two sections, and the entire coil assembly is covered with layers of paper. Thus, oil ducts of previous designs are completely replaced by solid insulating material, and, in a manner similar to the current transformers described in another paper,[3] the entire electrostatic field contains liquid-impregnated porous paper of uniform permittivity.

Such a method of insulation with closely wound and insulated coils has, of course, required the development of a new treatment and impregnating process.

By carefully controlled materials and methods, a very flat power-factor temperature curve is obtained, and any danger of thermal instability is eliminated.

IMPULSE CHARACTERISTICS OF THE HIGH-VOLTAGE COIL SHOWN IN FIGURE 7

Experimental data and theoretical study (see Appendix) have shown that when this type of high-voltage winding is subjected to impulse voltages with vertical front waves under the worst conditions, approximately 75 per cent of the total voltage appears across one section. Contrary to the behavior of the winding of conventional design, this voltage is approximately uniformly distributed among successive winding layers, and therefore at no point within the high-voltage winding is it necessary to provide insulation to withstand more than the proper share of the voltage appearing across half the winding.

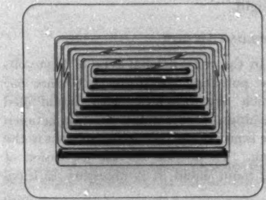

Figure 6. Improved method of insulating the high-voltage winding (for grounded neutral transformer)

MAGNETIC CIRCUIT

Some of the benefits of such an efficient coil construction would be lost without a companion magnetic circuit of equal efficiency. The wound-core type of construction,[3] so successfully used in distribution transformers, has been adopted in the new type of potential transformers. The magnetic circuit is formed from magnetic strip material, spirally wound flatwise. The core closely embraces the

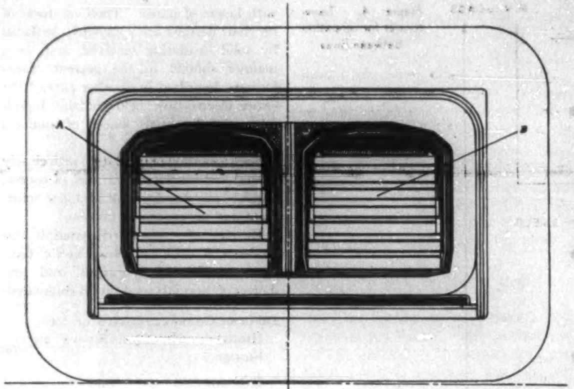

Figure 7. Improved method of insulating the high-voltage winding (transformer for operation between lines)

high- and low-voltage coils so that the coil structure substantially fills the window of the core, providing a high space factor which reduces its volume and weight.

Bushing Design

In the conventional type of high-voltage potential transformers (Figure 8a) which are provided with liquid-filled bushings, there is not connection between the liquid in the terminals and that in the tank. To provide for the expansion of the main liquid, it is necessary to leave an air space under the cover. This air chamber is responsible for the long ground sleeve of the bushings since the ends of these sleeves must always be under oil. Such transformers are necessarily tall and bulky.

If the joint between the tank and the cover is made liquid tight, it is possible to suppress the lower portion of the bushings. Evidently, the expansion chamber of Figure 8a, in the latter case, may be transferred to the top of the bushings. The size of the latter expansion chamber, of course, would be too large if applied to a transformer of the conventional type containing a large amount of liquid. In the new transformers, the amount of liquid is only a small fraction of that used in the conventional design, and therefore it has been found practical to enlarge the top of the porcelain and to use it for the expansion of the liquid. Such a construction is illustrated in Figure 9. In the new design the terminals are made an integral part of the high-voltage coils and are extended inside the porcelain shells.

Table I

	Relative (Per Cent) Values			
	34,500/115 Volts		69,000/115 Volts	
	Old	New	Old	New
Over-all height	100	51	100	51
Width of base	100	100	100	108
Net weight	100	41	100	42
Liquid volume	100	13	100	16

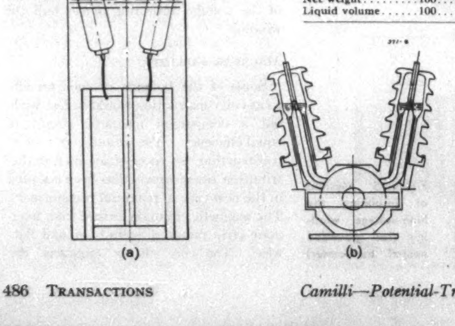

Figure 8. Comparison between old and newly developed potential transformers

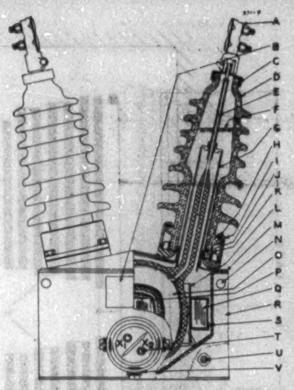

Figure 9. Cross section of the new potential transformer

(a). High - voltage clamp-type terminal
(b). Name plate
(c). Seal
(d). Porcelain bushing
(e). High - voltage lead
(f). Liquid level
(g). Insulating tube
(h). Cushion
(i). Mechanical clamping ring
(j). Insulating collar
(k). Seal
(l). Insulating shield
(m). Welded joint
(n). Lifting hole
(o). High-voltage coil
(p). Low - voltage coil
(q). Core
(r). Tank
(s). Core clamp
(t.) Low - voltage terminals
(u). Ground terminal
(v). Low - voltage outlet, remove pipe plug for one-inch conduit

Gasketless Construction

Another notable improvement incorporated in the new design potential transformers is the elimination of all gaskets. The cover and the tank are welded together, and the high-voltage porcelain bushings are sealed to the cover. Similarly, the low-voltage glass bushings[4] shown in Figure 10 are welded to the rank. The cross section of one of the high-

Table II

Voltage Rating (Kilovolts)	Maximum Rating (Kilovolt-Amperes)
24	2
34.5	2.5
46	3
69	3.5

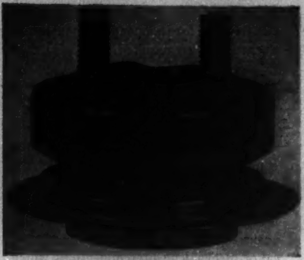

Figure 10. View of low-voltage glass bushing

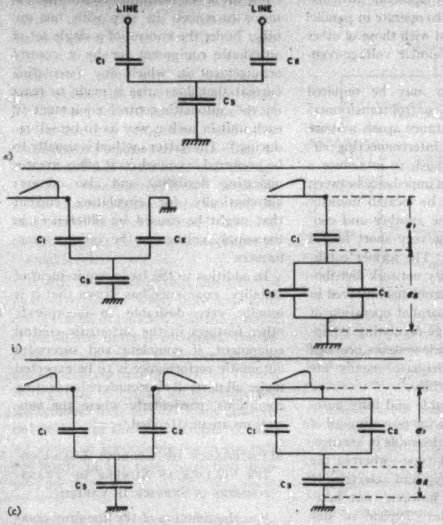

Figure 11. Equivalent circuit of transformer shown in Figure 4 subjected to impulse voltages

voltage bushings is shown in Figure 9. It should be noted that the functions of the mechanical clamping and the sealing of the bushing are independent from each other, thus relieving the seal of any mechanical stresses.

Use of Noninflammable Liquids

In view of the fact that the amount of insulating liquid required in this design is about one sixth to one eighth of the old (see Table I), these units can be insulated with noninflammable premium insulating liquids with very little extra expense.

Maximum Ratings of the New Transformers

The temperature rise of the new transformers is negligible when the units are operated at their normal rating of 500 volt-amperes secondary burden. The maximum rating, on the basis of 55 degrees centigrade with 30 degrees centigrade ambient is given in Table II.

Conclusion

Recent progress in potential transformer design has been primarily due to the development of a novel method of constructing and insulating the high-voltage winding. This novel winding also can be used advantageously with the wound-core construction and can be hermetically sealed without gaskets.

The new transformers are compact and light with accuracy equal to that of previous design. The compactness of these transformers permits the use of premium non-inflammable insulating liquids at little extra cost. The new transformers require little or no inspection and can be mounted in the bus structure near the high-voltage lines.

Appendix

The equivalent circuit of the coils shown in Figure 4, under Impulse Voltages, may be represented as shown in Figure 11a. There are two conditions to be examined:

(a). **Impulse wave reaching one line while the other end is grounded.**

(b). **Impulse wave reaching simultaneously from both lines.**

1. Impulse incoming from one line with the other end grounded. Referring to Figure 11b we have:

$$\frac{e_1}{e_2}=\frac{C_2+C_3}{C_1}$$

$$\frac{e_1}{e_1+e_2}=\frac{C_1+C_3}{2C_1+C_3}=p1=\text{fraction of the total voltage appearing across } C_1 \qquad (1)$$

If we assume (as is usually the case) that $C_3=2C_1$ then from equation 1 $p1=0.75$

2. Impulse incoming simultaneously from both lines. Referring to Figure 11c we have:

$$\frac{C_1+C_2}{C_3}=\frac{e_2}{e_1} \qquad \frac{C_3}{2C_1+C_3}=\frac{e_1}{e_1+e_2}=p1 \qquad (2)$$

If we assume $C_3=2C_1$, then $p1=0.5$

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A New Control System for Automatic Parallel Operation of Load-Ratio-Control Transformers

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Synopsis: The successful parallel operation of transformers equipped with automatic load-ratio control and line-drop compensation requires that stability of operation be insured. In addition, experience has shown that it is frequently highly desirable that other automatic features be included which will contribute toward operating flexibility, and which will permit switching transformers in and out of parallel operation without disturbing the voltage level at the load center or causing an excessive initial circulating current at the instant of paralleling.

This paper discusses the conditions which make these added features desirable, and presents a new control system whereby they all may readily be secured. In this new system, load current and circulating current are made to flow in separate paths in the control circuit, and each produce their effects independently, thereby insuring stable operation and maintaining the desired load-center voltage level. This separation of the load and circulating current, in conjunction with a modification in the closing circuit of the paralleling circuit breaker, also makes possible the avoidance of excessive initial circulating current when the breaker is closed. The simplicity and effectiveness of this arrangement in providing a fully automatic means for meeting all the anticipated operating requirements recommends its use in many applications.

THE economic trend in distribution-system arrangement toward locating individual substations at or near the center of each load area, plus increased attention to improved voltage regulation results in a growing number of relatively small substations.[1,2,3] These are frequently from 1,000 to 5,000 kva in size and are provided with means for automatically regulating the bus voltage. They are increasingly of the unit-substation type, in which the functions of three-phase voltage transformation, automatic voltage regulation, switching, metering, and protection are included in a co-ordinated and factory-assembled unit.[4,5,6] In such substations the voltage regulation is normally provided by transformers having built-in load-ratio-control mechanisms, capable of changing taps under load, and equipped with line-drop compensators for maintaining the desired voltage at the load center. Their load-ratio-control equipments are frequently called upon to operate in parallel with one another and with those of other substations having similar voltage-regulating equipment.

Parallel operation may be required where the load-ratio-control transformers are located some distance apart, and are joined together by interconnecting circuits of sufficient length to introduce a substantial amount of impedance between them; or they may be located immediately adjacent to one another and connected by circuits of very short length and low impedance. The former condition is typical of utility network distribution systems. The problems involved in securing successful parallel operation of the automatic voltage-regulating equipments in such network systems are well understood, and adequate means are available for solving them.[7]

A generally applicable and fully automatic control scheme for providing all of the features that are desirable in arrangements of the second type—wherein the units are physically and electrically close together—has, however, not heretofore been available. Typical of this class of application are those cases where rapidly growing local loads have necessitated the paralleling of load-ratio-control transformers in the same substation, or where two or more unit substations are installed side by side and interconnected by circuits of only a few feet in length. A similar condition exists in the case of all double-ended unit substations, in which two load-ratio-control transformers are made integral with a common switchgear section, and thereby connect directly to the same load bus.

The new control system presented here is particularly adapted to applications of this type, but the advantages which it offers may also be realized where the substations or load-ratio-control transformers are some distance apart, provided only that the distance is not beyond the feasible length of interconnecting control circuits. It permits the paralleling of any desired number of units, and yet retains full freedom for independent, nonparallel operation when required.

Operating Requirements

When two or more automatic load-ratio-control transformers with line-drop compensation are operated in parallel, the primary requirement of their automatic-control equipment is that it insure stability of operation.[8] Stability can be achieved automatically either by electrical or mechanical interconnections which cause all of the load-ratio-control mechanisms to remain in step with one another under the control of a single set of automatic equipment, or by a control arrangement in which any circulating current that does arise is made to react on the automatic-control equipment in each unit in such a way as to be self-reducing.[9] This latter method is usually to be preferred inasmuch as it offers greater operating flexibility and also corrects automatically for circulating current that might be caused by differences in the supply voltage to the various transformers.

In addition to the basic requirement of stability, experience has shown that it is usually very desirable to incorporate other features in the automatic control equipment, if complete and successful automatic performance is to be expected under all normally encountered operating conditions, particularly where the substations are unattended.

MAINTENANCE OF CORRECT LOAD-CENTER VOLTAGE AS NUMBER OF TRANSFORMERS IN SERVICE IS VARIED

It is the function of the line-drop compensators to indicate to the automatic regulating equipments the amount by which the bus voltage must be altered in order to maintain the load-center voltage at the desired level. This requires that the compensators receive a true indication of the magnitude of the load current. When the number of paralleled transformers is varied, as for example by removing one or more from service during periods of light load, special provision is necessary if the correct load-center volt-

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age level is to be preserved without requiring the attention of an operator. The need for such provision may be illustrated by the case of two load-ratio-control transformers operating in parallel and sharing a common remote load, as illustrated by the single-line diagram of Figure 1. With their line-drop compensators set to hold the desired load-center voltage, assume that one of the transformers is removed from service. The load current which was passing through it is thereby diverted through the other. This additional current reacts on the line-drop compensator and automatic-control devices of the transformer remaining in service to raise the bus voltage to a higher level than was being held while both units were in service. In other words, the removal of one transformer from service has the same effect on the line-drop compensator and automatic-control equipment of the other, as if the load had increased, and the bus voltage is raised accordingly. Since the actual line drop between the bus and the load center has not increased, however, the load-center voltage is raised above its normal level.

It will be apparent that this effect is most pronounced when only two units are operated in parallel. As the number of paralleled units is increased, the removal of any one from service produces proportionately less increase in current through the remaining units. However, the removal from service of any substantial portion of the total number of units will cause an appreciable rise in the voltage level; in any case, corrective measures are desirable.

Avoidance of Excessive Initial Circulating Current at the Instant of Paralleling

Unless means are included for bringing the load-ratio-control mechanism of an idle transformer to a position approximating the existing bus voltage level, there may be an excessive initial circulating current when the paralleling circuit breaker is closed. The magnitude of the initial circulating current is a function of the impedance in the loop circuit comprising the load-ratio-control transformers and the interconnecting circuits on both the high-voltage and low-voltage sides, and also of the voltage introduced in the loop by the difference in position of the voltage-regulating mechanisms. In the usual case, the predominant impedance is that of the transformers themselves. The high-voltage and the low-voltage interconnections usually add little impedance, particularly in the case of double-ended unit substations, where the transformers connect directly to a common load bus, or in the case of single-transformer unit substations joined in parallel through short interconnecting circuits. The units are sometimes connected to a common high-voltage circuit, although if each is supplied over a separate circuit, the total loop impedance is usually not increased materially, as the impedance of the high-voltage lines when reduced to a per-unit basis, is generally relatively small.

The maximum circulating current, of course, occurs when a unit whose voltage-regulating mechanism is at one extreme end of its range is paralleled with others whose mechanisms are at the opposite end. It is evident from this that the possibility of trouble arising from initial circulating current becomes greater as the range of the voltage-regulating equipment is increased. As an indication of the order of magnitude of the circulating current, consider as a typical case the two transformers illustrated in Figure 1. If each has a 20 per cent range of load-ratio control, they can introduce a maximum voltage of 20 per cent in the loop circuit when one is at the full raise position and the other at the full lower position. Then, assuming that each has an impedance of six per cent, and that the impedance of the interconnecting circuits is negligible, the maximum circulating current is 20/12, or 1.67 times normal full load current of one unit. This circulating current is essentially a zero power-factor current, and adds to the load current at an angle approaching 90 degrees.

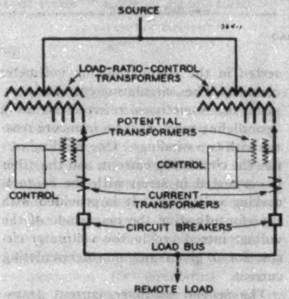

Figure 1. One-line diagram of three-phase load-ratio-control transformers connected in parallel

Automatic-control equipment energized from one phase of each transformer circuit

While excessive initial circulating current can be avoided if the voltage-regulating mechanisms are brought to the same voltage position manually, experience has shown that system operators are usually unwilling to accept this arrangement; and where remote operation or supervisory control over the paralleling circuit breaker is involved, the means for avoiding excessive initial circulating current must be fully automatic almost invariably.

In the new control system recently developed and described in this paper, great flexibility in operation is obtained by incorporating features that will:

(*a*). Insure stable operation during parallel operation.

(*b*). Maintain the correct load-center voltage as the number of transformers in service is varied.

(*c*). Avoid excessive initial circulating current at the instant of paralleling.

(*d*). Permit independent, nonparallel operation when desired.

The various voltages and currents referred to, unless otherwise specified, are those which appear in the secondary windings of potential and current transformers and which are measures of the true values in the power circuit.

The Control Equipment

Each automatic load-ratio-control transformer possesses and is controlled by its own complete set of control devices, including a contact-making voltmeter, line-drop compensator, potential transformer, and current transformer, permitting it, therefore, to operate as an individual unit.

For the purpose of reducing circulating current during parallel operation, each unit is provided with a circulating current compensator and an auxiliary current transformer. In addition, in order to maintain the correct load-center voltage level as the number of units operating in parallel is varied, and to permit the avoidance of an excessive initial circulating current, a second auxiliary current transformer is provided with each unit. Since these two auxiliary current transformers and the circulating current compensator operate in the secondary circuit of the main current transformer, they are physically so small that they may be mounted usually on the control panel. Only five external control wires are required for the interconnections between the units, regardless of the number of load-ratio-control transformers that are to operate in parallel.

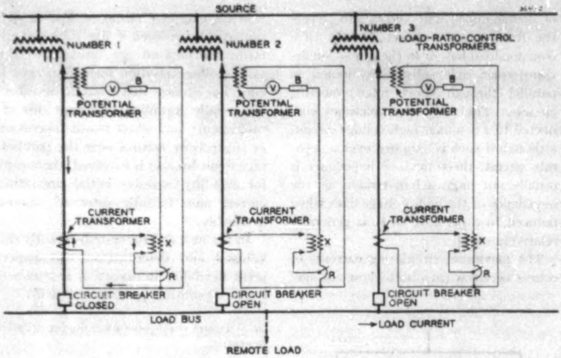

Figure 2. Control arrangement suitable only for individual operation

Principle of Operation

The connections of the control circuits between the several paralleled units, in conjunction with the appropriate connections of the circulating-current compensators and auxiliary current transformers in each unit are such that:

(a). Circulating current between transformers does not result in corresponding current in the line-drop compensators. This prevents the line-drop compensation from being influenced by circulating current. The line-drop compensators, therefore, respond only to load current.

(b). The load current in each line-drop compensator is always proportional to its transformer's share of the total load current, regardless of whether or not that particular transformer is in service. To accomplish this, the line-drop compensators on all units—whether in service or idle—are kept energized, and equal amounts of the load current flow through all of the compensators at all times.

In the case of transformers of unlike kilovolt-ampere ratings operating in parallel, the currents in the line-drop compensators will be equalized when the load current is divided among the transformers in proportion to their kilovolt-ampere ratings.

Functions of the Circulating-Current Compensator and Auxiliary Current Transformers

One auxiliary current transformer functions to separate the load current from the circulating current. The circulating current thus segregated is, by appropriate connections, forced through the circulating-current compensators of all units in service. A reactive voltage proportional to the circulating current is thereby inserted in the contact-making voltmeter circuit. The circulating-current compensator, sometimes referred to as a "paralleling reactor" is an iron-core reactor with two windings. One winding carries the circulating current, and the other is connected in series with the contact-making voltmeter. It is provided with taps for adjusting the magnitude of the voltage introduced in the voltmeter circuit for a given amount of circulating current.

The second auxiliary current transformer forces an equal division of load current through the line-drop compensators of all the units, regardless of whether they are in service or idle.

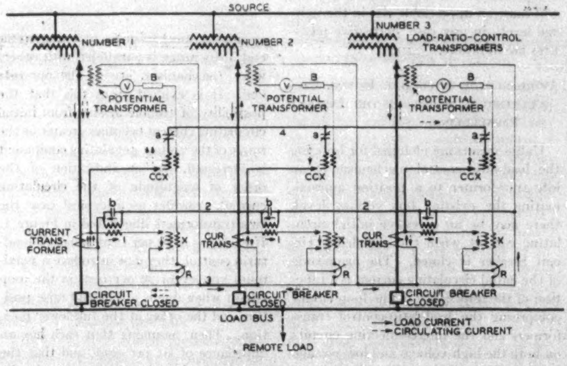

Figure 3. Control arrangement in which load current and circulating current are segregated

The load current indicated by solid arrows flows through line-drop compensators *R* and *X*, while the circulating current indicated by broken arrows flows through circulating-current compensators *CCX*

Operation of the Control Circuit

To facilitate an understanding of the manner of operation of the control system in which the foregoing features are incorporated, the relatively simple control arrangement for automatic operation of individual units is first considered. Figure 2 is a one-line diagram representing several three-phase load-ratio-control transformers, each with its own set of control equipment. The control circuits are separated from one another, and will be recognized as the arrangement normally used for independent, nonparallel operation, wherein each contact-making voltmeter and line-drop compensator is energized by its own potential and current transformer. With this arrangement, however, only one unit at a time may be connected to the load bus because, if one or more units are added in parallel the presence of any circulating current in the line-drop compensators will cause a further increase in the voltage delivered to the bus by the transformer whose voltage is already high, and a decrease in the voltage of the transformer whose voltage is already low. This results in unstable operation, with one unit going to the maximum raise position and the other going to the maximum lower position, thereby increasing the circulating current.

Circulating-Current Compensation

The circulating current, however, can for all practical purpose, be eliminated by inserting in the control circuit of Figure 3 the auxiliary current transformers 1, which separate the load current from the circulating current, and the circulating-current compensator *CCX*. In this figure the three transformers are shown connected in parallel to the load

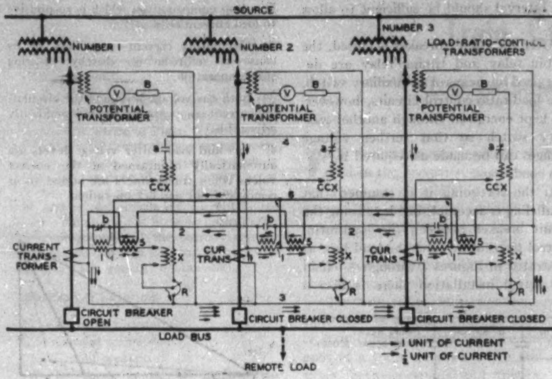

Figure 4. Control arrangement showing the addition of auxiliary current transformers 5 for effecting the proper division of load current among the line-drop compensators when transformers are removed from service

bus. Under balanced conditions, load current flows in the main current transformers and line-drop compensators, as well as in the auxiliary current transformers 1 as indicated by the solid arrows. Since these auxiliary current transformers are connected in series with each other through control wires 2 and 3, equal amounts of load current flow through all the line-drop compensators, thereby introducing the proper amount of voltage compensation in the contact-making voltmeter circuits.

Circulating current appearing in the secondary windings of the main current transformers, however, is prevented from flowing in the afore-mentioned loop by the polarity of the connections, but is forced to flow through the circulating-current compensators *CCX* and control circuits 3 and 4 as indicated by the broken arrows. It will be noted in Figure 3 that the flow of circulating current in the power circuits is from transformer number 1 toward the load bus, returning through transformers numbers 2 and 3. Accordingly, the current in *CCX* for transformer number 1 is in the direction to lower its output voltage, while in the *CCX* elements of transformers numbers 2 and 3 it is in the opposite direction and tends to raise the output voltage. The net effect is the reduction of the circulating current.

The vector relationship of the circulating-current compensation with respect to the voltage applied to the contact-making voltmeter is shown in Figure 6.

Auxiliary switches on the circuit breakers are used to change the control connections automatically when any transformer is removed from service. These are designated as *a* and *b* in Figure 3. When a transformer circuit breaker is opened, auxiliary switch *a* isolates the circulating-current compensator *CCX*, while switch *b* by-passes the auxiliary current transformer 1, thereby permitting the correct compensation for circulating current among the transformers remaining in parallel operation.

If for any reason the controls are prevented from correcting for circulating current, an overcurrent lockout relay (not shown in Figure 3) sometimes is provided to stop operation of the load-ratio-control motor before the current reaches an unsafe value. The relay operating coil is usually connected in series with the *CCX* element where it is responsive to circulating current only, and is, therefore, free from the influence of load current. When the power circuit breakers are equipped with current directional relays for other reasons, as is the normal practice in the case of unit substation, the lockout relay may be omitted. In these cases, the breaker is tripped automatically when the circulating current reaches the value corresponding to the relay setting.[10] The circulating current is thereby removed entirely rather than merely prevented from increasing further.

Equalization of Current in the Line-Drop Compensators

To permit varying the number of transformers in service without affecting the line-drop compensation, a previously mentioned second auxiliary current transformer 5 is added to each equipment, as shown in Figure 4. The manner in which this causes an equal division of load current among all the line-drop compensators will be apparent from the circuit of Figure 4, wherein transformer number 1 is removed from service as indicated by the open position of its circuit breaker.

If it be assumed that three units of current represents the total load current supplied to the load bus by transformers numbers 2 and 3, then each carries $1^1/_2$ units of current. It is apparent that if $1^1/_2$ units of current are allowed to flow through their line-drop compensators, these transformers will raise the bus voltage to a higher level than was being held before the breaker of number 1 transformer was opened, since previously only one unit of current flowed in each compensator. The presence of the auxiliary current transformers 5 however, permits only one unit of current to flow in each of these line-drop compensators, while the excess half units are forced to flow in control wire 6, and their sum—one unit—flows through the line-drop compensator of the idle transformer number 1. The line-drop compensation of all the transformers, therefore, remains at exactly the same value at all times, thereby maintaining the correct bus and load-center voltage levels regardless of the number of transformers in service.

Although in the figures used for illustration, only three transformer units are shown, the complete symmetry of the arrangements makes it apparent that this scheme is applicable to any number. If there are N transformers, X of which are operating in parallel, and if unit current flows in each when $X = N$, then the division of load current among the paralleled transformers will be in proposition to N/X. Only unit current, however, is allowed by the auxiliary current transformers 5 to flow in each of their line-drop compensators, and the excess, which is equal to $(N/X)-1$ or $(N-X)/X$ for each transformer, is diverted to the $N-X$ idle transformers. This results in a total current proportional to $[(N-X)/X](X)$ being divided among $N-X$ idle transformers with the result that each idle transformer also has unit current in its line-drop compensator.

Circuit-Breaker Closing Circuit

To avoid an excessive initial circulating current at the instant of paralleling, advantage is taken of the fact that the correct share of load current is automatically maintained in the line-drop compensators of the idle as well as the active transformers. This makes possible the automatic equalization of the output voltage with the existing bus voltage, so that when operating conditions require, any idle transformer may again be connected to the bus, with the assurance that its voltage will be of the correct value to permit paralleling without causing a large initial circulating current. A slight phase displacement will exist between voltages as a result of the impedance drop in the loaded transformers, but in the usual case this should not be objectionable.

Figure 5 is a partial elementary diagram showing the salient features of a circuit-breaker closing circuit which permits the automatic voltage equalization to take place before allowing the breaker to close. In this circuit, the only devices required in addition to those normally employed are a timing relay and an auxiliary seal-in relay. The manner of operation may be seen readily by reference to Figure 5. When the control switch is turned to closed, or when the reclosing relay (if present) operates, the seal-in relay is picked up and sealed. Immediately the load-ratio-control motor circuit is energized and the timing relay is started. This relay has a time interval which is sufficient to permit the load-ratio-control mechanism to reach the position corresponding to the existing bus voltage. A true indication of the bus voltage, as has been shown, is given to the contact making voltmeter of the idle unit by the potential transformer connected to its own output circuit, and its line-drop compensator. At the expiration of the timing interval, the circuit breaker closing mechanism is energized, and the breaker closes. While it is probable that in most cases the idle load-ratio-control mechanism will have assumed the correct voltage position before the interval elapses, there is the possibility that it may occasionally have to traverse its entire range before the correct position is reached, and the timing interval should be sufficient to allow this.

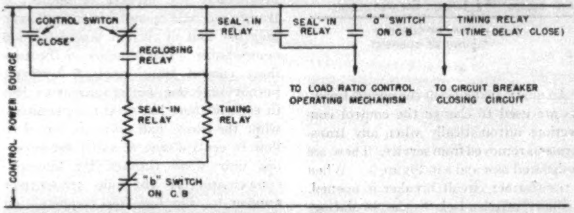

Figure 5. Partial elementary diagram of control circuit for permitting equalization of voltages before closing the circuit breaker

After the circuit breaker is closed, the seal-in relay and timing relay are de-energized by means of an auxiliary switch. The load-ratio-control circuits, however, are kept energized through another auxiliary switch so that further voltage changes can be made as required by system conditions.

In the foregoing it is assumed that paralleling is accomplished by closing the circuit breaker between the load-ratio-control transformer and the load bus, as illustrated in Figures 1 through 4. If in the actual installation there is also a switch or circuit breaker between the transformer and the source, this must be closed first in order that the automatic voltage equalization may occur.

Individual Operation

Since each load-ratio-control transformer is provided with a complete set of automatic-control equipment, any of the transformers shown in Figure 4 may be operated independently. It is necessary only to open control wires 4 and 6 and to by-pass the auxiliary current transformers 1 and 5, thereby reducing the interconnected control arrangement of Figure 4 to the simple isolated arrangement of Figure 2. For this purpose a control switch (not shown) may be used, or if the change from parallel to individual operation is to be made only very infrequently, the necessary changes in the control-circuit connections may be made directly at the terminal boards of the control equipment.

Summary

This new control system provides a means whereby all of the features may be secured which appear desirable in applications requiring the fully automatic parallel operation of load-ratio-control transformers. Its outstanding characteristics are these:

1. Accurately regulated voltage is maintained at the remote load center through line-drop compensation which is responsive to load current only.

2. Circulating current—regardless of its cause—is self-reducing, thereby insuring stable operation.

3. Both the voltage level and the circulating-current compensation are independently adjustable.

4. Bus and load-center voltage levels are automatically maintained at the correct values when transformers are added to or removed from parallel operation.

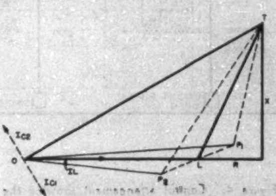

Figure 6. Vector diagram illustrating the corrective voltages impressed on the contact-making voltmeters as a result of circulating current

O-T—Voltage at transformer load bus

O-L—Voltage at remote load center and at each contact-making voltmeter for normal conditions. (No circulating current)

I_L—Load current, 100 per cent power factor

I_{C_1}—Circulating current through unit 1: lags *O-T* by an angle approaching 90 degrees

I_{C_2}—Circulating current through unit 2: leads *O-T* by an angle approaching 90 degrees

$O\text{-}P_1$—Voltage at contact-making voltmeter on unit 1 when circulating current exists

$O\text{-}P_2$—Voltage at contact-making voltmeter on unit 2 when circulating current exists

R-X—Resistance and reactance voltage components in line-drop compensator

T-L—Total line-drop compensation for normal conditions

$L\text{-}P_1$, $L\text{-}P_2$—Voltage compensation resulting from circulating current

$T\text{-}P_1$—Total impedance drop in series with contact-making voltmeter on unit 1

$T\text{-}P_2$—Total impedance drop in series with contact-making voltmeter on unit 2

5. In order to avoid the occurrence of a large initial circulating current, the output voltage of any idle transformer is automatically equalized with the existing bus voltage before permitting the closing of the paralleling circuit breaker.

6. Any number of load-ratio-control transformers may be operated in parallel, and new transformers may be added without requiring modification of the control equipment on those already in service.

7. Operating flexibility is inherent in this "unit" arrangement which permits either parallel or individual operation.

Appendix

Circulating-Current Voltage Compensation

The effect of circulating current on the voltage impressed on the contact-making voltmeters is shown in Figure 6 for the case of two load-ratio-control transformers operating in parallel. The vector diagram is drawn for the case of unity power-factor load current, and shows the conditions which exist before the circulating current is reduced. No attempt is made to indicate the precise magnitudes of the various components as the diagram is intended merely to illustrate their general relationship.

Definition of Symbols

PT—Potential transformer.
CT—Current transformer.
CB—Circuit breaker.
R—Resistance component in line-drop compensator.
X—Reactance component in line-drop compensator.
V—Contact-making voltmeter.
B—Ballast for contact-making voltmeter.
CCX—Circulating-current compensator.
a—Auxiliary switch on circuit breaker. Closed when circuit breaker is closed.
b—Auxiliary switch on circuit breaker Closed when circuit breaker is open.
$\neq$—Contacts closed.
$=$—Contacts open.

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Train Communication

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THE communication system that is used in the operation of railroads has developed very rapidly in the last few years. The most recent addition is what is known as train communication, which is a system of communication for operating purposes between railway vehicles and between such vehicles and fixed points. To illustrate, train communication equipment may be used for two-way telephone or code communication between the two ends of a train, between trains and stations, between trains and towers, between two parts of a train that are separated for work or otherwise, between two different trains whether they are on the same or on adjacent tracks, and between the conductor and locomotives in a receiving or in a classification yard. Following are illustrations of the uses to which such equipment may be put:

A. Communication Between Vehicles in the Same Train. This is most important in long freight trains in which it is very difficult to signal between members of the crew. There are many possible applications:

1. The conductor may call for an air-brake test before starting the train and may report to the engineer on the progress of the test, thus decreasing the amount of time required.

2. The conductor can notify the engineman that everyone is on board.

3. He can request the engineman to increase or decrease speed as desired or required.

4. The crews at the two ends of the train can compare train orders received while in motion.

5. Information in regard to change in work to be done at work points can be exchanged.

6. The conductor can give information enabling the engineman to spot the caboose at any desired point, for instance when it is necessary to clear road crossings or switches.

7. When in motion the conductor can report to the engineman dragging brakes, hot boxes, or any other condition that makes it necessary to stop the train, and arrange for the best time and place to stop.

8. The apparatus may be used to obtain the proper co-operation between the hauling and the pushing locomotives on a long train where two or more locomotives are cooperating.

9. The locomotive crew can give information to the crew at the rear of the train concerning causes for delay and estimates of the duration of the delay.

10. The conductor can give instructions to the engineman as to procedure under unusual conditions of any kind without having to walk to the front end of the train.

B. Communication Between Trains. When two trains are passing, the crew of one train may notice something that should be corrected on the other train, and with, the communication system it is possible to report such conditions, for instance, as the existence of hot boxes or dragging brake rigging or shifting loads.

C. Communication Between Train and Station or Between Train and Tower. Ability to communicate between trains and stations or towers is valuable first of all in that it enables the towerman to transmit messages to a train crew without stopping the train or reducing its speed. It is important also from the standpoint that it enables the train crew to report unusual road conditions to the towermen.

D. Communication in Hump Yards. In train classification the communication system is a very great convenience in that it enables the conductor, who is usually located at the hump of the yard, to communicate with his locomotives at all times and to tell them what to do, which string of cars to move, when to start, how fast to move, and when to stop. This is useful on the classification side of a hump, as well as on the receiving side.

The applications are so many and so important that one can almost say that in common with all communication equipment, as soon as the equipment becomes available, it also becomes indispensable.

Previously Proposed Methods

Several methods of providing train communication have been proposed. Among the first was the suggestion that the air-brake system might be used as a speaking tube. Investigation revealed that the brake air line was an extremely

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The material of the paper is the result of the work of a great number of past and present members of the research department of the Union Smith and Signal Company, especially past members Austin M. Cravath and John H. Findlay.

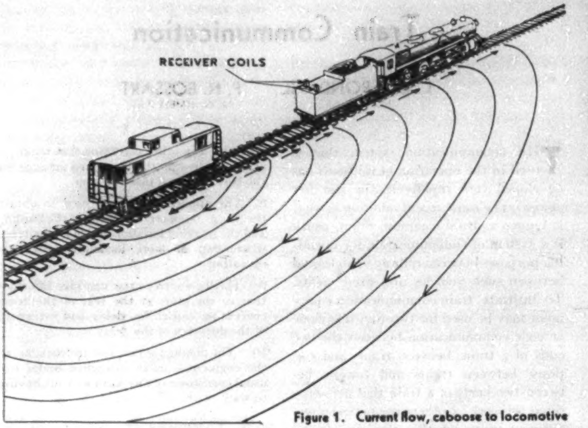

Figure 1. Current flow, caboose to locomotive

efficient low pass filter—so good, in fact, that no frequencies higher than about one cycle per minute could be passed through the brake pipe of a 150-car train. Even this signal could not be transmitted satisfactorily under certain conditions of the brake line, and the extremely low speed of signaling made the system impractical since a single signal would have to combine several pulses and might take five minutes to complete.

Another suggestion required the wiring of all freight cars. The wires were to be connected automatically through the brake line couplers, and there is no doubt that if such a circuit were established, communication over it would be easy. There is the consideration that the wiring of two million freight cars is a large undertaking, and even more important, that both this method and the signaling through the brake pipe have the fundamental limitation that neither can be used if the train is broken. This in itself renders both systems impractical, since communication is often most needed when the train is parted.

Radio has been proposed and tried on many occasions, and it is possible by radio to obtain communication, especially between ends of a moving train. Communication is uncertain in tunnels and fades under steel bridges and similar structures. This condition makes radio unsatisfactory for the purpose. The most important difficulty with radio is that the Federal Communications Commission has not been able to make permanent assignments of wave bands that are suitable. They have to be reserved for the use of services which have no alternative means of communication, such as ships and aircraft.

Commercial Method

The Union Train Communication System, a development of the Union Switch and Signal Company, is in regular commercial use and is being adopted rapidly in railway applications. It is a carrier telephone system transmitting usually the upper side band of a 5,700-cycle carrier and ordinarily feeding the signals conductively into the rails and picking them up inductively from the rails. Figure 1 shows the principle of the system. At the sending vehicle a voltage is produced along both rails in parallel to send out currents through the running rails with the return path through the ground. The signal is picked up at the receiving end by coils in inductive relation to the rails and is amplified and demodulated for reception in the loudspeaker. The apparatus has been developed as a simplex or "press-to-talk" system.

Description of Apparatus

Figure 2 shows the essential parts of a typical equipment. The transmitting circuit is a loop from the insulated truck at one end of the vehicle through the output transformer with tuning condenser to the truck at the other end of the vehicle with the loop completed through the running rails between the wheels of the trucks to which the connections are made. The impedance drop in these rails is the transmitting rail voltage. Speech is transmitted by a telephone-type transmitter and received either over a loud-speaker or over the receiver of a handset.

In Figure 3 two indication lights are shown at the top of the control panel, one showing red when the 32-volt d-c power is on, and the other flickering white with the modulation of the voice to assure the speaker that his message is being transmitted to the rails. The button beneath the lights is used for sending a calling signal which is received as a steady 1,050-cycle note and is sent as a single frequency 1,050 cycles above the carrier. The knob at the bottom of the panel is for the control of the received volume.

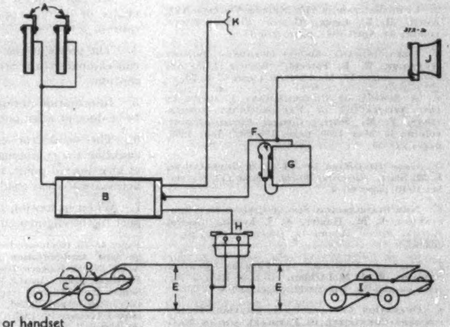

Figure 2. Block diagram of two-way equipment on caboose

A. Receiving coils (located in inductive relation to rails)
B. Equipment box with transmitter-receiver and dynamotor
C. Caboose rear truck (insulated from caboose frame)
D. Connections bolted to truck frame
E. One-inch copper pipe
F. Hand microphone or handset
G. Control panel
H. Output transformer unit
I. Caboose front truck (insulated from caboose frame)
J. Loud-speaker
K. 32 volts direct current from caboose battery: 100 watts receiving; 500 watts sending

In spite of the size of a locomotive, space in the locomotive cab is at a premium, and it is sometimes difficult to find a place for the speaker and the control box. The speaker may be located against the roof pointing down, and the control panel back of the engineman.

In Figure 4 the transmitter and receiver combined in one frame are at the left, and the 32-volt dynamotor, rated 500 milliamperes 400 volts d-c, combined with switching relay and ripple filters are in the frame at the right.

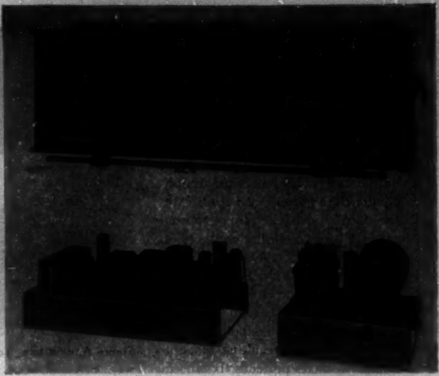

Figure 4. Open equipment box, showing at the top, frames in position in box, and below, frames when removed

An optional piece of equipment, sometimes used, is the "signal selector." It includes additional amplification and sharp filtering for the demodulated calling signal which operates a relay. It cannot be operated by short pulses or continuous wide band interference because of a combination of saturation of the tube, time delay in the relays, and narrow band of response. When this equipment is used for calling only, the loud-speaker is disconnected from the amplifier in the stand-by position, and the calling signal operates the bell. When used with signal lights for proceed and stop indications the loud-speaker is disconnected, and a green or proceed light shows as long as the calling signal is being received, but when the calling signal stops, the bell rings, a red light shows, and the loud-speaker is connected ready to receive speech. This provides a proceed indication on the so-called "closed circuit principle."

Figure 6 shows a portable receiving amplifier used on locomotives in hump yards for one-way communication. It weighs about 50 pounds. The circuits are essentially the same as those of the receiver for two-way communication, a 32-volt vibrator furnishing the plate voltage. In addition to the portable box shown, engine equipment for one-way communication involves a portable coil for picking up the signal from the rails and a portable loud-speaker and attached volume control for receiving messages in the engine cab. This equipment can be changed from one engine to another in a few minutes.

Equipment Circuits

Figure 7 shows the circuit diagram. The signal is picked up by the receiving coils at top left, amplified, as a single side band through two stages of carrier frequency amplification, and then demodulated by a pair of triodes. Some of the carrier frequency from the oscillator is fed into the demodulator tubes in parallel while the single side band is applied in push–pull. Any undesired high-frequency components are removed by the carrier suppression filter which follows. The output of this filter is applied to the grid of a power tube, and its output feeds the loud-speaker or a handset.

Automatic volume control is provided by the tube at the extreme left where the right half of the twin triode amplifies the demodulated signal taken from the filter input. When full output is reached by the output tube, the peak of this amplified voltage begins to exceed the positive voltage provided from *D* to ground, and the volume control starts to act by allowing the left half of the tube to rectify and charge the *RC* circuit connected so as to apply negative bias to the grids of the two carrier frequency amplifiers. The *RC* circuit provides considerable time delay. This is necessary since, with no carrier, transmitted volume control must be obtained from a time average of the amplitudes of the speech frequencies. Once the volume control begins to act, the output from the last tube stays ap-

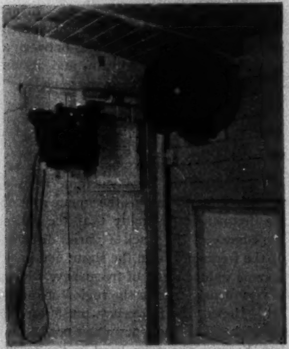

Figure 3. Control box with hand microphone and loud-speaker in caboose

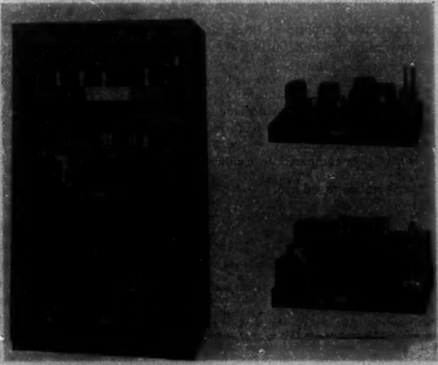

Figure 5. Cabinet for wayside station with receiver on top shelf, transmitter on middle shelf, and power supply at bottom, arranged for 110-volt 60-cycle supply

proximately constant, and the desired level in the loud-speaker or handset is set by a manual potential divider or volume control across the output.

Short high interference peaks are cut off by the peak limiter at an amplitude no higher than required for normal signals to load the output tube. This is a twin triode connected to short-circuit at the filter output the two halves of any amplitude greater than normal, and it operates after the *AVC* adjusts the gain. Normal peaks are set by two sliding contacts on the cathode resistor of the output tube.

In the transmitter the output of a telephone-type microphone is applied to the grids of the modulator stage in push-pull while the carrier energy is applied to the grids in parallel. The resulting modulation, if the circuit is perfectly balanced, does not contain the carrier but does have in it principally the two side bands. Any remaining carrier and the lower side band are removed by the filter which follows, so that practically the tubes which drive the power stage are energized by the upper side band only. The output stage consists of four 6*L*6 tubes in parallel push-pull and is connected to the sending loop or direct coupling circuit shown in the figure which extends from the engine pilot truck wheels through the rails to the drivers and back through the engine frame.

Transmission Through Track

It is interesting to examine the transmitting characteristics of a railroad track with ground return. The characteristics of any infinite transmission line with distributed constants are determined by four fundamental constants, namely:

R, the series resistance per unit length
X, the series reactance per unit length
G, the shunt conductance per unit length
B, the shunt susceptance per unit length

Approximate values of these quantities for two rails in parallel at 8,000 cycles per second are as follows:

$R = 1.25$ ohms per thousand feet
$X = 12.5$ ohms per thousand feet
$G = 1$ mho per thousand feet (for 3-ohm ballast resistance)*
B is negligible compared to conductance G.

Other symbols used are:

$Z = R + jX$—series impedance per unit length

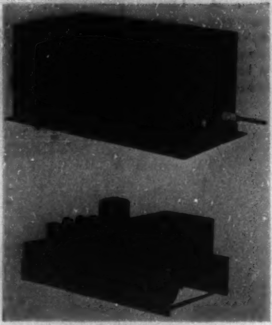

Figure 6. At top, portable receiver for one-way communication. Its chassis is shown removed, below

$Y = G + jB$—shunt admittance per unit length
Z_0 = characteristic impedance
$\gamma = \alpha + j\beta$ = transmission constant
α = attenuation constant
β = phase constant

We can apply the formula for the characteristic impedance of a transmission line

$$Z_0 = [(R + jX)/(G + jB)]^{1/2}$$

or we can write it as the series impedance and the shunt conductance

$$Z_0 = [\overline{Z\underline{/\theta}}/\overline{G\underline{/0}}]^{1/2}$$

As we substitute the values chosen we have

$$Z_0 = [12.5\underline{/84^\circ}/1\underline{/0^\circ}]^{1/2}$$
$$= 3.5\underline{/42^\circ} \text{ ohms}$$

This means that under the conditions assumed, the impedance presented by a railroad track with three ohms ballast resistance is about 3.5 ohms in one direction for an infinite track.

The transmission characteristics are obtained from the equation

$$\gamma = [\overline{Z\underline{/\theta}} \cdot \overline{G\underline{/0}}]^{1/2}$$
$$= [12.5\underline{/84^\circ} \cdot 1\underline{/0^\circ}]^{1/2}$$
$$= 3.5\underline{/42^\circ}$$

or

$$\alpha + j\beta = 2.6 + j2.35$$

Thus, if we start with a current I_0 of value 1 at the transmitter, the current will fall to the value $e^{-\alpha x}$ at distance from the transmitter equal to x in thousands of feet, or for the value given, the current will fall to half value in 270 feet. The phase shift is β radians per thousand feet and the wave length, $\lambda = 2\pi/\beta$, or distance for phase to be retarded a complete cycle, is in this case about 2,700 feet.

The equations show that if the conductance, G, is one fourth as great, corresponding to 12 ohms ballast resistance, the characteristic impedance Z_0 is twice as great, and the attenuation and phase shift are half as much. In this case,

$Z_0 = 7\underline{/42^\circ}$ ohms
$\lambda = 5{,}400$ feet
$x = 540$ feet (for $I = I_0/2$)

Range

The current levels involved in train communication are of interest. The current in the rails near the sending vehicle is of the order of $^1/_2$ to 1 ampere. The noise level in a quiet yard track is about 10^{-7} amperes. For communication to be intelligible, it is necessary that the signal should be received at a current level about four times that of the noise level. It follows that the ability to receive a signal with about one-half microampere in the rails represents the maximum usable sensitivity of the amplifier. On an ordinary main-line track of a railroad in a rural region, the noise level is about 10^{-5} amperes. On main-line track through an industrial region, the noise level may be about 10^{-4} amperes. This is measured by noting the noise current through the loud-speaker and then, with noise removed, adjusting and measuring the rail current of a 1,000-cycle modulated upper side band to give the same loud-speaker current. If we take these values and put them into our expression for track attenuation, we find that with 12 ohms ballast resistance, a signal can be received through the track alone, without line wires, at a distance of about $1^1/_2$ miles on main-line track in relatively quiet areas. With paralleling line wires present, speech can be exchanged at 100 miles between a wayside station and a train, and at ten miles or more between moving vehicles.

Choice of Carrier Frequency

If we look at the expression for the attenuation of the track current, we see that it varies directly as the square root of the frequency, assuming that the series impedance of the track Z varies directly as the frequency, and the shunt conductance is independent of frequency, which is very nearly true in the region of interest. Hence, the frequency must not be too high. The frequency must not be too low, because the noise level in the track is much greater for low frequencies. Experiments were made with voice frequen-

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* Ballast resistance, a common term in railway signaling, is the resistance between the two rails of a unit length of track, 1,000 feet if not otherwise specified. Measurements show the resistance of two rails in parallel to ground is approximately one-third instead of one-fourth the resistance from rail to rail. The reason for this is that some current, with a voltage between rails, flows along the ties and through the ballast without going to ground.

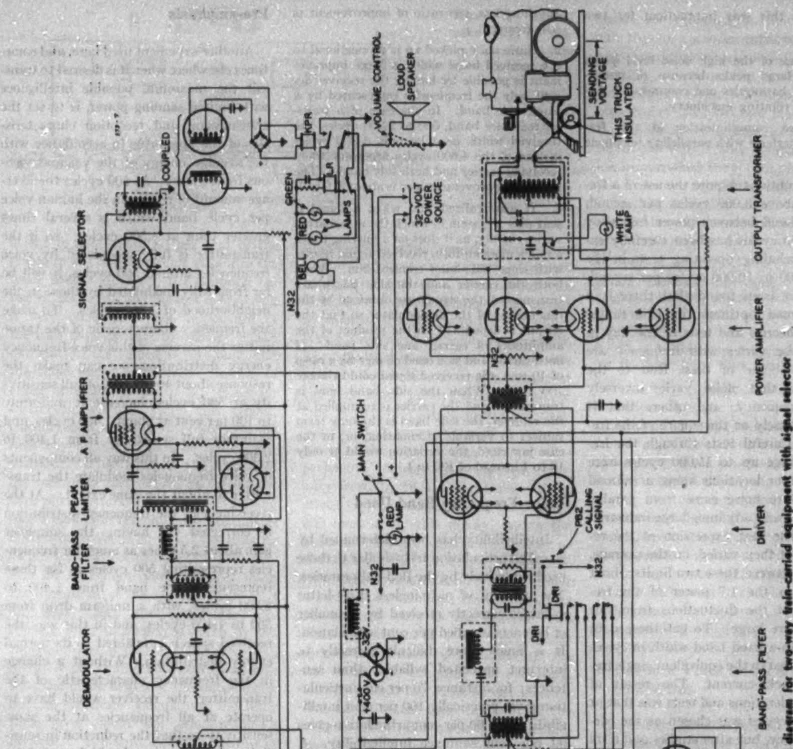

RECEIVER
TRANSMITTER
INDUCTIVELY COUPLED TO RAIL
DELAYED AVC
D
CARRIER FREQUENCY AMPLIFICATION
DEMODULATOR
BAND-PASS FILTER
PEAK LIMITER
A F AMPLIFIER
SIGNAL SELECTOR
COUPLED
KPR
VOLUME CONTROL
LOUD SPEAKER
GREEN
RED
BELL
LAMPS
LR
32-VOLT POWER SOURCE
N32
B+
PB1-PRESS TO TALK
DR
DR1
+400V
-32V
RED LAMP
MAIN SWITCH
PB2 CALLING SIGNAL
MICROPHONE
MODULATOR
OSCILLATOR
BAND-PASS FILTER
DRIVER
POWER AMPLIFIER
OUTPUT TRANSFORMER
WHITE LAMP
SENDING VOLTAGE
THIS TRUCK INSULATED

Figure 7. Circuit diagram for two-way train-carried equipment with signal selector

cies, but this was impractical for two reasons:

1. Because of the high noise level which contains large peaks because of power-frequency harmonics and commutator ripples from rotating machinery.

2. Because communication at this frequency interferes with paralleling telephone lines.

These conditions require the use of a frequency above 3,000 cycles per second, and the band between power frequency and 15,000 cycles has been carefully explored, including operating tests in the range 3,000 to 10,000 cycles per second, and it has been found that there is a rather broad optimum in this range. Various theories and tests of the way in which noise varies with frequency are available. Some of these lead to the conclusion that noise varies inversely as the frequency, and others that it varies inversely as the square of the frequency. Careful tests through the frequency range up to 15,000 cycles were made at four locations along a railroad where severe noise came from parallel power lines and adjoining large industrial plants. The best expression of the results is that they varied, on the average, at a rate between these two limits, about inversely as the 1.7 power of the frequency, but the fluctuations from this average were large. To get these data the noise in a fixed band width in cycles was compared to the equivalent single frequency track current. The result of these considerations and tests was that at first 7,000 cycles was chosen as the carrier frequency, but after studies and tests in which the Bell Laboratories co-operated this was later changed to 5,700 cycles at their suggestion. This choice is at the same time a good compromise between attenuation and noise level. In these bands there is no interference in either direction between our circuits and signal circuits or radio.

Single Side-Band Transmission

The decision to transmit only one side band with suppressed carrier was based on the following considerations:

1. The efficiency of the transmitter power unit is greater. The expression for the power transmitted with a carrier modulated to produce two side bands is $(K^2/4)+1+(K^2/4)$, where K is the modulation factor and 1 is the carrier power. This leads to a total power of 1.5 with only 0.5 in the two side bands, with 100 per cent modulation or $K=1$. By transmitting one side band only, all of the transmitted power may be put in it, and the efficiency of intelligence transmitted is improved by a factor of three for 100 per cent modulation; and for K anything less, the ratio of improvement is still greater.

2. Since noise picked up is proportional to the received band width, a large improvement is possible by having the receiver accept only the frequencies represented by a single side band. In the case of a transmitted voice band, 500 to 2,300 cycles, the received width need be only 1,800 cycles instead of the 4,600 cycles necessary when receiving carrier and both side bands. The factor of improvement here is about $2^1/_2$ to 1.

3. It was realized that when a signal is sent over a system in which the attenuation varies rapidly, as it does on a moving train, a much more uniform received signal results with single side-band transmission. When both the carrier and the side bands are transmitted, the signals are obtained by the functioning of the demodulator so that the output is dependent on the product of the amplitudes of carrier and side band. If both carrier and side band change by a ratio of 10 to 1, the received signal could change 100 to 1. When the side band only is transmitted and the carrier is resupplied at the receiver, the side band is the only term subject to variable attenuation, or, in the case just cited, the variation would be only 10 to 1 instead of 100 to 1.

Voice-Frequency Band Used

Intelligibility has been determined by so-called articulation tests similar to those used extensively by the Bell Laboratories. The per cent of meaningless three-letter syllables correctly received by a number of listeners is called per cent articulation. It is much more difficult correctly to interpret unrelated syllables than sentences; for instance, 70 per cent articulation gives practically 100 per cent intelligibility, and 60 per cent articulation gives 99 per cent sentence intelligibility. A study of the Bell Laboratory data indicated that we could exclude the frequencies below 500 cycles and those above 2,300 cycles without serious loss in sentence intelligibility. As a result of many tests made under severe noise conditions, this band 1,800 cycles wide, that is, from about 500 cycles to 2,300 cycles, was chosen as being best for our purposes, including naturalness of reproduction, which is the chief gain from including frequencies below 800 cycles.

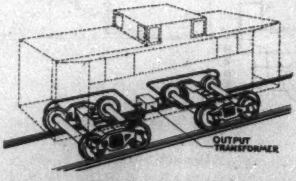


Figure 8. Phantom view showing direct-coupling output circuit on caboose

Pre-emphasis

Another expedient used here, and sometimes elsewhere when it is desired to transmit the maximum possible intelligence with limited sending power, is to set the transmission and reception characteristics of the apparatus in accordance with the average energy of the voice at various frequencies. At 500 cycles the average amount of energy in the human voice per cycle band width is several times greater than at 1,500 cycles; so if the transmitter is fully modulated by voice frequencies around 500 cycles, it will be far from fully modulated by those in the neighborhood of 1,500 cycles. To make the frequency characteristic of the transmitter the inverse of this voice-frequency energy distribution, we can make the response about 40 per cent of full sensitivity at 500 cycles, increase it uniformly to 100 per cent at about 1,400 cycles, and maintain full sensitivity from 1,400 to 2,300 cycles. In this way all components of voice frequencies modulate the transmitter to about the same extent. At the receiving end the frequency distribution is corrected by having the amplifier gain about 2.5 times as much for frequencies representing 500 cycles as for those representing the band from 1,400 to 2,300 cycles, with a uniform drop from 500 to 1,400 cycles, and in this way the received speech is restored to its normal energy distribution. Without a change in the frequency characteristic of the transmitter, the receiver would have to operate at all frequencies at the same sensitivity so that the reduction in sensitivity of the receiver for higher modulation frequencies represents a reduction in the noise level picked up in this band without any loss in reception of the signal desired. With the values chosen this represents about 2 to 1 improvement in the signal noise ratio of the received signal.

Coupling to Track for Transmission

Originally the coupling between the transmitter and the rails was made by means of large iron-core coils carried six or eight inches above the rails, but it was found possible to transfer only about one half of one per cent of the power in the coils into power in the track. The so-called direct coupling circuit of Figure 8 is 15 or 20 times more efficient. Figure 9 gives typical measured impedance values for such a circuit applied to a caboose. The large copper pipe used for the conductor has a resistance of 0.01 ohm, whereas the rail resistance is 0.04 ohm, so in this respect the circuit is fairly ef-

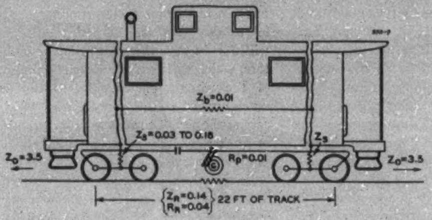


Figure 9. Impedance values for output circuit on caboose

ficient in that about 80 per cent of the power in the output circuit goes into the track itself. However, the coupling between this circuit and the rest of the track is very inefficient. The impedance of the rails from contact to contact on the two ends of the vehicle is 0.14 ohm, and the impedance of the track looking in either direction is about 3.5 ohms for three-ohm ballast. Thus, this circuit, considered as a source of power into the rails beyond the vehicle, is badly mismatched. If we consider the section of the rail under the caboose as the impedance of the source 0.14 ohm, it is feeding into a load of seven ohms, or the impedance ratio is about 50 to 1. Considering as 100 per cent the efficiency of transmission for maximum power transfer when the impedance of the source and the load are equal, then the approximate efficiency for a mismatch is given by the expression $4n/(n+1)^2$ and this, if $n = 50$, gives 7.7 per cent. This expression neglects the phase angles of the impedances, but an accurate calculation including these gives about the same result—8 per cent.[1] It is thus seen that because of the mismatch of impedances we are losing about 92 per cent of the power which we would like to put into the track beyond the vehicle. In case of sending from a wayside station, it is possible to make this factor better than 100 per cent, for we can connect directly between rails and ground, balance out reactance, and, for three-ohm ballast resistance, make our source resistance about 1.30 ohms, which is the real part of the characteristic impedance to ground for the track extending in the two directions in parallel.

Inspection of the output circuit diagram, Figure 9, reveals that there is a shunt path through the vehicle tending to short out the rail which, in the average case shown, might have a value of about 0.2 ohm. This, in parallel with the rail impedance of 0.14 ohm, is not extremely serious and in the case of a light vehicle, such as a caboose, gives fairly satisfactory communication without insulating the trucks. However, the shunt impedance is quite variable, and in the case of the lowest values shown, which are measured values on a steel caboose, an impedance of only 0.07 ohm would be put in shunt with 0.14 ohm in the rail, and a severe lowering of efficiency would result. This situation is very much worse in the case of a locomotive or tender because of the very much greater weight involved. One theory of contact resistance[2] indicates that it varies inversely as the $^1/_3$ power of the force on the contacts. Experiments indicate the exponent may vary from $^1/_3$ to 1. A caboose weighs about 20 tons, and a tender may weigh 200 tons, so by this theory, taking the contact resistance variation inversely as the square root of the weight, the contact resistances through the body of the tender would be less than one third as great as through the body of the caboose, and these low values in shunt would cause serious impairment of transmission. In the trains now equipped and operating, trucks in both the caboose and locomotive are insulated from the body of the vehicle. The pilot truck of the locomotive is usually chosen for insulation, and the front coupler is also insulated to prevent undesirable shunt paths.

Effect of Transmitting Loop Position

At first it was thought that by having the copper pipe of the loop high, a better circuit would result, principally because by getting the pipe further away from the rail the self-inductance of the rail would be increased, and a given current would give considerably more rail voltage effective for transmission. This is probably true for a wooden caboose, but it has been determined experimentally that it is certainly not true for a steel vehicle which covers almost all the newer design present-day cabooses and tenders. The characteristics of a high loop over the top of the vehicle were compared with a low loop in which the copper pipe is placed just under the floor of the vehicle, and the somewhat surprising result was found that the low loop is more efficient. There are two reasons for this:

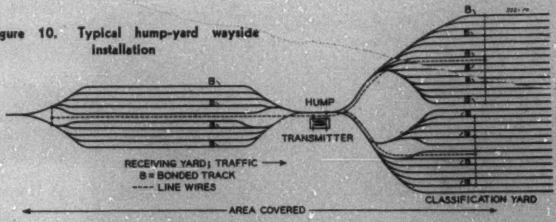
1. The presence of the steel body practically eliminates any gain from added height, so that the actual effective height of the high loop is almost the same as that of the low loop.
2. Considerably more copper pipe is needed for the high loop, thus increasing copper loss.

The effective height of the high loop was determined by five separate measurements made in different ways. These all led to the same conclusion that the effective height of the high loop was almost exactly the same as the clear air space between it and the rails. In other words, a loop about 14 feet high functioned just the same as one about four feet high, which represents the amount of clear space from ground to the floor of the vehicle, plus the distance from the caboose roof to the high loop.

Relation of Track and Parallel Line Wires

In the system of communication under discussion, the signal is fed into the track and is picked up from the track, usually with assistance in the transmission by mutual induction to and from the paralleling line wires which are nearly always to be found along the right of way. Measurement and calculation show that the induction into the line wire is principally from the track and not from our sending loop. This is not surprising when one takes into account that the length of exposure between the track and the line wires is much greater than that between the sending loop and line wire. It can be demon-

Figure 10. Typical hump-yard wayside installation



strated that the voltage induced in a line wire from a voltage introduced in a track is independent of the ballast resistance of the track, and hence of wet or dry seasons. This can be seen qualitatively by considering that if the ballast resistance is low, larger track currents flow, but they attenuate more rapidly. Analysis shows that the two effects just balance one another. In order to receive the strongest signal, however, experiment shows that it is necessary to orient the coils for maximum coupling with the rails rather than with the line wires.

When sent from a wayside station, the signal is ordinarily applied between a line wire and a track as a ground connection, rather than between tracks, although the latter is done sometimes. With such use of a line wire, conversation between a tower and a moving train 100 miles distant is practical.

Yard Communication

An important application of train communication is found in yards where it is desired to direct the work of several locomotives from one office. Some yards use one-way equipment, office to engine only, while others use two-way equipment which permits the engineman to talk to the office. A typical railroad hump yard is shown in Figure 10. Here communication is desired between the hump and locomotives working any place in the receiving yard and past the entering switches of the classification yard. In order to furnish this communication, line wires are provided as shown dotted in the figure. These line wires may be on pole lines if these are present in suitable location, or clearances permit building new ones. It is generally best not to have these wires immediately adjacent to open-wire telephone pairs unless special transpositions are used, but pole lines carrying power, telegraph, or signal wires may be used. Often a buried single conductor Parkway cable with nonmetallic sheath is used. Lines are supplied so that no point in the area to be covered is more than ten tracks away from a line wire for one-way communication, or more than seven tracks away for two-way communication.

One-way yard communication depends on rail currents which flow because of conductive connection to or induction from the line wire. Hence, to provide positive rail paths, one rail of each third track is bonded so that a locomotive will always have rail current flowing in its own or in the immediately adjacent track, which is near enough for satisfactory inductive pick-up of the signal. For two-way communication, one rail of each track is bonded to provide positive rail paths for the locomotive sending currents. When trains operate on well maintained main-line track, it is not necessary to bond, but bad electrical connections in rail joints are common in yard tracks.

The sending voltage at a yard office or wayside station is connected between the line wire and ground. Any bonded track of considerable length may be used for a ground. At the limits of the area covered, the line wires are grounded to the bonded tracks. Direct connections between line wires and tracks are not necessary, since mutual induction between line and track will supply communication, but the connection line to rail is usually the most convenient way to terminate both line and bonded tracks at the coverage limits.

Approximate calculations can be made of the communication currents in such railroad yards. A primary factor is the mutual induction between a line wire and a track, and this may be determined from published charts.[3]

There are several other effects to be taken into account, one of which is the loss occurring when the signal is fed into one wire in a group of wires running parallel to one another. Opposing currents will flow in the other paralleling conductors, which will tend to reduce the effect of the wire employed. Two-thirds as much current as flows in the wire which carries the signal current may be induced in an immediately adjacent wire, and a rough calculation indicates that the net induction from the transmitting current in a single wire which is a member of an open wire lead can be estimated by dividing the calculated value for a single wire alone by three times the square root of the number of additional paralleling wires. Likewise, the currents induced into any one track are reduced by mutual induction from the currents flowing in adjoining tracks. A study of this effect indicates that current calculated in any one track considered alone must be divided by the square root of the number of tracks between it and the line to approximate the actual current.

In considering transmission in the opposite direction, the effect of interfering tracks is the same; that is, they interfere with transmission from a track to a line in about the same way. However, multiple wires on a pole line are an aid in this case because the currents in each are approximately inphase and they add in effect on a receiving means able to pick up from all the wires equally well. The results obtained with these methods of calculation agree with the results of some field measurements within ±30 per cent.

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Thermal Rating of Overhead Line Wire

MYRON ZUCKER
MEMBER AIEE

THE major problems in rating overhead wires according to thermal limits are

1. How materials lose their strength.
2. How hot wires become when various currents are passed through them.

Much scattered work has been done on these subjects in the last 20 years, but results have been either inconclusive or, in many cases, contradictory. This discussion brings together data from many sources, adds certain data, and outlines the general problem.

Omitting the obvious discussion of efficiency and voltage drop, let us say that the rating of overhead wires must be such that there shall be no appreciable weakening of the conductor material and clearances at the higher temperatures of operation must be ample. To solve this general problem, we must obtain data on the following specific phase:

I. PERMISSIBLE TEMPERATURES

(*a*). To retain the strength of wire. This depends on time of heating, impurities in the wire, and to a less degree on amount of cold working prior to heating.

(*b*). To protect wire covering from undue deterioration.

(*c*). To preserve reasonable clearances.

II. TEMPERATURE THAT THE WIRE WILL ATTAIN

(*a*). Basic thermal relations expressed as temperature rise versus current, wire size and material, wind at various directions, and sunshine.

(*b*). Weather conditions under which heating will occur, finding reasonable values of wind, temperature, and rain combinations under normal and emergency operation.

Importance of Frequency and Duration of Overload

The total expected duration of heavy loads affects permissible temperatures, acceptable minimum ground clearances, and probable adverse weather combinations. Therefore, any thermal limits must be made with reference to specified frequency and duration of overload.

I. Permissible Temperatures

I*a*. MAXIMUM TEMPERATURE THAT WILL NOT ANNEAL WIRE

We will confine our metallurgical study to copper. The cold-worked copper used in overhead lines has its crystals so arranged that overheating will start new crystal growth that weakens the wire. Some X-ray diffraction tests (of which details will follow) indicate that the beginning of recrystallization coincides with a loss of five per cent of the tensile strength of the wire. We therefore adopt this as the allowable loss of strength by annealing. The problem is then to express temperature and time that will produce five per cent loss of tensile strength.

The solution cannot be found directly in the literature, because few of the 37 authorities[1–37] consulted give results in directly usable form. However, by correlating some curves (such as Figure 1) and many isolated points, the field can be roughly covered. Figure 1 is a sample of tests in which many pieces of one wire were heated for various times and then broken in tension after cooling. From these curves, the time-temperature combinations for five per cent decrease in strength can be found. The curves presented by different authorities show considerable variation, but results can be reconciled reasonably well by considering the chemical impurities. Fortunately, in this type of copper only oxygen and silver remain in important quantities.

Figure 2 shows the permissible temperatures for one hour. Some of the points are from curves such as Figure 1, but most are from isolated test runs of 1/2 to 2 hours. The latter were corrected to one hour by comparison with other investigators' strength-time curves. Some points are from hardness or elongation tests, corrected to equivalent breaking-strength results from interlocking reports. (A loss of one per cent in Vickers, Brinell, and scleroscope hardness corresponds to about five per cent loss of tensile strength, but Rockwell tests lagged about 50 degrees centigrade in showing the change.)

The trends of the points in Figure 2 are indicated by the labeled areas. From these we see that permissible temperatures increase with silver content, with the most minute parts of silver having greatest effect. We also see that oxygen is only important when below 0.02 per cent.

Typical compositions of wire copper are indicated, but the range of individual coppers is great. "Lake" copper, common before 1924 but since then used only on mile-long seamless draws, varies naturally as mined. The amount of silver in electrolytic copper varies with the value of silver. Oxygen-free copper is not commonly used for conductors but shows what can be accomplished by fine chemical controls.

We now turn to curves of temperature versus time (Figure 3). Considering the wide range of conditions represented by these authors, the agreement in trends is good. The shape of these curves, plus some isolated tests, were used therefore to draw a chart for four values of silver (Figure 4), whose intercepts at one hour were selected from Figure 2. From these curves, if silver is known, temperature may be estimated for any time of heating. If silver is unknown, tests may be run at 200 degrees centigrade for periods varying from one-half hour to a day (that is, data obtained as in Figure 1), and time at which the wire loses five per cent of its strength spotted on Figure 4. The nearest curve can then be taken to represent the action of this material. Such tests substitute for chemical analyses.

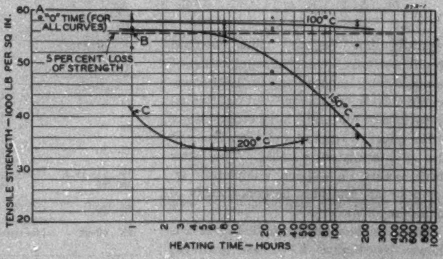

Figure 1. Cold breaking strength in tension versus time of application of several temperatures

For number 6 strands from number 000 copper wire after some years service

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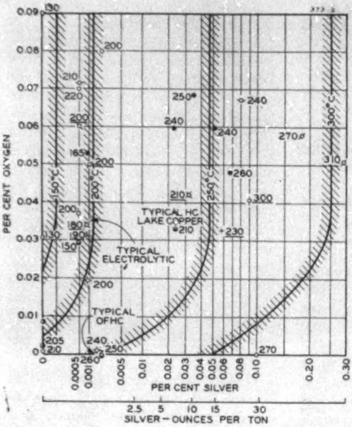


Figure 2. Influence of oxygen and silver on temperatures that will cause five per cent decrease in copper strength if applied one hour

Each symbol represents a different authority

Underlined values are estimated

Metallurgists will note the absence of history of the metal prior to cold-working, and degree of cold working, from this graph. The effects are relatively minor for the strengths, times, and temperatures involved in this problem.

The results are admittedly crude. If the problem continues to be important, we might suggest that outdoor spans be set up, heated as desired, and tested regularly by X-ray diffraction until recrystallization begins. This method would not only avoid the duplication of samples that is necessary in destructive test methods to average out individual variations in material, but permits study of the effect of tension and vibration and also of intermittent heating. The latter three points have been completely ignored so far, and may be conceivably of major importance.

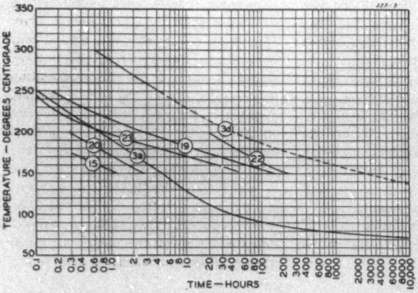


Figure 3. Temperature to begin weakening of copper wire held continuously in oven under no stress

Beginning of weakening shown by one per cent decrease in hardness or five per cent decrease in tensilestrength(cold)

	Copper			
Authority	Initial Strength	Silver (Per Cent)	Oxygen (Per Cent)	Test Method
3*a*	57–59	0.001	0.009	Vickers, tensile
3*d*	50±	0.0017	0.001	Vickers numbers
15	36	Not stated		Tensile strength
19	51, 55, 57	0.0005	0.07	Scleroscope
20	59	Not stated		Tensile strength
22	70	0.035– 0.069	0.05 0.065	Tensile strength
23	42–62	Not stated	0.066	Tensile strength

The sensitiveness of the X-ray test is shown by Figure 5. These patterns were made by D. McCutcheon of the Ford Motor Company, at the author's instance. Figure 5*A* shows an unannealed specimen; Figure 5*B* shows a piece heated for an hour at 150 degrees centigrade; and Figure 5*C* shows a piece heated for an hour at 200 degrees centigrade. The black spots in Figure 5*C* are from recrystallization. The specimens when broken in tension (points are shown by *A*, *B*, and *C* on Figure 1) showed 58.3, 56.5, and 40.5 thousand pounds per square inch strengths.

Figure 5*D* is from another strand in the same wire, subjected to the same treatment as Figure 5*C*. Only a moderate number of new crystals appear, and the strength was about 50 thousand pounds per square inch. This strand had more silver than its mates.

To round out the subject of wire strength, we may add that there is no evidence that nicks or burns too small to call for replacement under normal good practice impose any lower limit; tests have shown that modern joints are equal to their proper wires in carrying capacity.

I*b*. WIRE COVERING

The dripping of compound and loss of dielectric may impose limits on the heating of covered wires.[39] No results are yet available that include the time element. Recent tests on *URC* covering indicate that it may safely be taken to 100 degrees centigrade for ten hours.

I*c*. CLEARANCES

To operate lines at high temperatures, the increased sag should be computed and the line surveyed to see whether that much loss of clearance can be spared, considering the expected frequency of heavy electric loading. As an example, sags after heavy ice-and-wind loading of a number 000 copper line with 600–800 foot spans is about 2.8 feet more at 210 degrees Fahrenheit than at 120 degrees Fahrenheit and 4.5 feet more at 270 degrees than at 120 degrees.

II. Temperature That the Wire Will Attain

II*a*. WIRE HEATING

The studies of Schurig,[40] Luke,[41] and others,[42–52] have given a reasonably good basis for predicting temperature rise of wires under a limited range of temperature rise and wind speeds. Further tests over wider ranges, and including various wind dirctions, were made by the Detroit Edison Company last spring, when decisions were required on the rating of certain transmission lines. Figure 6 shows a typical set of data. The curves are self-explanatory.

According to these later tests, when there is no wind the temperature rise depends on the 1.9 power of current (running from 1.8 to 2.0 for different wire sizes). As the temperature rose, the local air current induced by heating increased until at about 150 degrees centigrade rise the vertical air movement

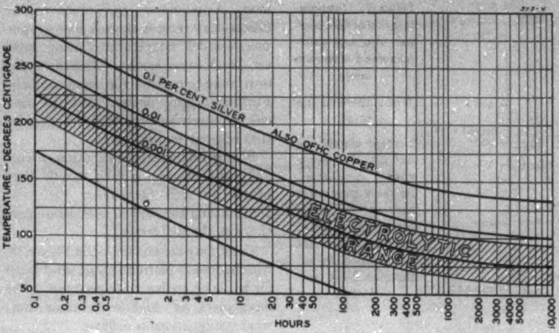


Figure 4. Temperature versus time to reduce strength of copper wire by five per cent

For various silver contents when oxygen is 0.02 per cent or more. The curve for 0.1 per cent silver also may be used for typical oxygen-free high-conductivity copper

was one half mile per hour. This circulation was more effective, per unit speed, in carrying away heat than equal real wind speeds. This accounts for the difference in slope between the no wind and wind curves.

Wind, even at low speeds, was found to decrease temperatures materially. The temperature varies as the 2.3 power of current. The direction of wind is now of prime importance; when parallel to the wire, a 2.4 mile per hour wind permits temperature to rise 205 degrees centigrade compared with 180 degrees for wind at right angles to the wire.

From data available at the time, the nomograms of Figures 7 and 8 were drawn. Some inconsistencies are being checked by further tests, but the charts are believed reasonably accurate.

It will be noted that sunshine has had no place on these charts. At the elevated temperatures, the influence is minute. More important, we should note that wind is a more critical factor than is a variation of temperature in the range of ambients ordinarily encountered. For instance, the following combinations will heat number 0000 bare copper wire to 100 degrees centigrade:

Ambient Temperature (Deg C)	Wind Velocity (Miles Per Hour) 0	1	2	5	
10	400	540	570	650	amperes
30	350	480	510	600	

The temperatures listed, change the current about 12 per cent, whereas one mile per hour of wind increases the current about one third over the calm.

II*b*. Weather

Weather is really a corollary to part II*a*, but it takes so much effort as to deserve separate treatment. Because of the present shortage of copper, we cannot

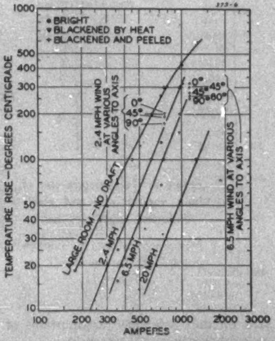
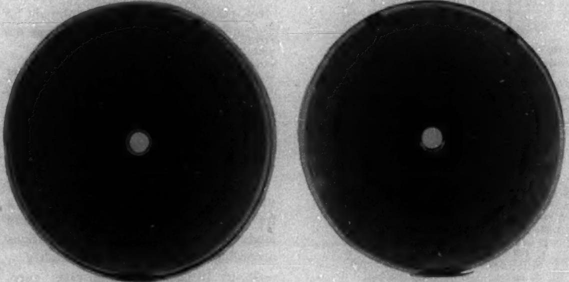
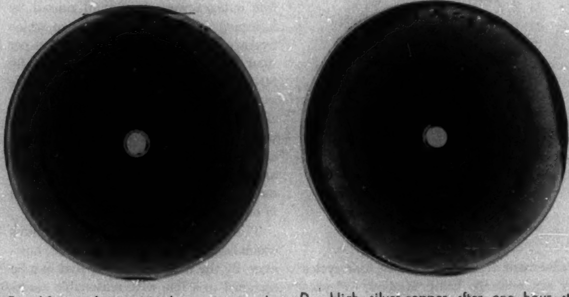


Figure 6. Temperature rise versus current

In number 000 bare stranded copper wire for various wind and surface conditions



A. No heating *B.* After one hour at 150 degrees centigrade



C. After one hour at 200 degrees centigrade *D.* High silver-copper after one hour at 200 degrees centigrade

Figure 5. X-ray diffraction patterns for copper wire

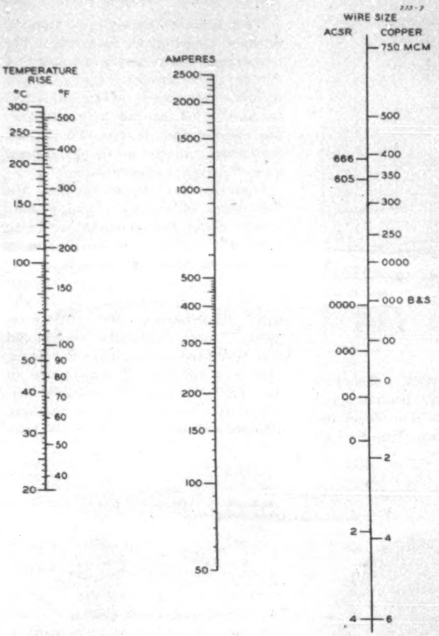


Figure 7. Temperature rise of bare copper wire and steel-reinforced aluminum cable

When there is no wind

Increase current five per cent for blackened conductors. Decrease current five per cent for bright conductor. Increase current 10 per cent for steel-reinforced aluminum cable conductors

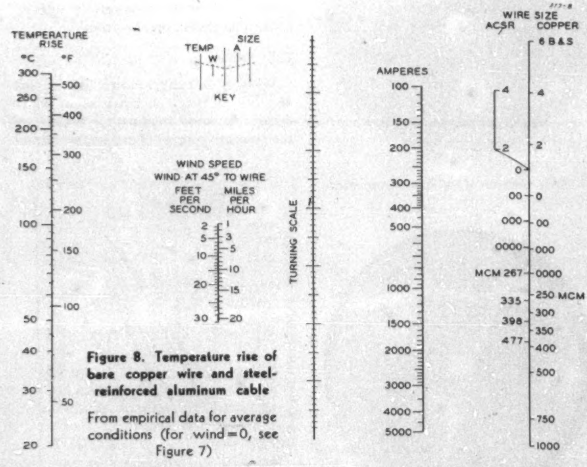


Figure 8. Temperature rise of bare copper wire and steel-reinforced aluminum cable

From empirical data for average conditions (for wind = 0, see Figure 7)

work on the "safe side" assumptions of most adverse weather conditions.

The procedure in finding reasonable combinations of wind and temperature to use in Figures 7 and 8 depends on the refinement desired, and on the load cycles or emergencies being studied. Figures 9, 10, and 11 show one form in which data may be presented for normal rating of a circuit that has continuous daytime load. A preliminary study of the weather observatory records showed that in summer, besides temperatures being highest, winds were lowest. Therefore, records were compiled for summer only. Further scrutiny showed that the coincidence of rain and low winds was so small that rain can be neglected.

Figure 9 shows the distribution of wind and temperature for each month from March through September, 1940 and 1941, during the normal peak-load hours of 8 a.m. through 5 p.m.

These data are condensed in Figure 10, with frequency plotted against wind speed for winds up to five miles per hour. The meagerness of high-temperature low-wind combinations is evident.

Finally, Figure 11, showing frequency of various winds, regardless of temperature, gives a straightforward curve for choosing wind speed after a permissible frequency has been selected. Data are for July and August (which Figure 9 shows to have the worst combinations) for five years, and curves are plotted for both the part of the day when the electrical load is heavy and for the off-peak period. Percentage of calms is 1.5 for on-peak hours, or on the average 6.5 per month.

On this basis, considering the number of hours the heavy load is expected to endure, the permissible operating temperature for the wire may be selected from Figure 4 and corresponding current from Figure 7.

Frequency curves are more important under emergencies, when they give data for comparing the coincidence of heavy loads with various weather conditions according to the theories of probability.

Figure 12 shows a special case: average wind and temperature following the passage of the intense phase of lightning storms, in an area where transmission line outages were almost all caused by lightning. There are no cases in which average wind was less than six miles per hour, and temperatures are generally 80 degrees or cooler. Incidentally, on the average there were four hours of rain in the eight-hour period. With such data, wire rating may be specified for these conditions. This should be checked against the one-hour values, and allowance be made for the proper proportions of outages from other causes.

This type of investigation requires much labor. It is often warranted, however, by the increased rating that can be assigned to a line with reasonable assurance. Incomplete data may be misleading—but still not so much as values chosen by common-sense estimates.

Summary of Conditions in References on Annealing of Copper

Reference Number	Author	Initial Conditions						Test Conditions			
		Strength (Thousand Pounds Per Square Inch)	Per Cent Cold Reduction	Strand or Gauge	Type Copper			Tension While Heated (Thousand Pounds Per Square Inch)	Test Method	Variables	
					Per Cent Silver	Per Cent Oxygen	Commercial			Time (Hours)	Temperature (Degrees Centigrade)
1	Wacker	62	71	6	*NS*	*NS*		0	*T*	*x*	100–200
2	Unknown	52	71		*NS*	*NS*		0	*T, E*	*x*	250 and 300
3	Voce	60–57		14–6	0.0017	0.001	*OFHC*	0	*T*	*x*	80
		59–57		14–6	0.001	0.019	*E*				
		59–58		14–6	0.00006	0.030	*EC*				
		50 ±	50	0.56 inch	0.0017	0.001	*OFHC*	0	*V*	1 and 24	200 and 600 and
					0.001	0.019	*E*				
					0	0.030	*EC*			*x*	250
4	Beedle	65		6	*NS*	*NS*		10	*T*	48	100–150
5	Zickrick			34	*NS*	*NS*				0.05	225–325
6	Bradley	73			*NS*	*NS*					25–500
7	Talbot	*NS*	*NS*	*NS*	*NS*	*NS*	*E*	0	*J*		
8	Price	48	25	*NS*	*NS*	Low	*E*	0	*TH**		150–1,000
9	Jares	57		*NS*	*NS*	*NS*	*E*	0	*T*	*x*	180
10	Bassett					See reference 24					
11	Bassett	*NS*	*NS*	*NS*	*NS*	*NS*	*E*				
12	Taylor	Soft		6	*NS*	*NS*	*NS*	5	*T*	*x*	
13	Parker	30	0		0.001	0.035	*E*	High	*T*	*x*	200
					0.002	0	*OFHC*				200
					0.054	0	*OFHC* and silver				200 and 250
14	Fuller	*NS*	*NS*	*NS*	*NS*	*NS*	*E*	High	*T*	20	165
		NS	*NS*	*NS*	0.03	*NS*	*L*		*T*	140	300
15	Alkins	63	93	0.26 millimeter	*NS*	*NS*		*NS*		*x*	150–200
16	Pratt	58	62	*NS*	*NS*	*NS*	*E*		*T, E*	1	*x*
		56	62	*NS*	*NS*	*NS*	*L*				
		58	62	*NS*	*NS*	*NS*	*OFHC*				
17	Zeerleder	*NS*	*NS*	*NS*	*NS*	*NS*	*NS*	*NS*	*NS*	1,000	80
18	H. Moore	60–58		14–19	*NS*	*NS*		0	*T*	*x*	100
19	Caesar	57	69	Strips	0.0005	0.07	*E*	0	*S(T)*	1/2	100–500
		NS	67	Strips	0.1	0	Pure + silver				
		NS	67	Strips	0	0.09	Pure, oxidized				
		53	67	Strips	Pure copper (special)			0	*S*	1/2	100–500
		NS	67	Strips	0	0.03	Pure, slightly oxidized				
		NS	67	Strips	0.002	0.08	Pure, oxidized plus silver				
		57–51	69–51	Strips	0.0005	0.07	*E*	0	*S*	*x*	150–300
20	Offer	62 before aging 59 at test		6		Present	*E*	0	*T*	*x*	100–200
21	Wyman	64	58, 85	8	0.024–0.058	0	*OFHC* + silver	34	*E*	*x*	105
		64	58	8	0.024	0	*OFHC*+ silver	26–34	*E*	*x*	105
		62	50	8	*NS*	*NS*	*E*	26–34	*E*	*x*	105
		62	50	8	*NS*	*NS*	*E*	24	*E*	*x*	150
22	Kinney		20 37 60	Sheets	0.001–0.132	0.05 ±	*L*	0	*R*	1/2	200–400
		70–75	99.8	24	0.001–0.132	0.05 ±	*L*	0	*T, E*	0.033	275, 300, 325
		70–73	97.5	16	0.001–0.32	0.05 ±	*L*	0	*T*	*x*	150 and 200
23	Pilling	42-62	23–84		*NS*	0.066	*E*	0	*T*	*x*	100–900
24	Bassett	58	10	Nos. 0.05-inch sheet	0.0005+ 0.0868,	*NS*	*E, L*	0	*T, E*	*NS*	0–1,000
25	Rolle	59	62	0.5-inch tube	0.0022	0	*OFHC*	0	*T, E*	1	190–700
		59–63	62–91		0.002	0	*OFHC*	0	*T, R*	1	250–400
26	Webster	56	62–90	6–0	*NS*	0.0285	*E*	0	*T, E*	1	180–800
		59	62–90	6–0	0.0216†	0.033	*L*				
		57			0.002	0.0	*OFHC*	0			
27	Angus	*NS*	33–78	0.28 and 0.31 inch	*NS*	*NS*	*E*	0	*B*	1	100–600
28	Grard	56	57	2 millimeters	*NS*	*NS*	*E*	0	*T*	0.5	100–300
29	Bardwell	66		12	*NS*	0.026–0.07	*E*	0	*T*	0.33	100–400
30	Mathewson	51	50	0.065 inch	0.0005	0.071	*E*	0	*T, E*	0.67	100–1,000
31	Zickrick	62–69			*NS*	0–0.028		0	*T*	0.1	200–400
32	Archbutt										
33	J. L. Gregg										
34	Lorig										
35	Widman		98		0 0.24						
36	Hudson		28		0.05	*NS*	Pure+	0	*B*	2	300–500
			50		0.08	*NS*	Silver				250–500
37	Johnson	28–59	0–98 60	Strip	<0.01	0.05	*E*	0	*T, E, S*	1	200–800
					NS	0.089		0	*T, E*	0.5	200–470

Symbols:
In column "Commercial Type"
P—"Pure"
E—Electrolytic
L—Lake
C—Chilean

OFHC—Oxygen-free high-conductivity
"*NS*" = Not stated
In column "Test Method"
J—Judgment
T—Tension test

E—Elongation
R—Rockwell hardness
B—Brinell hardness
V—Vickers
S—Scleroscope
H—Hardness

In column "Time" an "*x*" indicates that a time-temperature curve is given
*Apparently tested while hot. Strength lowered at lowest temperature tried.
† High in arsenic and antimony.

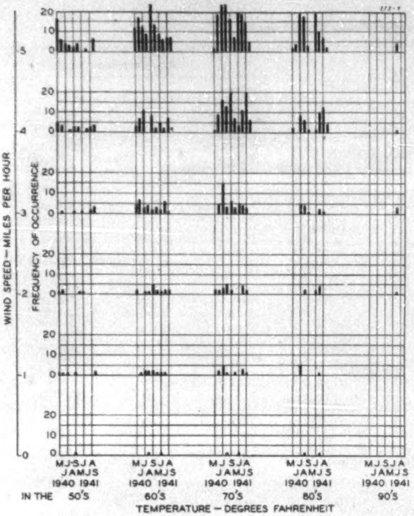


Figure 9. Wind and temperature

Number of hours occurrence of various values at centrally located observatory

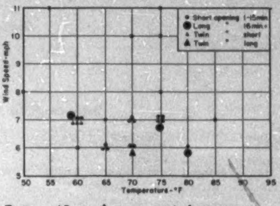


Figure 12. Average wind—temperature

For eight hours following storms that caused 120-kv line openings in 1941. Values at centrally located observatory

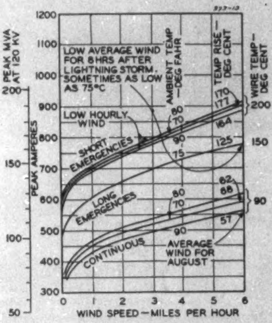


Figure 13. Composite chart for selecting wire rating

Number 000 *HD* copper

III. Combination of Data

When we take into account all the considerations discussed, the situation seems involved. If it is visualized as in Figure 13, the relations become clearer. Curves are plotted showing permissible current against wind speed for number 000 copper containing 0.004 per cent silver. Three conditions result:

1. Continuous load (lowest curve).
2. 20-hour single-circuit outage of a twin-circuit 120-kv line.
3. One-hour outage of the same line.

From Figure 4, the permissible temperatures are found to be 90, 150, and 200 degrees centigrade respectively. Using these temperatures and reasonable ambients, from Figures 7 and 8, we derive values of current for the expected range of wind. On this graph we indicate the probable wind-temperature combinations. Thus for case 1 the record shows an average August wind of about six miles per hour. From Figure 13, we see that about 550–600 amperes can be carried. This is not enough to satisfy the conditions for long-time loading. There may be other lower limits. For instance, the 1.5 per cent of calms in August, plus other hours of low wind, may impose a lower limit, even though the permissible temperature is higher.

If we knew more about the effect of intermittent heating upon annealing, we would be justified in developing a method of integrating the temperature-time relationship in a manner similar to the procedure on transformers.

The other curves and weather data shown on Figure 13 should be considered similarly. Such visualization indicates the range of currents that can be reason-

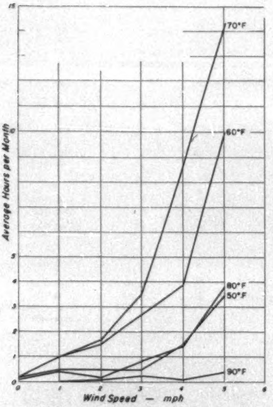


Figure 10. Incidence of low winds at various temperatures

May to September 1940–41, 8 a.m.–5 p.m. Figure shows small number of cases at 90 degrees Fahrenheit all with two miles per hour or more

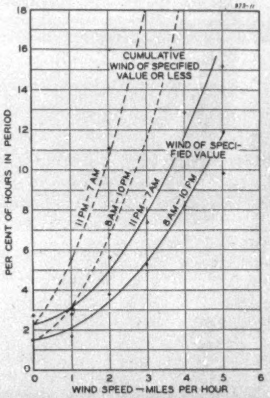


Figure 11. Frequency and cumulative frequency of winds in July and August 1935–40

Values at centrally located observatory

ably used and shows the desirability of adjusting the ratings to each set of conditions encountered in operation.

This report summarizes a great deal of information of electric rating of overhead wires on a thermal basis. It gives a reasonable working basis for most cases but indicates there are many gaps to be filled: in finding how intermittent heating under tension and vibration affects heating, the temperature limits on wire coverings, resolving some discrepancies in temperature-rise data, in computing expectancies of loads and weather combinations, and in developing a simplified method of applying these data in the field.

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Advantages of High-Speed Traction Motors

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THE present use of high-speed lightweight propulsion motors on various types of traction vehicles is the result of an engineering evolution. An important phase of this development began approximately fifteen years ago, but the greatest progress has been during the past seven years. The motors resulting have definite advantages over their larger and heavier predecessors, and it is the purpose of this paper to point out and evaluate the more important of these advantages.

Historical

Prior to 1927 practically all of the traction motors in use on streetcars and interurban and subway cars were of the axle-hung type, geared to the wheels by a pinion on the armature shaft and a gear on the car axle. During the middle 20's there was a demand for motors of much lighter weight for use on gas-electric and trolley coaches. This demand was met by the design of motors of considerably higher speed and connected to the wheels through propeller shafts and automotive-type axles. These early high-speed motors were somewhat hybrid in construction, utilizing experience obtained from the lightest weight axle-hung streetcar motors and those used on electric battery trucks at that time. It is significant that nearly all of them used commutating poles and all employed antifriction armature bearings.

In 1928, the Westinghouse double-reduction *W-N* gear-unit drive was developed for streetcars. This required a high-speed motor which had approximately 35 per cent of the weight and double the maximum armature speed of its axle-hung predecessor. Since that time, much design, research, and operating experience has been combined to produce the modern motors that are used today on the various forms of electrically driven city and intercity transit vehicles and some types of mining and railway locomotives. The following discussion enumerates and evaluates the principal advantages of these motors:

Reduced Size and Weight

The principal feature that permits motor designs of decreased size and weight is the gearing used which allows higher-speed armatures. The maximum gear reduction usable on the axle-hung motors was around 6 to 1, and reductions used were usually around 5 to 1. This was because of an inherent limitation in gear reduction in the tie-up between the center lines of armature and axle, gear-tooth strength, and clearance from gear case to rail. With the motor separated from the gears, a gear ratio is selected which allows the motor armature to rotate at its highest economical speed. The result is that traction-motor gear ratios from 6 to 1 up to 22 to 1 are now used, depending on the application and type of vehicle.

The size and weight of a motor armature are approximately proportional to its torque. Horsepower is proportional to the product of speed and torque. If a given horsepower rating can be obtained from more speed and less torque, the armature is smaller in proportion to the decrease in torque. Another reason for smaller armatures on recent motors is the saving in space effected by modern insulation of the armature coils. The development of thinner and stronger mica and glass tapes and wrappers has made this possible. Any decrease in armature diameter by improvements such as this means that the rotational speed can be increased further, and thus a still smaller armature for the same horsepower can be used. A smaller armature carries with it a reduced field structure, and therefore the whole motor is reduced in size and weight. Figure 1 shows a comparison of motor weights. The comparison is between the latest light traction axle-hung motors *A*, and modern high-speed motors developed since 1935–*B*. The weights given are exclusive of gears or gear housings in both cases.

Figure 2 shows comparatively how the size of traction motors having a one-hour rating of 125 horsepower has varied over a period of years.

One of the advantages of the reduced motor size is the saving in material. The saving of copper is especially important as an aid to the war effort. The copper saving in motors used on 2,100 Presidents Conference Committee streamlined streetcars recently built in this country and Canada is approximately 1,250,000 pounds. This saving is estimated by comparing the weight of copper in the present motors used on these cars with the weight of copper in the most recently designed axle-hung streetcar motors of the same rating.

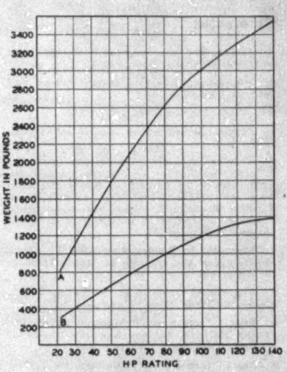

Figure 1. Motor weight comparison

A. Weights of most recent axle-hung streetcar and interurban car motors

B. Weights of modern high-speed traction motors

Another important advantage of decreased motor size and weight is the allowable reduction in truck or chassis size and weight. Modern streetcars, trolley-coach, or Diesel-electric-coach construction would not be possible if the modern light-weight motors were not available.

Increased Ventilation

The higher-speed armature permits a fan design that provides increased ventilation. This causes the continuous rating more nearly to approach the one-hour rating. The slower-speed motors had continuous ampere ratings that were between 60 and 75 per cent of the one-hour rated amperes. The high-speed motors have continuous ratings between 80 and 90 per cent of the one-hour rating.

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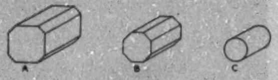

Figure 2. Graphic comparison of traction motor body size

A. Rated 600 volts, 125 horsepower, 275 rpm (developed in 1909)

B. Rated 600 volts, 125 horsepower, 800 rpm (developed in 1915)

C. Rated 300 volts, 125 horsepower, 2,000 rpm (developed in 1939)

Higher Efficiency

The high-speed motor is fundamentally more efficient than a lower-speed motor of the same voltage and rating. This is because fewer conductors of shorter length are required both in the armature and field coils. This reduces the copper I^2R loss which is a major part of the total loss especially at heavy accelerating loads. The iron losses and friction losses of the high-speed motor may become greater at low values of tractive effort, but the small per cent of operation at these low loads makes the reduction of efficiency here unimportant.

Figure 3 shows a comparison of motor efficiencies of two 125-horsepower, 600-volt traction motors. *A* has a rated speed of 2,440 revolutions per minute and maximum speed of 4,500 revolutions per minute. *B* has a rated speed of 800 revolutions per minute, maximum 1,800 revolutions per minute.

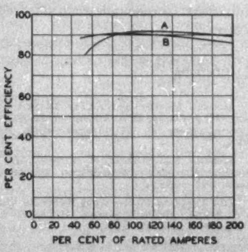

Figure 3. Comparison of motor efficiency

A. Rated 600 volts, 125 horsepower, 2,440 rpm

B. Rated 600 volts, 125 horsepower, 800 rpm

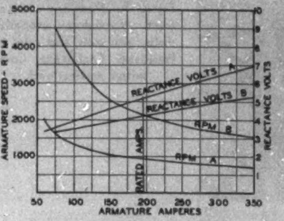

Figure 4. Commutation comparison

A. Motor rated 600 volts, 140 horsepower, 930 rpm

B. Motor rated 600 volts, 140 horsepower, 2,000 rpm

Better Commutation

The modern high-speed motors have better commutation than their predecessors. This is partly inherent in the higher-speed design and partly due to increased knowledge. The so-called "reactance" voltage that tends to cause sparking at the brushes is dependent mainly on the number of armature conductors, armature size, and speed. The higher-speed armature has less conductors and is smaller, and this more than offsets the effect of increased speed. The use of the full number of commutating poles and brush arms, as well as greater care to avoid saturation of the commutating-pole magnetic circuit at heavy loads, have also been factors in producing improved commutating and flashing characteristics. Figure 4 shows a comparison of reactance or "sparking" volts for a low-speed and a high-speed motor.

Improvements in Mechanical Construction

Mechanical improvements have gone along with motor speed increases. Some of them have been stimulated by the higher speeds. Examples of these are accurate dynamic balancing, better commutator seasoning, and more accurate commutator surfacing. Once shop methods for performing these operations were established, they were found to be as easy to do as the less adequate methods formerly used for slower-speed armatures. The types of motor mounting have been simplified so that motor frames have become simple cylinders of rolled steel. Easily removable, simple commutator covers are used, allowing easy inspection of commutator and brush holders. Motors are being mounted so that they have the same spring-supported ride as

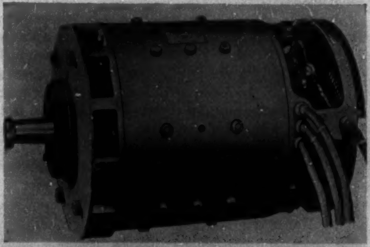

Figure 5. High-speed 125-horsepower 300-volt motor for use with double- or triple-reduction gearing on Diesel-electric switcher locomotives

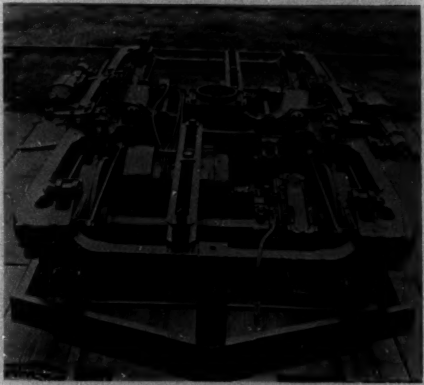

Figure 6. [High-speed motors and W-N gear unit mounted in truck of high-speed trolley train

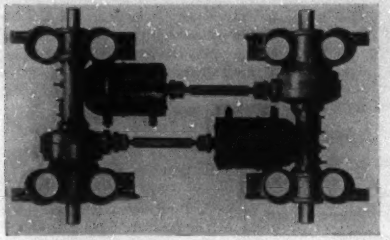

Figure 7. Mounting arrangement of motors, propeller shafts, gear units, and axles in Presidents Conference Committee streetcar truck

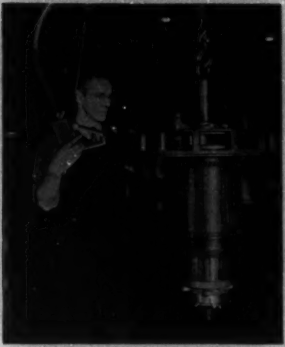

Figure 9. Armature of 140-horsepower trolley-coach motor complete with bearing housings and bearings, weighs only 525 pounds

the passengers. This permits constructional simplification not allowable on a motor receiving direct impacts through wheels and axle. An example of this is the elimination of field-coil springs. Field coils are now made accurately to size and clamped solidly between a flat washer at the pole tip and the frame at the base of the pole. In the design of the stator, any improvement that decreases the frame diameter for a given armature diameter pays almost double in weight reduction. It not only reduces the amount of iron in the magnetic circuit but shortens the length of magnetic path, thus rapidly reducing the field turns required for the same flux.

Improved Insulation

Improvements in insulating materials and treatment processes have been numerous during the past few years. These improvements have been incorporated in the new motors as they were designed. Class *A* insulation is rarely considered in the design of new traction motors. Class *B* conductor coverings, tapes, and slot wrappers have been improved so that they are thinner, yet have increased dielectric and mechanical strength along with the ability to withstand high temperatures. Improved insulating varnishes and methods of treating coils before and after their assembly have made windings less liable to grounds.

Ease of Maintenance Increased

Ease of maintenance and therefore the cost of maintenance have been considered constantly in the design of modern high-speed traction motors. The reduced labor of handling because of the smaller size of complete motor and parts is an important factor. The use of grease-lubricated roller or ball bearings and a bearing housing construction that permits removal from the frame without exposure of the bearings or lubricant to dirt is another. With the present bearing and shaft construction, shaft breakages are practically unknown and bearing replacements are very infrequent. The cost of renewal parts and the difficulty of their replacement both have been reduced in this type of motor.

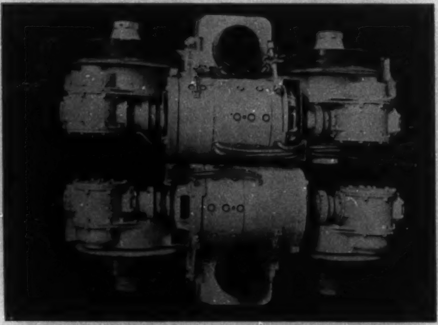

Figure 8. High-speed traction motors mounted in Differential Car Company "axleless" truck for mining locomotives

Conclusion

The modern high-speed traction motor as used today may be said to be the product of an evolutionary development and is based on sound engineering fundamentals. The outstanding advance that has been made in reduction in weight and size has been accompanied by better commutation, increased ventilation, higher efficiency, and improved mechanical construction. The various improvements that have accompanied the increase in speed have combined to produce a motor that is easier to place in a truck or chassis and is more trouble-free and easier to maintain than its predecessors.

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Pilot-Wire Relaying on a Metropolitan System

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A NUMBER of papers have been written which discuss individual pilot-wire relaying schemes. A recent paper presented by the relay subcommittee of the committee on protective devices[1] analyzed the pilot-wire circuit used in these relaying schemes. The present paper is a discussion of pilot-wire systems on a large metropolitan system where they have been in successful use for many years. The paper brings out the fact that pilot-wire relaying has many advantages over other forms of relaying because of selectivity and high-speed operation. It has also been found that the reliability of a complete relaying scheme is substantially better than would be expected from the record of the pilot-wire circuit as reported in the subcommittee paper at the 1943 winter technical meeting.

The general use of pilot-wire relaying on the metropolitan system referred to was necessitated by some special characteristics of this system. The system has a large amount of generating capacity closely interconnected through short transmission lines. Therefore, phase isolation is used to reduce the short-circuit duty on circuit breakers and limit physical damage from faults. This isolated phase system includes the use of neutral resistors which limit the ground fault current to such low values that the tripping time would be too slow with conventional overcurrent relaying schemes. The use of pilot-wire relaying permits accurate selectivity with these low fault currents regardless of the switching arrangements of the transmission lines for various operating conditions.

Practically all of the large industrial customers are served by cable loops out of stations or substations. The large number of customers served by a single loop would require excessive time settings on overcurrent relays. Therefore, pilot-wire relaying was selected to permit an indefinite number of steps without increasing the relay timing. Figure 1 shows the gain in relay time obtained by the use of pilot wires on the loop system. The overcurrent relays at the substation end of the loop (see Figure 1*b*) provide back-up protection; those shown in the center section provide primary protection for this section and at the same time limit outages caused by back-up operations to one half the loop.

On this power system, where large amounts of power are transmitted over relatively short distances, the transmission lines are of unusually low impedance. For this reason the use of distance relays is not feasible. The shortness of the transmission lines also favors pilot wires because the cost of pilot wire between stations, when added to the cost of relays at the terminals, results in a total cost for relaying comparable with the cost of distance or directional relays.

The paper includes an economic comparison to indicate that for reasonably short transmission lines, pilot-wire relaying is more economical than carrier relaying in addition to having inherently simpler methods of obtaining selectivity.

Description of Relaying Systems

At the present, a number of different pilot-wire schemes are available for transmission-line protection.[3,4,5,6] These schemes vary considerably in method, some using a direct comparison of currents at the two ends of the line, while others use a derived d-c pulse to effect the comparison. This paper describes only those pilot-wire schemes which are being used on the power system under consideration. The first published reference to these schemes, one of which has been used on this power system for the last 30 years, is a paper by R. F. Schuchardt in 1917.[7] These are circulating current schemes in which alternating current flows through the pilot wires. While all installations are of the same general type, they can be classified in the following two groups:

Scheme 1 (Figure 2) is used on transmission lines consisting of three-conductor underground cable and operates for ground faults only. Under normal conditions, there is no residual current in the line with the result that there is no current in the pilot wires. Upon the occurrence of an external ground fault, current flows through the pilot wires, but the circuit is so devised that in each relay the currents in the two coils will be equal and opposite, with the result that the relays will not operate on an external fault. For an internal ground fault, the current through the two coils of each relay will not balance, and the relay will operate. A description of the current flow for external and internal faults is given in the appendix. The relays used on this scheme are simple two-coil balanced differential relays which require $1\frac{1}{2}$ amperes difference in current to operate and trip in approximately two cycles. On most lines to which this system is applied, the current setting corresponds to a primary fault current which is lower than the full load current of the line. Recently, a new telephone-type relay was developed for this system which operates in one cycle with the same current setting.

Scheme 2 (Figure 3) is similar in principle to the first scheme except that a special pilot transformer is used instead of a two-coil relay. By the use of four pilot wires and three sets of terminal equipment as shown, the scheme protects for all types of phase

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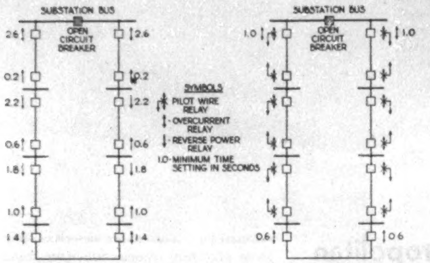

(a) RELAY SYSTEM WITHOUT PILOT WIRES

(b) RELAY SYSTEM WITH PILOT WIRES

Figure 1. Effect of pilot-wire relaying in reducing relay time settings on a loop transmission system

and ground faults. The special transformer serves two purposes: first, the differential current is obtained from the transformer and, second, the current supplied to the relay is stepped up in value by the transformer. This special transformer makes possible the use of standard overcurrent relays and also results in a lower burden on the main current transformers, since a smaller value of current can be circulated through the pilot wires. This scheme is used on high-voltage lines consisting of single conductor underground cables and operates for all types of phase and ground faults. The high-voltage lines on which this scheme is used are considerably longer than the lines protected by scheme 1. This situation made it advisable to reduce the burden on the current transformers, and the special transformers were developed for this purpose. Most of these relay installations are set to operate at 0.7 ampere which corresponds to a primary fault current below full load current for the line. The latest installations of this system use high-speed relays operating in one cycle. The flow of current in the pilot wires is not shown for this scheme, but it is similar to that shown for scheme 1 in the appendix. It will be noted that the resistors used for this scheme are one half the value used in scheme 1. This is necessary because the circulating current must pass through two resistors in series at each terminal for all types of faults.

While the two schemes shown are the basic ones employed, a modification has been adopted to make possible the use of the two-wire scheme to protect for all types of faults. The method by which this is accomplished is shown in Figure 4. This method is applicable both for scheme 1 and also a two-wire scheme using pilot transformers. The principle advantage of using four pilot wires and three relays instead of two pilot wires and one relay is that the relay target will identify which cable has failed in a transmission line using single-conductor cables. The modified scheme is used to protect three-conductor cables on tie lines between important substations.

For scheme 1, which requires two pilot wires, three-conductor, number 12 pilot-wire cable is used. This is lead-covered underground cable with $^{3}/_{64}$-inch rubber insulation rated at 500 volts. All new pilot cables are tested at 3,000 volts, alternating current, for one minute. The third wire in this cable is a spare and is used in routine testing of the relays. For scheme 2, four wires are needed and five-conductor, number 12 cable is used of the same type as used for scheme 1.

It is interesting to note that in all the schemes described the relay operation is independent of the source of fault current. Relay operation is the same whether the total fault current comes from either end or from both ends. The relay operating currents at both ends of the line are equal and are a direct function of the total primary fault current. This condition holds even for the line open at one end.

A scheme was developed for neutralizing the effect of the capacitance of the pilot wires, but it has not been necessary to apply the scheme for this company. The longest line to which the relay system is now being applied has a length of approximately 15 miles, and even for this line the capacitance of the pilot wires has no appreciable effect. Charging current of the main transmission line inherently appears as fault current to the relay system, and with long, underground, high-voltage cables this factor is of some importance. For the long line previously mentioned it was found necessary to use a one-ampere setting of the relay instead of 0.7 ampere used on shorter lines.

Another characteristic of the schemes described is that it is desirable to use similar terminal equipment at both ends of a line. Correct operation of the system depends on a definite division of the secondary currents, which, in turn, depends on the relative impedance of the terminal equipment. The use of dissimilar terminal equipment might result in a loss of sensitiveness, or the possibility of tripping on through faults.

Obviously, all of the schemes described depend on the pilot circuit being in sound condition for correct operation. Either an open circuit in the pilot wires or multiple grounds on these wires will result generally in incorrect operation. For the schemes which operate for phase faults, trouble in the pilot wires will result in tripping the line on heavy load currents when there is no fault. This is not serious since it results in only one outage in a group of lines. For the schemes limited to operation on ground faults, trouble in the pilot wires will result in tripping of the line for an external fault. In all schemes, the special case of a short-circuit between pilot wires near the center of the line will prevent tripping for an internal fault on the power line.

In recent years, the pilot-wire systems have been used to protect lines which are directly connected to transformers without a primary circuit breaker. The method employed is to use the pilot-wire system to protect the high-voltage line, and standard percentage relays to protect the transformer. The operation of the transformer relay open-circuits the pilot wire with the result that the line breakers at the far end of the line will open for a small value of fault current.

This principle is also employed in some cases to trip the far end of a tie line between important substations. The location of the current transformers used in the pilot-wire scheme is such that the zone of protection of the pilot-wire relay does not cover all the terminal equipment of the line. In many cases a fault-bus or overcurrent relay is available which protects the equipment not covered by the pilot-wire relays. In these cases, the operation of the fault-bus or overcurrent relay unbalances the pilot-wire system,

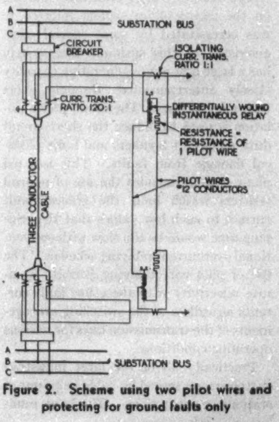
A
B
C
SUBSTATION BUS
CIRCUIT BREAKER
ISOLATING CURR. TRANS. RATIO 1:1
W
CURR. TRANS. RATIO 120:1
DIFFERENTIALLY WOUND INSTANTANEOUS RELAY
RESISTANCE = RESISTANCE OF 1 PILOT WIRE
THREE CONDUCTOR CABLE
PILOT WIRES #12 CONDUCTORS
W
A
B
C
SUBSTATION BUS

Figure 2. Scheme using two pilot wires and protecting for ground faults only

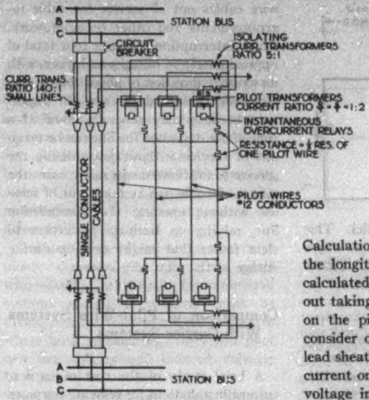
A
B
C
STATION BUS
CIRCUIT BREAKER
ISOLATING CURR. TRANSFORMERS RATIO 5:1
CURR. TRANS. RATIO 140:1 SMALL LINES
PILOT TRANSFORMERS CURRENT RATIO = = 1:2
INSTANTANEOUS OVERCURRENT RELAYS
RESISTANCE = ½ RES. OF ONE PILOT WIRE
SINGLE CONDUCTOR CABLES
PILOT WIRES *12 CONDUCTORS
A
B
C
STATION BUS

Figure 3. Scheme using four pilot wires and protecting for all types of faults

thereby causing the far end of the line to be tripped.

Abnormal Voltages on Pilot Wires

As shown in Figures 2 and 3, the pilot-wire systems are isolated from all ground connections. With this arrangement, one ground on the relay system will not cause an operation of the relays. Also, the longitudinal induced voltage on an unfaulted pilot cable is so distributed that at each end of the cable one half of the total induced voltage is impressed from conductor to sheath.

In general terms, the longitudinal induced voltage is a voltage induced along a conductor, which builds up a voltage between the conductor and ground. In the underground power system being discussed in this paper, the longitudinal voltage induced under fault conditions has a somewhat different character. In this system practically all the current in a ground fault returns to the source over the lead sheaths in the duct run carrying the faulted cable. The sheath of the pilot cable is connected to the sheaths of the power cables at each manhole, and the entire group of sheaths is practically at ground potential throughout its length. Calculations and tests have shown that the longitudinal induced voltage can be calculated with sufficient accuracy without taking into account the flux linkages on the pilot cable. It is necessary to consider only the resistance drop in the lead sheaths caused by the return of fault current on this sheath. The longitudinal voltage in this case is the difference of potential between the two ends of the pilot-cable sheath. Also, it is the voltage that would appear between conductor and sheath at one end of the cable if the conductor were connected to the sheath at the other end. As noted previously, if the pilot-cable insulation is in good condition, one half of the longitudinal voltage would appear between the conductor and sheath at each end of the cable.

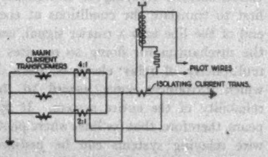

Figure 4. Method for modifying pilot-wire circuit to make it operative for phase-to-phase and phase-to-ground faults

All terminal equipment of the relay system is tested at 1,500 volts a-c for one minute, when placed in operation. The isolating current transformers are tested at 2,500 volts a-c for one minute. As previously noted, new pilot cable is tested at 3,000 volts a-c for one minute.

The low values of ground-fault currents which are obtained in this power system, combined with the fact that all transmission lines and pilot wires are in lead-covered underground cable, result in very low values of induced voltage on the pilot-wire system. Calculations and tests have shown that on practically all lines the maximum induced longitudinal voltage is considerably less than 1,000 volts. For about five per cent of the transmission lines, the induced voltage is above 1,000 volts, and in the maximum case there are approximately 2,500 volts induced. However, even in this maximum case, the effect of operating pilot wires ungrounded results in the voltage stress on the pilot wires and terminal equipment being within their insulation strength.

In many cases, the rise of ground bus potential under fault conditions, caused by high grounding resistance, creates a problem in the protection of pilot-wire systems.[2] However, on this power system this effect is practically negligible. As stated before, most of the fault current returns to the station over lead sheaths, which, in turn, are directly connected to the ground bus at the station. This arrangement, combined with the fact that the total ground fault current is relatively small, results in extremely low values of ground bus voltages in the station supplying the fault current.

As a result of the low values of induced voltage on the pilot-wire cables, it has not been found necessary to use any equipment on the cables to protect against transient overvoltages.

Comparison of Privately Owned and Leased Pilot-Wire Circuits

One important question that always arises in the application of pilot-wire relays is the use of privately owned pilot wires versus leased telephone wires. Privately owned circuits are relatively high in cost; on the power system under consideration, the pilot cables used cost up to approximately $1,500 per mile installed. No cost for ducts is included in this figure because pilot cables are generally installed in center ducts which do not have good heat-radiating capacity for power cables. This cost for privately owned circuits compares with a cost for leased wires of approximately $60 per mile per year, in the metropolitan area.

Privately owned wires have several operating advantages. They make possible the use of lower resistance wires which are preferable for the circulating current schemes described in this paper. Also private cables are of higher insulation strength and, therefore, are less susceptible to the effects of induced voltage. These circuits are better protected from external damage and their service records show the benefits gained. Over a period of ten years, on this power system, there have been only five service failures on 218 circuits, consisting of approximately 300 miles of cable. This power system uses leased telephone wires on a telemetering system which consists of approximately 120 circuit miles. In a two-year period

Table I

Relay System	Per Cent Incorrect Operations	Remarks
All classes	3.7	Majority of relays, simple induction type
Used on high-voltage system	7.2	Includes many of the more complicated types
Pilot-wire schemes	3.7	Speed of operation faster than other relays on high-voltage system, in many instances

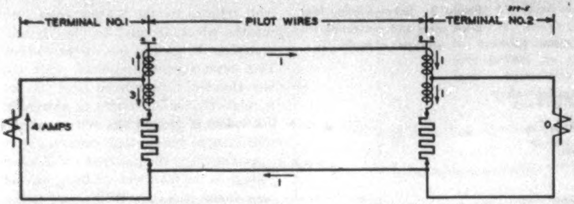

Figure 5. Current flow for internal fault with all fault current supplied from terminal 1

there have been eight emergency outages on this system, the duration ranging from a few minutes to five hours.

Data on Operating Record of Pilot-Wire Circuits

The number of pilot-wire circuits now in use on the power system under consideration, are as follows:

Scheme 1	155 circuits
Scheme 2	25 circuits
Modified scheme 1	38 circuits
Total	218 circuits

The total length of cable involved on these circuits is approximately 300 miles. During the last ten years there have been 164 operations of the pilot-wire relay systems, of which six have been incorrect. Of the six cases of incorrect operation, one was caused by terminal equipment and five by trouble on pilot cables. Following is a detailed analysis of these six incorrect operations:

In one case a terminal resistor was damaged by overloading during the testing of the relay system. As a result of this damage, the resistor was open-circuited and the line tripped out on a through fault.

In one case a pilot cable was cut by mistake by an employe of another company, and the line opened on load current. In the process of locating the trouble in this pilot cable, a company employe cut another pilot cable by mistake with the result that a second line opened on load current.

In one case a street-lighting circuit failed and caused the burning of a pilot cable, which caused an opening of the transmission line.

In two cases a pilot cable was damaged and the conductors became short-circuited, with the result that the lines tripped on through faults.

In all cases of incorrect operation, the lines were tripped out either on load current or on external faults. In the cases where the line tripped on load current, service was not affected because the line which was tripped out was only one of a number operating in parallel. This situation also applies to those cases where a line tripped on through fault. Therefore, none of the incorrect operations resulted in service interruptions.

The high degree of reliability of the pilot wires has made it unnecessary to use any form of continuous supervision to check the continuity of the wires. The wires are checked semiannually as part of the routine relay test.

A comparison of results obtained by different methods of relaying requires consideration of the relay speed necessary on the different parts of the system. On this power system, the largest number of line relays is on the intermediate voltage transmission system where lines are short, stability is not a factor, and a simple time selection scheme is satisfactory. On the high-voltage system, including all lines in the interconnected group, the power concentration is greater and stability is a factor on the long overhead lines. For this high-voltage system, high-speed relaying is essential, and modern distance and carrier relays are being used in these installations.

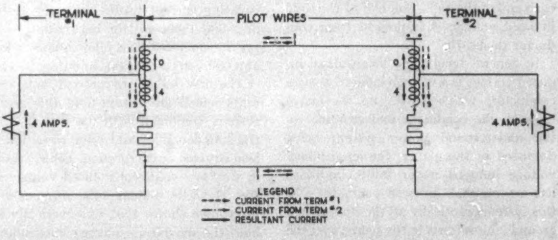

Figure 6. Current flow for internal fault with equal currents supplied from the two terminals

Table I is a comparative record to show relay performance on this metropolitan system over a ten-year period.

In addition to the service outages referred to there are a relatively large number of planned interruptions of the pilot-wire systems. These outages are occasioned by the necessity for taking pilot-wire cables out of service for cable rearrangements and other types of work. These interruptions amount to a total of approximately 75 outages each year, with an average duration of about four hours. However, these outages are not important because they generally come at a time of the day when the line can be taken out of service without jeopardizing the service to customers. In many cases the pilot-wire cable can be taken out of service without opening the transmission line, relying on back-up protection to clear faults that might develop during outage of the pilot-wire system.

Comparison of Pilot-Wire Systems With Carrier Systems

A brief study of the cost of carrier-current installations for relaying purposes shows that using the type of system described in this paper, pilot-wire systems are competitive in cost for lines up to approximately 20 miles in length. Using telephone wires, the pilot-wire system is competitive with carrier current up to lines of approximately 35 miles in length. The cost comparison definitely favors carrier-current relaying on longer overhead transmission lines.

Pilot-wire relaying of the type described in this paper is inherently simpler than the carrier-current systems now in use. In the pilot-wire system the comparison of conditions at the two ends of a transmission line is made directly, whereas in the case of the carrier-current relaying, the comparison is an indirect one. In carrier-current relaying it is necessary first to translate the conditions at each end of the line into a carrier signal, and the mechanism for doing so requires a multiplicity of relay elements, each of which introduces some hazard to the reliability of the entire system. It appears, therefore, that for lines where pilot-wire relaying systems can be justified from an economic viewpoint, they should provide a more reliable form of relay protection than carrier-current relaying.

Conclusion

Pilot-wire relaying is applicable over a substantial field in transmisson-line protection. This scheme provides high speed and selective operation under all switching arrangements. There are many types of pilot-wire-relaying schemes which have been developed over the past 30 years. Only the schemes with which the authors have had operating experience are discussed in this paper.

Induced voltage is one of the most commonly discussed limitations to pilot-wire-relaying schemes. On overhead systems special precautions must be taken, but for this metropolitan system where both transmission lines and pilot wire are underground induced voltages have caused no trouble.

On this metropolitan system the percentage of correct operations, over the past ten years, is approximately the same as obtained from the very simple induction relay schemes. On the other hand, the percentage of correct operations is appreciably higher than that obtained from the more complicated schemes, including high-speed distance relays and carrier relays which are required for modern high-speed switching.

Pilot relaying is more economical than other schemes on relatively short transmission lines. Where pilot wires are usable, they are inherently more simple than other relays with comparable operating speed.

Appendix

Figures 5, 6, and 7 show the flow of current in the pilot wires and relay coils for external and internal fault conditions. These diagrams are based on scheme 1, but the same principles apply to the other schemes.

In Figure 5 an internal fault is assumed with all current being supplied from one end of the line. The primary fault current is assumed to be of such a magnitude that the current in the secondary circuit of the isolating current transformer is four amperes. This current divides at the point where the isolating current transformer connects to the relay coils, in proportion to the impedance of the two paths. Three amperes flow into the coil connected to the resistor and one ampere to the coil connected directly to the pilot wire. The current of one ampere flows through the pilot wire, through the two relay coils at the far end of the line, and back to the first terminal over the other pilot wire. This three to one division of current between the two coils is the same for all conditions and in all the schemes described. The ratio of the two currents is fixed by the value of the resistor in the circuit at each terminal, which in the case of the two-wire scheme is equal to the resistance of one pilot wire. With the value of the fault current chosen, the vector sum of currents in each relay for the case of Figure 5 is two amperes. Since the relays are set to operate at a net current of $1^1/_2$ amperes, this fault current is sufficient to trip both relays. Note that the resultant current at the two terminals is identical, although the fault is fed from one end only.

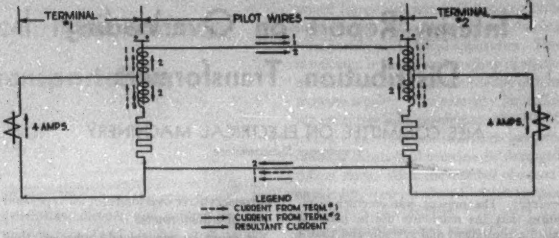

Figure 7. Current flow for external fault

In Figure 6 an internal fault is again assumed, but, in this case, there is a current of four amperes supplied from each end of the line. The simplest method of analyzing the flow of current in the pilot-wire system is to use the principle of superposition. The diagram shows separately the flow of current supplied from each end of the line. The division of current at the point where the isolating current transformer connects to the relay coils is based on the same principle covered in Figure 5; that is, three fourths of the current flows in the relay connected to the resistor and one fourth to the coil connected directly to the pilot wire. This diagram also shows the resultant of the two circulating currents, which is the actual current flowing in the system. It will be noted that the resultant differential current in each relay is four amperes instead of two as in the previous case. This illustrates the general principle that the net current in each relay is a direct function of the total internal fault current. In this specific case of equal currents fed from the two ends of the line, the pilot-wire current is zero. This condition holds only for this specific division of fault current.

Figure 7 shows the condition for an external fault with the magnitude of the current equal to four amperes in the isolating current-transformer circuit. Again, this diagram uses the principle of superposition to show the flow of current. It will be noted that the flow of current from terminal 1 has the same magnitude and direction as for the case shown in Figure 6. For the current from terminal 2 the current has the same magnitude but is opposite in direction because of the through-fault condition. The effect of this reversal of direction is to give equal and opposite currents in the two relay coils. This illustrates the general principle that for a through fault of any magnitude the currents in the relay coils will be equal and opposite, with the result that there is no tendency for the relay to operate on such faults.

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Interim Report on Overloading Distribution Transformers

AIEE COMMITTEE ON ELECTRICAL MACHINERY
Transformer Subcommittee

Preface: The present war emergency requires that the maximum use be made of existing equipment and systems and that a minimum of critical material be used for new equipment.

This publication, as well as other guides and reports in this series, has been prepared for the information of users during the war emergency. Upon termination of the war emergency, they will be reconsidered by the standards committee and the committees that prepared them, and will be approved, revised for normal use, or rescinded.

This procedure is being followed in preference to the preparation of special emergency standards which might involve redesigning and drastic changes in manufacturing practices. These guides will accomplish the maximum conservation of critical materials, since they provide for the maximum use of existing equipment and systems, as well as new equipment, without changing the fundamental basis on which the present standards have been prepared.

THE "Interim Report on Guides for Overloading Transformers and Voltage Regulators," which was presented at the AIEE summer convention, June 22, 1942, and was published in the September 1942 issue of ELECTRICAL ENGINEERING, gave information on the overloading of all types of transformers and of voltage regulators.

The interim report gave basic principles to be followed for determining permissible overloads with normal life expectancy based on the assumption that the transformer would be operated continuously throughout its life on the basis of the assumed conditions. It also gave tables and charts covering permissible emergency overloads with moderate sacrifice of life expectancy. The data given result in conservative overloads for all types and sizes of transformers.

It must be recognized, however, that such overloads may be too conservative for some small distribution transformers where the temperature rise is appreciably lower than the specified rise on account of the use of a tank larger than necessary for thermal requirements, because general rules and tables such as those given in the "Interim Report on Guides for Overloading Transformers and Voltage Regulators" must be made to fit transformers of all kinds, varieties, and sizes (within certain limits) having different design characteristics.

It is difficult to fix specific limitations for operation of distribution transformers because of the multiplicity of factors governing such operation.

For distribution applications the shape of the load curve imposed upon a transformer will vary for each application. Although for larger-sized distribution transformers the load curve may be comparable to that for a complete distribution system, for the smaller units, such as used on residential and commercial applications, the load curves are usually very complex and are not classified so readily in terms of load factor as are the load curves for power transformers.

The thermal characteristics of distribution transformers also are affected by numerous factors. Small distribution transformers inherently have greater cooling ability per kilovolt-ampere than larger sizes with the result that they have greater thermal ability. In addition, the particular design has greater influence on the permissible load than any other factor being considered. The establishment of thermal characteristics for each size, type, and manufacture of unit would be a tremendous task.

In distribution transformer application, consideration must always be given to permissible voltage drop and voltage variation in the transformers and the rest of the system as well as to the thermal characteristics of the transformers. In some cases, voltage drop rather than thermal capacity may be the limiting factor in the loading of distribution transformers.

While it would be desirable to have recommendations that would more nearly cover the ultimate load capabilities of these smaller units, specific recommendations must depend on factors peculiar to individual systems and loads to such extent that it seems impractical to produce data that can be applied generally.

The overloads for transformers recommended in the "American Standards for Transformers, Regulators, and Reactors, *C*-57.3" and in the "Interim Report on Guides for Overloading Transformers and Voltage Regulators" are based on actual load values. Various methods of estimating loads on distribution transformers are in common use. These methods do not always give results on a basis comparable to the actual load values given in the afore-mentioned reports.

It seems necessary, therefore, that those users seeking the near ultimate in loading of distribution transformers base their thoughts and conclusions on the particular characteristics of the equipment and loads peculiar to their own systems. They will find some assistance in the guide and report referred to previously and in the papers listed in the references.

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Personnel of transformer subcommittee: M. S. Oldacre, *chairman;* F. S. Brown, E. S. Bundy, represented by R. T. Henry; J. E. Clem, I. W. Gross, V. M. Montsinger, J. R. North, W. C. Sealey, F. J. Vogel, C. F. Wagner.

This interim report was prepared by the AIEE transformer subcommittee of the committee on electrical machinery for the purpose of making essential information immediately available to war industries, thus furthering the conservation of valuable material for the war emergency. It is educational and in no way mandatory. It is not intended as a "Standard," and has not been approved formally by the standards committee nor the board of directors.

Adequate Electrical Maintenance Essential to Transportation

W. J. CLARDY
MEMBER AIEE

Synopsis: The demand for public transportation is increasing rapidly under present conditions of accelerated industrial activity. Adequate maintenance of electrical apparatus on rolling stock is essential to avoid impairment to the war effort. Measures which will assist to retain generators, motors, and control in service practically 100 per cent of the time are proper operation, effective inspection, preventive maintenance, keeping apparatus clean, accurate adjustments, prompt worn-part replacements, undelayed commutator conditioning, use of improved insulation, and correct lubrication. More careful attention to all of these factors during the present emergency will assure maximum rolling stock availability.

Maintenance of Transportation Electrical Apparatus and the War Effort

AMERICANS are confirmed optimists. They like to believe that life, somehow, works out for their individual good. But today, we are faced with a transportation situation which can ruin our entire internal economy just as surely as being bombed to destruction. Goods and people must be moved to places where they are needed to be effective in the war effort. Electric locomotives, suburban cars, rapid transit trains, streetcars, trolley coaches, and Diesel-electric buses are doing their bit in meeting this demand. The adequate maintenance of electrical apparatus on this rolling stock is a vital factor in providing the necessary supply of war materials.

Armies and navies are useless without the tools of battle. Materials and workers must be moved to and from industrial plants to produce these tools. The need for maximum productive capacity makes such transportation essential to the war effort. The "maintenance front" for generators, motors, and control on locomotives, cars, and coaches must be one that utilizes all available facilities expeditiously. There can be no overestimating the importance of this action or the seriousness of the situation. The maintenance of electrical apparatus on rolling stock requires planning, supervision, and execution that will assure continuity of service from every piece of equipment available.

Adequate Maintenance Needed

The steadily mounting demand for transportation has produced an acute condition, both on the city transit systems and the railroads. The total number of passengers handled by city properties in 1942 was 24 per cent above the figure for the year 1941. In the case of the railroads, passenger traffic attained an all-time record as measured by passenger miles. In 1942 (11 months) the increase was 80 per cent in comparison with a like period in 1941. A corresponding figure for the step-up in freight ton-miles is 35 per cent above the previous year. The allocations of material to build new locomotives, cars, and coaches are far below needs, and this demands maintenance standards that establish maximum availability for all existing rolling stock.

The loss of trained personnel to the armed forces and war industries contributes to the difficulties of maintaining transportation electrical apparatus. This is reflected in improper handling of equipment as well as lack of knowledge of how to make repairs. The necessity for utilizing operators and enginemen who fail to measure up to normal standards has produced a substantial increase in damaged rolling stock. Also, the number of experienced shop men is reduced, which further complicates the problem. Turnover has been stepped up, and more time is required for training new employees.

The lack of a sufficient supply of rolling stock and the constantly increasing traffic demands have established a definite need for adequate maintenance. The condition is made more serious by the decrease in the number of skilled workmen. A demand has been created for more effective planning and more dependable maintenance practices. The problem is one requiring ingenuity and resourcefulness to assure a reasonable measure of success in meeting an extremely difficult situation.

Providing Effective Electrical Maintenance

The demand for adequate generator, motor, and control maintenance requires improvements in supervision. There must be better administration and direction in order that the available man power can be used to the best advantage. It is the duty of the supervisory forces to ascertain how to get a better job done with an expenditure of the same or fewer man-hours. The time which is saved by doing a job in a more simple manner is worth as much, if not more, than if it is secured by making additions to the working force. Supervisory planning is useful as a means to avoid abusive operation of rolling stock, provide effective inspection procedures, and make full utilization of preventive maintenance.

The attainment of efficient execution of maintenance procedures, once they are established, is vital under present wartime conditions. The limited supply of rolling stock, maintenance parts, and man-hours makes it imperative that there be no waste. Those who direct the activities must follow all work closely and see that it is done in a manner conforming to practices which produce the best results. This requires a broader knowledge of progress in the industry and more attention to details than has been customary in the past. Some of the measures which are productive are keeping apparatus clean to improve operation, making accurate adjustments to insure correct

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functioning, replacing worn parts immediately to prevent possible failures, conditioning commutators promptly to avoid major repairs, using improved insulating materials to prolong the life of windings, and providing correct lubrication to assure greater dependability.

Proper Operation

Abusive operation of rolling stock should be avoided since the ultimate result is lowering the standard of service provided. A schedule may be completed and a job done at the time the damage occurs, but the loss on some future occasion is likely to be many times the immediate gain. Safety can be a justification of emergency handling, or a "net" contribution to the war effort may warrant such action.

On one city property, motor failures during the severe weather of the past winter resulted in the operation of as much as 5 per cent of the equipment with three motors instead of four. A three-motor car is overloaded and the machine operating alone severely punished. The ultimate result is an increase in total failures and the number of cars out of service. Perhaps a long-range plan to use higher-grade repair materials would avoid this situation—a procedure which is proving effective on many city transit systems.

Another burden on the maintenance personnel is a substantial increase in damaged rolling stock. This is particularly true on city properties where it has been necessary to make use of many inexperienced operators. A similar condition exists in locomotive operation because of such practices as overloading Diesel-electric engines by hauling heavy tonnages at low speeds, damaging motors on switcher engines by exceeding maximum safe-speed limits, holding on grades with power on, leaving partial power on the motors after making stops, and bringing trains in with motive power partly inoperative. The trend in damaged generators, motors, and control is upward at a time when every piece of rolling stock is needed. This is a supervisory problem —one where co-operation between mechanical and transportation or operating departments has demonstrated its value in securing maximum use of coaches, cars, and locomotives.

Effective Inspection

Regularity of inspection is a definite benefit in reducing failures and lowers the over all man-hours required for maintenance work. Road failures frequently prove costly, and measures to eliminate them are essential. Too frequent attention is often as unsatisfactory as neglect. In the former case, an excessive amount of "tinkering" produces trouble, and it is better to let the apparatus alone until attention is known to be needed. One procedure under present emergency conditions is to do more "looking" and less work. However, the examination must be by men experienced in detecting possible sources of trouble.

Results indicate that inspection offers an opportunity for supervisory studies to adjust frequency to the exact requirements of the particular location and equipment. Naturally, there is considerable variation in needs, and schedules are fixed by items requiring the most frequent attention. Consideration of each part to determine its operating time without attention will disclose if this can be made longer. Inspection of routine items may be extended, provided the condemning limit is reached after the next scheduled examination. Changes in design to extend the inspection time of parts which establish limitations have proved helpful.

Definite inspection plans are employed which divide the work into classes based on frequency. The use of a central location for all rolling stock has demonstrated its advantages. Various jobs are specialized, and, with men trained for specific duties, man-hour requirements are lowered. Also, the provision of tools to operate on a "production-line" basis decreases the time for each job. Check lists are beneficial in securing complete coverage. Each man must know his duties, and a check list gives the details of what is required. Such lists can be worked out for individual properties to meet the needs of the equipment operated.

Preventive Maintenance

Preventive maintenance—making replacements or repairs before defects occur—has been utilized with excellent results. A part of such a program that has proved advantageous is the exact determination of the cause of failures to avoid repeaters. For example, a generator or motor may break down because of a weakness within the machine, a defect in the control, or an improper operating condition. The same may be true of a control failure. If the defect is outside the particular piece of apparatus that is injured and remains undiscovered, replacement of the damaged part is almost certain to result in a recurrence of the trouble. This happens frequently on many properties and is chargeable to inadequate maintenance.

Experience has demonstrated that "repeaters" can be eliminated. An effective system on one city property is the provision of a staff of assistant equipment engineers under the direction of the equipment engineer. Definite apparatus assignments are made, and it is the duty of each assistant equipment engineer to follow continually all items under his jurisdiction. When failures occur, or

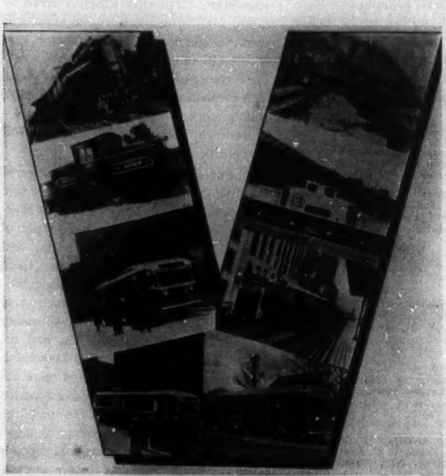

Figure 1. Electrically propelled transportation rolling stock which must be kept running to assist in the war effort

Figure 2. Checking the length of traction motor brushes

wear appears abnormal for a particular part, it is investigated and recommendations made to eliminate weaknesses that may exist. Exceptional freedom from trouble has been attained on this property.

Overhauls constitute the backbone of preventive maintenance. Under the present difficult conditions there is a decided tendency to neglect this work. Such a procedure will ultimately lead to a breakdown on the transportation system and require "heroic" measures to reestablish satisfactory operation. Results have shown that overhauls in accord with a regular schedule are essential, and they must be complete.

Keeping Apparatus Clean

Dirt can be defined as material which is foreign to any part of a piece of electrical apparatus. For example, oil or grease are classified as dirt when they are on cables, windings, and commutators. Dust particles constantly are forming a blanket over windings and confining the heat. Dust accumulates between motor fields, plugs up armature ventilating ducts, enters armature slots, and reduces V-ring creepage distances. It serves as an abrasive on commutators and in brush boxes. Its spongelike nature permits soaking up harmful fumes, moisture, oil, and acid, thus bringing them in proximity to windings. It clogs controller cams, interferes with relay operation, causes interlock misfunctioning, and contaminates oil.

As a result of the presence of dirt, motors are overloaded, throw solder, become grounded, and require rewinding. Brushes stick in brushholders, brush spring action is impaired and commutators damaged. The reduction of creepage distances causes grounds and flashing, thus leading to failures. The dust blanket overheats control coils, damages bearing surfaces, causes relays in interlocks to misfunction, impairs cylinder operation on pneumatic switches, and clogs magnet valves. Dirt in bearing lubricant is certain to result in short bearing life.

Experience shows that keeping motors and control clean is insurance of increased availability. Man-hours are saved by care and attention to a regular cleaning schedule for removal of dirt before trouble occurs. Compressed air is used for this purpose, but care should be taken that it is dry and contains no foreign material. Exposed surfaces can be wiped with clean dry cloths after compressed air is employed. Locating ventilating intakes where clean air is obtainable and applying tight fitting covers to keep dust out of the apparatus has proven helpful. Regular overhaul of motors and control affords an opportunity for much needed thorough cleanings. This is another reason why maintaining the overhaul schedule is essential.

Accurate Adjustments

Checking apparatus to see that adjustments correspond to values that give correct functioning is necessary to secure satisfactory operation. Instruction books and check lists include data indicating what is needed to insure proper performance and avoid failures. The use of gauges wherever practicable gives uniformity and limits the time consumed in making adjustments.

Maintaining brush tension accurately is needed to secure good commutation and can be checked readily with a small spring balance attached to the brush pressure finger. Commutators must be concentric and, when deviation is suspected, can be verified with a dial indicator. Unit switches need to be free from sluggishness when closing. In the case of pneumatic types, cylinder travel is required to meet specific limits and accurate adjustment of magnet valves obtained. Finger pressure on reversers and interlocks must be maintained at recommended values. Correct relay settings, especially for overload and operating currents, contribute to protection and proper functioning. In some cases gauges have been developed for verifying gaps and spring tensions, thus speeding up relay work.

Prompt Worn-Part Replacements

Worn parts demand prompt attention to assure that none are left in service except those which are certain to run until the next inspection. Otherwise, failures in service may occur which often prove costly. The inclusion of condemning limits for various wearing parts in check lists provides a convenient reference for determining when a part should be removed. Such limits are subject to variation for different localities and types of service.

Generator and motor brushes require regular replacement, since they are in constant use. Current collection and mechanical friction cause them to wear out, the life varying with differences in operating conditions. Control parts that have maximum usage and require most frequent renewals are contact tips (main switch, cam switch, and interlock), arc

Figure 3. Method of attaching fixture to grind commutator with armature running in its own bearings

horns; and arc chute sides. There may be occasional worn shunts, damaged resistor units, and, in the case of pneumatic switches, broken cylinder springs.

When abnormal wear of parts occurs, it is an indication that an unsatisfactory condition exists. This is a warning which requires corrective action. Replacement of parts which appear to be possible causes of faults has done much to minimize maintenance work. There are items, such as brushholder boxes, brushholder pins, controller bearings, switch armature shafts, and switch hinge pins that are subject to relatively slow wear. The normal procedure is to make necessary renewals during heavy inspections or when the equipment is overhauled.

Undelayed Commutator Conditioning

The appearance of commutators is an excellent indication of the condition of the complete equipment. Poor commutation is evidence that something is wrong, and the cause may not always be within the machine. The condition is a warning, and it is usually ample since commutators seldom fail suddenly, to ascertain the reason for unsatisfactory performance. Investigation and prompt correction of conditions that produce poor commutation always reduce "maintenance man-hours." It is essential that the surface of commutators be kept smooth and true as this has a direct bearing on their life and that of the brushes. Burned spots may result because of surge currents which flow during short circuits or when power and brakes are applied simultaneously. Such spots cause the brush to leave the commutator momentarily since it is unable to follow the contour as fast as the commutator rotates. Thus, the burned spot becomes progressively worse. It is necessary to smooth the surface and bring the commutator back to concentricity.

Figure 4. Fixture to hold armature firmly in position when assembling bearings and housings

One method of grinding is by means of a rig which is attached to the frame of the machine. A generator or motor is run at slow speed during the operation. When working on a motor the wheels are jacked up and power supplied from a welding generator or the trolley. In the latter case, the motor is brought up to speed by placing the controller on the first notch. Power is then shut off, and the grinding done while the speed drops to a lower value. Light cuts are made to avoid deflection of the rig and impairment of concentricity. A dial indicator serves to check accuracy, and after the operation has been completed, the slots must be cleaned and all dust blown out of the machine. A second method for grinding a commutator with the armature in its own bearings is the provision of a fixture for shop use when the armature is out of the machine. Such devices are easily constructed and support the complete armature with its bearings and enclosures.

Improved Insulation

Insulation (class *B*) is now obtainable made from asbestos, glass textiles, and mica that provides exceptional durability and is far superior to the high-temperature materials of a few years ago. The life of windings is being materially increased by making substitutions for combustible substances (class *A*), thus stepping up heat-resisting characteristics. Advantage is being taken of high-temperature insulation when making repairs to improve the performance of older electrical apparatus. This procedure increases the "capacity margin" and assists in bettering availability at a time when every effort should be made to reduce "repair man-hours."

Dipping and baking of armatures and fields at regularly scheduled intervals prolongs the life of insulation. The frequency of such work is dependent on the conditions existing in the localities where the equipment is operated. For example, there will be differences for cold, hot, wet, and dry climates. Also the dust content of the atmosphere is an important consideration. A check of the condition of the insulated surfaces is the best indication of what is needed.

Improvement in varnishes is another factor having a direct bearing on dipping and baking practices. The heat-setting or heat-reactive synthetic-resin types represent an important development in varnish making. Curing is by heat reaction rather than oxidation as occurs with the oil types. The heat hardening produces an "all through" drying even for thick films and in restricted locations. Some of the characteristics of the varnishes are excellent resistance to moisture, oil, weak acids and alkalies; good dielectric strength; long life; ready penetration; complete drying by baking; tough

Figure 5 (left). Inspecting the accelerator and contactor group of a modern street-car control equipment

Figure 6 (right). Procedure followed in tightening the contact tips of a magnetic contactor

resilient surface film; reasonable curing temperature, and ability to withstand high class *B* temperatures.

The measurement of insulation resistance is a valuable tool for "preventive maintenance" and is usable to save many service interruptions. It is subject to wide variations, and the factors which influence measurements must be known and viewed in relation to each other. Temperature, moisture, test voltage, and the length of test are the important items to be considered. Standards can be fixed for various pieces of apparatus and measurements taken at regular intervals. When unsatisfactory values are observed, this is a warning that corrective action is needed. The measurement of insulation resistance will also assist in determining dipping and baking frequency.

Correct Lubrication

Care is required in the selection of the correct lubricant for various pieces of apparatus in order to secure satisfactory results. In the case of waste-packed axle and armature bearings, straight mineral oils appear best. Summer grades are required during hot weather and winter grades when it is cold, the suggested dividing line being 40 degrees Fahrenheit. This assures a more nearly constant rate of feed, since viscosity is the determining factor. Packing must be correct with a vertical wool yarn wick adjacent to the journal or shaft and a tightly filled box to hold the wick in place.

Gears and pinions require special attention to assure proper operation. The lubricant which is used must maintain its consistency and contain no nonlubricating components, have exceptional adhesive properties, be of sufficient body to withstand high tooth contact pressure, have freedom from acids, be applied easily, have enduring qualities and possess noise-deadening characteristics. Possibly the properties of stickiness and load-carrying ability are most important. There is need for the careful selection of summer and winter grades where wide temperature variation occurs.

It has been found that grease does the best job in lubricating antifriction bearings. Occasionally such bearings are allowed to run too long without lubrication, but more often the attention received is too frequent. The result of overgreasing is an increase in the operating temperature. Tolerances are exceptionally close and are comparable with those in fine time pieces. Bearings must be kept clean, since dirt means damage. When greasing, take precautions to keep the cover on the grease can, use a clean instrument to remove grease from the can, and avoid overgreasing. Housings should be one-fourth to one-half full. In the case of a new bearing, the housing should be one-third full, but the bearing space should also be filled. A relief or drain plug for use when greasing will prevent the overgreasing danger. It is desirable to apply such plugs on older type bearings when machines are being overhauled.

Lubrication of control apparatus requires restraint in the use of the "oil can." Some pieces of apparatus, such as master controllers, may be equipped with bearings requiring lubrication while others have the oilless type. For example, a typical locomotive controller requires lubrication for the upper and lower bearings of the main drums, the reverse drum bearing, and the main handle bearing pins. On the other hand, the modern street car master controller needs no lubrication. Pneumatic switch cylinders must be oiled, but the hinge pins and armature shafts of magnetic contactors require no lubrication. Strict adherence to instructions which can be included in check lists produces the best results.

Progress in Realizing Adequate Maintenance

The procedures which have been discussed give a general outline of the best means for obtaining intensified use from electrical equipment of transportation rolling stock. Many properties are benefiting from proper operation, effective inspection, preventive maintenance, keeping apparatus clean, accurate adjustments, prompt worn-part replacements, undelayed commutator conditioning, improved insulation, and correct lubrication. The availability of generators, motors, and control is being stepped up in an endeavor to keep pace with the demand. The results are creditable as a contribution to an "all-out" effort to provide essential transportation. There is need for more general use of all factors which improve maintenance efficiency in meeting the present emergency. There may be other profitable ideas which still lie dormant, and it is to be expected that the future will bring further progress.

A New Type of Adjustable-Speed Drive for A-C Systems—II

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Synopsis: The preceding paper of this series[1] presented the theory and principles of operation of a new type of a-c motor and its control circuit. This system of speed control has the advantage of being economical from the standpoints of initial cost, efficiency of operation, and ability to correct power factor. However, these advantages are not realized unless the machine is suitably designed and its control circuit properly adjusted to supply the necessary voltages and currents. The purpose of this paper is to explain the design proportions and the adjustments of the supply voltages to provide for optimum operating characteristics.

THE characteristics of this adjustable-speed motor can be altered over a wide range by adjustment of either the magnitudes or phase relationships of the applied voltages, or by changes in the construction of the motor windings. While it may be desirable to make alterations to obtain certain characteristics, the more general problem is to make adjustments that will provide the best overall characteristics for all speed settings. As an example, it is usually desirable to operate a motor with highest possible efficiency, but, with this machine, minimizing armature circuit resistance may ruin the speed-regulation characteristics. Thus the design proportions enter the problem from several angles, and criteria of design should not be based on one feature of the motor without regard to all consequences resulting therefrom.

In this particular system as in other adjustable-speed systems there are certain requisites that must be fulfilled to make the system practical. These requisites are as follows:

1. The design and adjustments of the machine should be such that it will operate satisfactorily for all speed settings.
2. The design and adjustments should be such that the best speed regulation is secured.
3. Throughout the load range of each speed setting currents should not be excessive. This is important in this machine because of the possibility of large no-load currents as well as large load currents.
4. The full load armature current should be approximately in phase with the armature voltage so that maximum capacity is obtained from the machine.
5. The efficiency should be high.
6. Commutation should be satisfactory.

Prevention of Overheating at No-Load

If the voltages supplied to the field and armature of this motor are adjusted to the proper phase difference at the highest-speed setting, no danger will result from the standpoint of armature heating at lower-speed settings. However, the reverse is not true. For if the phase of the field voltage relative to the armature voltage be adjusted for maximum power-factor correction at no-load for low-speed settings, the armature will overheat when the motor is operated at high speeds.

These phenomena can best be seen from Figure 1 which shows

(*a*). A circuit diagram used in supplying the motor.

(*b*). A vector diagram of the motor itself when adjusted for high-speed operation.

The dotted lines shown are for the same field adjustments (magnitude and direction of field voltage with respect to armature voltage), but with a lower-speed setting. When the motor armature is supplied with the voltage E_a, and the time phase of the field voltage is such that the flux ϕ crossing the air gap lags the armature voltage by α degrees, the generated voltage in the armature must have a locus along the line *ad*. The locus of this generated voltage must be parallel to the flux vector ϕ. The generated voltage must be of sufficient magnitude such that when it is added vectorially to the armature impedance voltage I_aZ_a the sum is equal and opposite to the applied voltage E_a shown as the distance *oa* for this high-speed setting. The voltage I_aZ_a due to the armature impedance is shown as the distance *op*.

When load is applied to the machine, the speed drops slightly. The generated voltage *ap* decreases slightly causing the impedance voltage drop *op* in the armature to shift clockwise with a corresponding shift in the resistance voltage drop of the armature shown here as the distance *oy*. Since the armature current *ox* is in time phase with the armature resistance voltage, an increase in motor load will likewise cause a shift of *ox* in a clockwise direction until the torque is sufficient to supply the load. This torque is equal to $K\phi ox \cos(\theta+\alpha)$ where K = the torque constant, *ox* is the armature current associated with the resistance voltage drop *oy*, and θ is the angle between the armature current *ox*, and the armature voltage *oa*. When load is removed, the process is reversed, and the impedance triangle *oyp* revolves counterclockwise until at no-load the I_aR_a vector *oy* and the I_a vector *ox* are perpendicular to the flux vector ϕ. For this condition the torque developed is zero. Thus for zero developed torque or true no-load on the motor, the armature current vector *ox* will always be perpendicular to the flux vector ϕ. When the voltage E_a supplied to the armature of the motor is reduced to a value *oa'* as shown in Figure 1*b*, the speed decreases, and the no-load current remains perpendicular to the flux vector ϕ. Since the angle α was not changed, the no-load value of the armature impedance voltage must be less. The new vector relations are defined by the dotted lines—that is, with an armature voltage *oa'* the generated voltage will be *a'q*, the armature impedance voltage *oq*, the armature resistance voltage *ow*, and the corresponding no-load armature current *os*. Thus a reduction of speed caused by reducing armature voltage is associated with a reduction of no-load current, and vice versa. The phase of the field voltage with respect to the armature voltage must be adjusted so that the angle α between the flux vector and the armature voltage is sufficiently small to limit the no-load armature current for the high-speed setting to a value equal to or less than the rated current of the armature.

Adjustments for Minimum Speed Regulation

The preceding paper of this series showed that the speed of this motor decreases with increase in torque. Also, that the power factor becomes more lagging with increase in load. Change in speed with increase in load is usually undesirable in the adjustable-speed motor.

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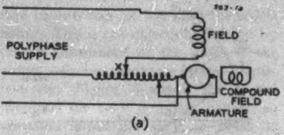
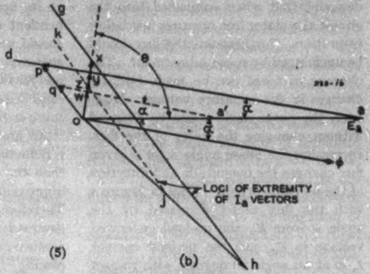


Figure 1

(a). The motor and supply circuit

(b). Vector diagram of the motor for two values of applied voltage



It is therefore desirable to make the speed regulation a minimum. Proportioning the armature circuit resistance and armature circuit reactance makes this possible. The proper proportions can be derived from the vector diagram of the motor shown in Figure 2.

The expression for the torque developed by this motor is

$$T = K\phi I_a \cos(\theta+\alpha) \qquad (1)$$

where θ is the power-factor angle of the armature circuit considered, plus for leading power factors and minus for lagging power factors. K is a constant, and α is the angle between the flux vector ϕ and the voltage vector E_a.

By the law of sines

$$\frac{E_g}{\sin(\beta+\theta)} = \frac{I_aZ_a}{\sin\alpha}$$

Consequently

$$E_g = \frac{I_aZ_a \sin(\beta+\theta)}{\sin\alpha} \qquad (2)$$

But

$$E_g = K'\phi N$$

where N is the speed of the motor and K' is a constant. Solving for N and substituting for E_g from equation 2, the speed

$$N = \frac{E_g}{K'\phi} = \frac{I_aZ_a\sin(\beta+\theta)}{K'\phi\sin\alpha} \qquad (3)$$

Equations 1 and 3 provide expressions of torque and speed as functions of the variables θ and I_a. From the vector diagram and the law of sines

$$\frac{I_a}{\sin\alpha} = \frac{I_{ab}}{\sin(180^\circ-\alpha-\beta-\theta)}$$

$$= \frac{\dfrac{E_a}{Z_a}}{\sin(180^\circ-\alpha-\beta-\theta)}$$

since

$$I_{ab} = \frac{E_a}{Z_a}$$

Therefore

$$I_a = \frac{E_a\sin\alpha}{Z_a\sin(180^\circ-\alpha-\beta-\theta)} \qquad (4)$$

Substituting equation 4 in equation 1, the expression for torque in terms of the one variable θ is obtained.

$$T = \frac{K\phi E_a\sin\alpha\cos(\alpha+\theta)}{Z_a\sin(180^\circ-\alpha-\beta-\theta)}$$

Differentiating

$$\frac{dT}{d\theta} = \frac{-K\phi E_a\sin\alpha\cos\beta}{Z_a\sin^2(180^\circ-\alpha-\beta-\theta)} \qquad (5)$$

Substituting equation 4 in equation 3

$$N = \frac{E_a\sin(\beta+\theta)}{K'\phi\sin(180^\circ-\alpha-\beta-\theta)}$$

Differentiating

$$\frac{dN}{d\theta} = \frac{E_a\sin\alpha}{K'\phi\sin^2(180^\circ-\alpha-\beta-\theta)} \qquad (6)$$

But

$$\frac{dN}{dT} = \frac{dN}{d\theta}\,\frac{d\theta}{dT} = \frac{E_a\sin\alpha}{K'\phi\sin^2(180^\circ-\alpha-\beta-\theta)} \times \frac{-Z_a\sin^2(180^\circ-\alpha-\beta-\theta)}{K\phi E_a\sin\alpha\cos\beta}$$

$$\frac{dN}{dT} = \frac{-Z_a}{K'K\phi^2\cos\beta} = \frac{-Z_a^2}{K'K\phi^2R_a} \qquad (7)$$

$$\frac{dN}{dT} = \frac{-R_a}{KK'\phi^2} - \frac{X_a^2}{KK'\phi^2R_a}$$

From equation 7 it is evident that the slope of the speed-torque curve is a function of the flux ϕ and the impedance of the armature circuit Z_a. Both of these are essentially constant for all conditions of load. Therefore the slope of the speed-torque curve is a straight line. Experiments show this to be true. Further analysis of this equation reveals that to make the slope small (low-speed regulation) the flux ϕ should be large and the impedance of the armature circuit Z_a should be made as small as practicable. To determine the armature impedance angle β that will give a minimum slope to the speed-torque curve, equation 8 can be differentiated with respect to R_a. Let S equal the slope of this curve, then

$$\frac{dS}{dR_a} = \frac{-1}{KK'\phi^2} + \frac{X_a^2}{KK'\phi^2R_a^2}$$

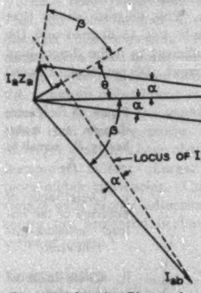


Figure 2 (above). Phase relations of currents and voltages in the vector diagram

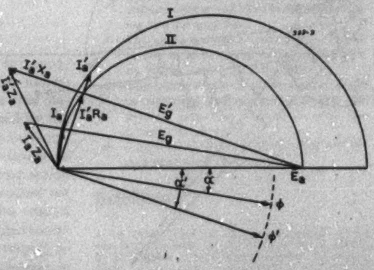


Figure 3 (right). Vector representation of the effect of a change in the angle α

I. Circle locus of extremity of the no-load armature-current vector

II. Circle locus of extremity of the no-load I_aR_a voltage vector

When

$$\frac{dS}{dR_a} = 0$$

$$R_a = X_a \qquad (9)$$

and cos β which is equal to

$$\frac{R_a}{Z_a} = 0.707$$

or

$$\beta = 45^\circ$$

Equation 7 shows that best speed regulation is obtained when the flux is large. Therefore the field circuit should be so designed that when connected into the circuit the stator iron operates just below saturation. Furthermore, this flux should be unchanged by speed adjustment. The changes in speed can be made best by changes in the armature voltage. However, the phase angle α can be changed without changing the value of the flux. Increasing the phase angle α for a given flux changes the magnitude and direction of the no-load current. Thus in Figure 3 with the flux vector displaced by the angle α from E_a, the no-load generated voltage is E_g, and the no-load current I_a is at an angle 90 degrees with respect to the flux vector. If the flux is held constant and the phase angle α shifted to the angle α', the generated voltage will be changed in magnitude and direction to the position of E_g'. This causes an increase in the armature impedance voltage from I_aZ_a to $I_a'Z_a$. The armature current is likewise increased from I_a to I_a', and the change in direction of this true no-load current corresponds to the change in direction of the flux vector. Since the no-load current vector is always perpendicular to the corresponding flux vector, it is also perpendicular to the corresponding generated voltage vector. Since the vector I_aR_a must be perpendicular to the vector E_g at no-load, the intersection of the vector I_aR_a with the vector E_g must be located on a semicircle the diameter of which is E_a. The extremity of the no-load armature current vector for different values of the angle α must also fall on a semicircle the diameter of which is coincident to the vector E_a, and of magnitude E_a/R_a units in length. Thus the magnitude of the no-load armature current for a given motor operated at its normal field strength will be dependent on the angle α. *This angle α should never be larger than that value required to make the no-load armature current for the highest-speed setting equal to the rated armature current. Larger angles will cause overheating at no-load.*

An analysis of equation 7 shows that a reduction of X_a to an ohmic value less than the ohmic value of R_a results in an improvement in the speed regulation. Therefore the armature circuit should be designed with the lowest leakage reactance practicable. This can be done by placing short-circuited compensating turns on the stator that are well coupled with the armature. These short-circuited turns should be well distributed, preferably in all slots. They should be made from copper straps or bars to save space and should be located as near to the armature conductors as possible. Having made the leakage reactance of the armature circuit as low as possible, the resistance of the armature circuit should be made equal to the ohmic value of the armature circuit leakage reactance. As shown in equation 9, this is necessary in order to obtain good speed regulation. If the leakage reactance of the armature circuit is greater than the resistance, the motor should have an external resistor connected in series with the armature in order to obtain the best speed regulation. The motor will then have a characteristic armature impedance such that the angle β is equal to 45 electrical degrees. The vector diagram for such a motor is shown in Figure 4. This diagram shows that when the angle β is 45 degrees and the angle α is sufficient to make the no-load armature current 100 per cent of rated value, the application of load causes the current to shift in direction until at full load the armature current op is directly in phase with the flux vector ϕ. Thus when the armature circuit resistance and reactance are made equal, the motor can correct power factor at no-load to the full extent of its rating, and under conditions of full load its current will be in phase with its flux giving maximum torque per unit of armature current.

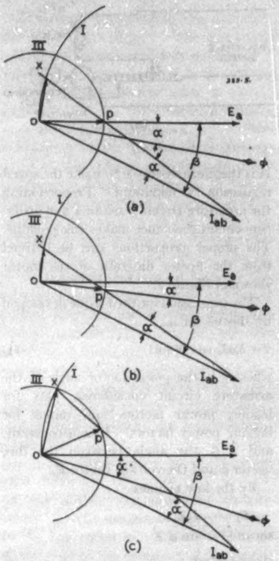


Figure 5. Vector diagram for armature circuit of the motor

(a). Angle α is 10 electrical degrees

(b). Angle α is 6 electrical degrees

(c). Angle α is approximately 16 electrical degrees

I. The locus of the extremity of the no-load armature-current vector

III. The locus of the extremity of the no-load I_aR_a voltage vector

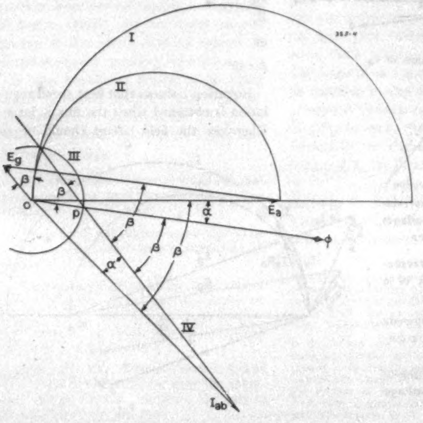


Figure 4. Vector diagram for motor having R_a equal to X_a

I. Circle locus of extremity of the no-load armature-current vector

II. Circle locus of extremity of the no-load I_aR_a voltage vector

III. Circle defining the 100 per cent value of armature current

IV. Locus of the extremity of the armature-current vector for different values of load

Motors of this type can be designed so that the resistance and reactance of the armature circuit are approximately equal. If experiments indicate that the resistance is lower than the reactance, the addition of a small amount of resistance to the armature circuit may be desirable. For frequencies above 60 cycles, this may be necessary. If the motor is to be designed for frequencies less than 60 cycles, say 25 cycles, the reactance of the armature circuit may be less than the resistance. This is not objectionable, and by proper adjustment of the angle α the motor can be made to operate satisfactorily. Figure 5 shows the vector diagrams for such a motor having an armature impedance angle β equal to 25 degrees. Figure 5*a* shows that if the machine is adjusted to take a 100 per cent power-factor armature current at full load, it cannot correct power factor to the full extent of its rating as was possible when the angle β was 45 degrees. Figure 5*b* shows the diagram of the same motor as illustrated by Figure 5*a* except the angle α is made slightly less so that the armature will develop maximum torque with rated current. There is little difference in these adjustments since the power factor of the full load current is very nearly unity in both cases. More leading current at no-load with this same motor can be obtained by increasing the angle α as is shown in Figure 5*c*. The objections to this adjustment are evident from the diagram. The maximum available torque has been reduced, and consequently the horsepower rating of the machine with this adjustment is less. Thus if the armature circuit resistance is greater than its reactance, adjustment of the angle α should be such as to give conditions illustrated in Figure 5*a* or Figure 5*b*, but not those of Figure 5*c*.

Determination of Phase Angle of Armature and Field Voltage

The method of determining the proper phase angle between the armature and field voltage can best be explained on the basis of the vector diagram of Figure 5*a*. This method applies to any motor of this type having an armature circuit reactance equal to or less than its resistance. Armatures not so proportioned should have additional resistance connected in series with them to give such proportions. The steps in the procedure of making proper adjustments are as follows:

1. Determine the value of the standstill armature current I_{ab} for maximum armature voltage E_a. This can be obtained from low-voltage determinations of the armature impedance.

2. Draw the vector E_a as a reference for the maximum armature voltage to be applied to the motor and its corresponding standstill current I_{ab} at the proper angle β with respect to E_a.

3. Draw the circle locus I of the no-load armature current for different values of the angle α so that its diameter is coincident with the vector E_a and its length is E_a/R_a units in magnitude.

4. Using o as a center, draw the circle locus III of the 100 per cent value of the armature current.

5. Determine the locus of the extremity of the armature current vector by drawing a line through the extremity of the vector I_{ab} and the 100 per cent power-factor armature current op.

6. Extend the armature current locus to the no-load armature current circle. Determine intersection at X.

7. The proper value of the angle α between the flux vector ϕ and the armature voltage E_a can now be determined from the angle made between the lines representing the standstill armature current and armature current locus (the angle between I_{ab} and the line xp extended).

8. Apply maximum values of armature and field voltages to the motor such that the field voltage is approximately 90 degrees minus α ahead of the armature voltage.

9. With the motor running at no-load change the phase relation of the field and armature voltage by proper selection of tap x on the adjusting transformer until the armature current has a value corresponding to that of ox shown in Figure 5*a*.

This provides proper adjustment of the control circuit supplying the motor.

Efficiency

When the motor is adjusted as described in the preceding section, it will take a full load current at 100 per cent power factor. Such an adjustment will give optimum full load efficiency. Departures from these adjustments are apt to cause excessive currents because of low power factors, and damage to the machine may result from overheating. The full load copper losses of this machine are somewhat larger than those of a d-c motor of the same rating. The iron losses and the loss in the armature coils undergoing commutation tend to make the machine less efficient than the d-c motor. Efficiencies of 70 per cent have been obtained on two horsepower motors. Larger motors will have higher efficiencies. Curves of efficiency with proper adjustments on this motor are presented in Part I of this series of papers.[1]

Commutation

Commutation on this motor is comparable with that of the a-c series motor. The causes of brush sparks on this machine are essentially those caused by the inductance of the coils undergoing commutation and the induced voltages in those coils because of transformer action from the field. Since the brushes are located on a magnetic axis with respect to the stator field, they are located essentially in a neutral plane with respect to this field. Since the field is pulsating in nature and not rotating as in other polyphase a-c machines, it is much easier to obtain sparkless commutation on this motor, than on a motor which has its armature subjected to a rotating field flux.

There is a shift of flux in space with load on this motor because of armature reaction just as there is in a d-c motor. Consequently good commutation is obtained by shifting the brushes backward slightly with respect to the direction of rotation. This is satisfactory for small motors. Larger machines should be provided with interpoles.

The effect of the transformer voltages induced in the armature coils undergoing commutation can be made small in this motor by designing the armature for operation on low voltage. The control circuit makes this feature possible. By adapting a low-voltage armature to a high-voltage supply, by use of the autotransformer, and by shifting brushes slightly good commutation may be obtained.

List of Symbols

E_a—The voltage applied to the armature.
E_g—The effective value of the generated voltage in the armature.
I_a—The effective value of the armature current expressed in amperes.
I_{ab}—The effective value of armature current expressed in amperes when the motor is at standstill.
K—Torque constant of the motor.
K'—Generated voltage constant of the motor.
R_a—The equivalent resistance expressed in ohms of the armature and compensating winding.
S—Slope of speed-torque curve.
T—The developed torque of the motor expressed in pound-feet.
x—The point on the adjustable transformer at which the field is connected.
X_a—The equivalent reactance of the armature and compensating winding expressed in ohms.
Z_a—The equivalent impedance of the armature and compensating winding expressed in ohms.
α—The phase angle expressed in electrical degrees between the applied armature voltage vector and the vector representing the air gap flux.
β—The angle having a cosine that is equal to the ratio of the armature resistance R_a to the armature impedance Z_a.
θ—The phase angle or power-factor angle in electrical degrees between the impressed armature voltage E_a and the armature current I_a.
ϕ—The total flux per pole crossing the air gap expressed in maxwells.

Reference

1. A NEW TYPE OF ADJUSTABLE-SPEED DRIVE FOR A-C SYSTEMS, A. G. Conrad, S. T. Smith, P. F. Ordung. AIEE TRANSACTIONS, volume 62, 1943, January section, pages 7–10.

High-Voltage Power-Transformer Design

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Synopsis: A core-type power-transformer design with shielded high-voltage winding and graded insulation has been developed for application in the higher-voltage classifications, and up to the present time more than one-quarter million kilovolt-ampere capacity of this kind of transformer has been manufactured and placed in service. The design employs a new form of construction for high-voltage power transformers which has been called "distributed concentric" construction. Outstanding characteristics of the distributed-concentric-type transformer include:

1. Reduced inherent reactance resulting in a reduction of critical materials for a given specification.
2. Ready adaptability to forced oil flow directed through the winding ducts resulting in a reduction of critical materials for a given thermal capacity.
3. Strategic distribution of voltage stress throughout the dielectric structure.
4. Simple and stable mechanical construction of windings, insulation, and shield.

POWER transformers for service in the higher-voltage transmission systems generally operate with grounded-neutral star-connected high-voltage windings, and the core-type transformer design described herein has been developed principally for this type of application.

In most core-type power transformers, as usually manufactured on this continent, the high-voltage winding of each core leg comprises the familiar stack of horizontal disc coils assembled over a conventional-type low-voltage winding. It is common practice to provide shielding and grade the insulation of these disc-coil stacks when designed for star connection and grounded neutral operation at the higher-voltage levels.[1] In general this shielding is secured by enveloping the high-voltage stack with a number of line potential shields which are so shaped and disposed as to supply each individual disc coil with its required charging current, as illustrated in Figure 1.[2] Grading of the major insulation in these transformers is ordinarily attained by special series-multiple connections of the high-voltage winding, in combination with variable diameter of individual disc coils, also illustrated in Figure 1.

In the core-type design under consideration, termed "distributed concentric" design, the high-voltage winding of each core leg comprises a group of vertical helical coils which are concentrically distributed between a line potential cylindrical shield and the low-voltage winding, as illustrated in Figure 2. In general the high-voltage coils of each leg are series connected between line and neutral terminals and progressively spaced from the low-voltage winding and grounded end surfaces in relation to the individual coil potentials from ground. With this distributed arrangement of the high-voltage winding, adequate shielding is secured with a single cylindrical shield on each leg of the core, and the several coil-to-coil insulations not only serve individually as internal winding insulation but also function in series as graded major insulation to the low-voltage winding and ground.

I. Evolution of Design

From the viewpoint of shielding, the distributed concentric construction employs, in effect, a concentric series of relatively long and narrow "barrel" coils which are progressively disposed between the two plates of a condenser, an obvious general arrangement suggested in principle many years ago as a means of shielding.[3] As usual, however, the gap between principle and practical application is not readily bridged. In this regard, it is well known from service experience with ordinary single barrel applications in power transformers, that conventional-type barrel-coil structures without radial spacers tend to break down between turns under conditions of short circuit. Progressive arrangements of conventional-type barrel coils are commonly used in high-voltage testing-transformer design, but here there is no mechanical problem.

According to responsible engineers[*] recently escaped from Europe, the Allgemeine Elektricitäts-Gesellschaft have used power-transformer-winding arrangements in Germany within the past few years which comprise

1. A concentric series of barrel coils without radial coil spacers.
2. Each coil directly supported by a wound untreated paper cylinder with slit flanged ends and cooled by an outer axial duct.
3. The complete group of coils and untreated paper layers progressively wound with one set-up to form a virtually inseparable winding structure.

It is submitted, however, that such a construction is mechanically inadequate and unacceptable in respect to service conditions on this continent, since positive turn-to-turn separation is not provided, and neither secure factory clamping nor field replacement of individual coils is feasible.

The distributed concentric structure, as developed and specifically described, is offered as a new form of construction for high-voltage power transformers. In this construction positive axial separation of conductors is provided, and individual coils may be readily clamped and handled in the shop or replaced in the field as independent units—fundamental mechanical characteristics which have been standardized on this continent for many years by conventional disc-coil construction.

It will be shown that the distributed concentric transformer possesses highly desirable electrical and thermal characteristics, including low inherent reactance and ready adaptability to forced oil flow directed through the coil ducts, and the advantages of the general winding arrangement are by no means confined to the application of shielding.

II. General Description

Figure 3 illustrates some of the available high-voltage coil connections as applied to single-phase and three-phase designs. Ordinarily from four to six coils are used for each leg, corresponding approximately to a test level range from 277 kv to 576 kv respectively. In general top-to-bottom outside coil crossovers are cumbersome and to be avoided, particularly in the case of directed forced oil flow, and some of the connections shown are unique and have been devised to meet this condition.

Each high-voltage coil employs a new form of helical coil structure, as shown in Figure 4, in which a continuous wound-in spacer of channel section functions as the key feature. High-voltage taps are located in one or more coils of each leg and symmetrically spaced with respect to the coil center line, as illustrated in Figure 5, the tap straps being carried along the face of the coil in the axial cooling ducts and then outward through radial end ducts.

The low-voltage winding may be either

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[*] Two members of the Association of Polish Engineers in Canada who are thoroughly familiar with recent transformer practice in Germany. The names of these engineers are withheld, since their families are still in Europe.

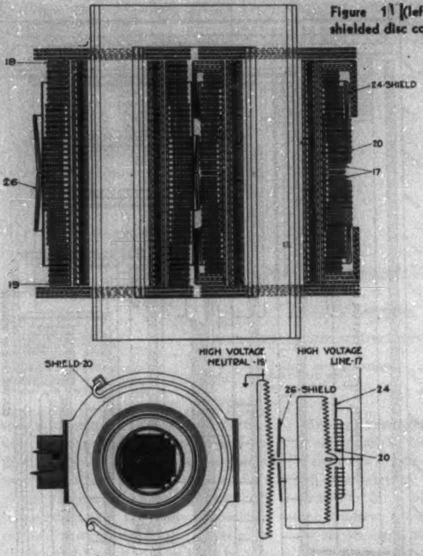

Figure 1 (left). General arrangement of shielded disc coil winding with graded insulation

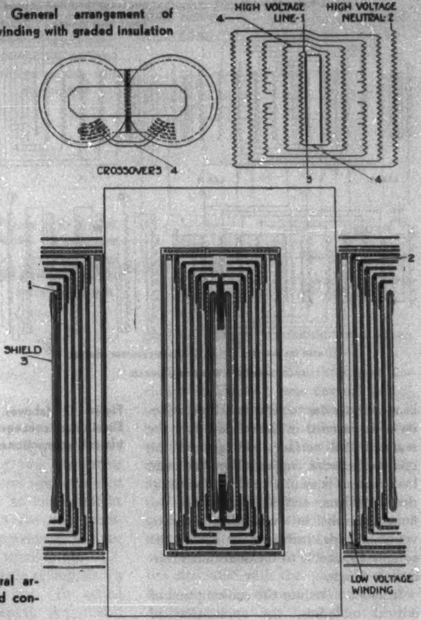

Figure 2 (right). General arrangement of distributed concentric design

of conventional type with horizontal ducts and intermittent spacers or of the same general type as the high-voltage winding. In the latter case, which is used in conjunction with directed forced oil flow, the number of coils will be substantially less than the number of high-voltage coils.

Insulation between adjacent high-voltage coils includes one winding cylinder and one axial oil duct in combination with independent flanged collars at the cylinder ends, as illustrated in Figure 4. The end insulation structure is designed around the coil-to-coil insulations, and the progressive radial end ducts are used for bringing out coil leads and tap straps.

The single shield of each core leg consists of a simple paper cylinder enveloping the outer coil and containing an embedded layer of circumferentially wound metal strip.

III. Inherent Reactance Characteristic and Associated Reduction in Critical Materials

The distributed concentric design has low inherent reactance resulting from the radial distribution of high-voltage ampere turns through the major insulation. This characteristic is illustrated in Figure 6, which compares the effective leakage width of two approximately equivalent hypothetical designs; one of the distributed concentric type and the other of conventional disc-coil construction. It will be seen that the reactance of the latter is approximately proportional to 5.5, a value more than 40 per cent greater than the corresponding figure of 3.9 for the distributed concentric design.

In this illustrative comparison, refinements in reactance calculation have been neglected for the sake of clarity, and the reactance ratio would vary somewhat from that shown, depending on the actual rating under consideration. It will be apparent to the transformer designer, however, that in general the distributed concentric construction employs a core some 20 per cent smaller in section than that required by conventional disc-coil construction for a given reactance and specification.

In addition to reducing the consumption of core steel by about one fifth, the decreased core dimensions result in a smaller volume of over-all physical structure with corresponding reductions in tank plate, clamp structure, oil, and insulation, and the total weight of critical materials is reduced by approximately one tenth. Thus, it is evident that the low inherent reactance characteristic of the distributed concentric design is of considerable practical significance, particularly under present wartime conditions when the necessity for conservation of critical materials is paramount.

IV. Thermal Characteristics and Associated Reduction in Critical Materials

The outer surface of each high-voltage coil is directly exposed to vertical oil ducts and heat flow from conductors is principally outward, although some loss is transmitted through the foundation cylinder. Thus, for any horizontal section taken through a coil, the temperature rise of each strand above local oil is readily determined by solution of an elementary series–multiple thermal circuit, once the thermal constants are established. The constants for solid insulation are well known, and a series of experimental direct-current heat runs indicated that the continuous spacer flanges, extending into the axial duct, only increase the oil-film constant of the vertical surface by about one degree for the usual design proportions. This surprisingly small effect of the spacer projections

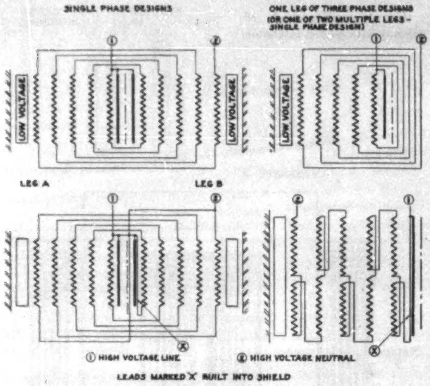
SINGLE PHASE DESIGNS
ONE LEG OF THREE PHASE DESIGNS (OR ONE OF TWO MULTIPLE LEGS – SINGLE PHASE DESIGN)
①
②
LOW VOLTAGE
LOW VOLTAGE
LOW VOLTAGE
LEG A
LEG B
①
②
②
①
ⓧ
ⓧ
① HIGH VOLTAGE LINE
② HIGH VOLTAGE NEUTRAL
LEADS MARKED 'X' BUILT INTO SHIELD

Figure 3 (above). Distributed concentric coil connections

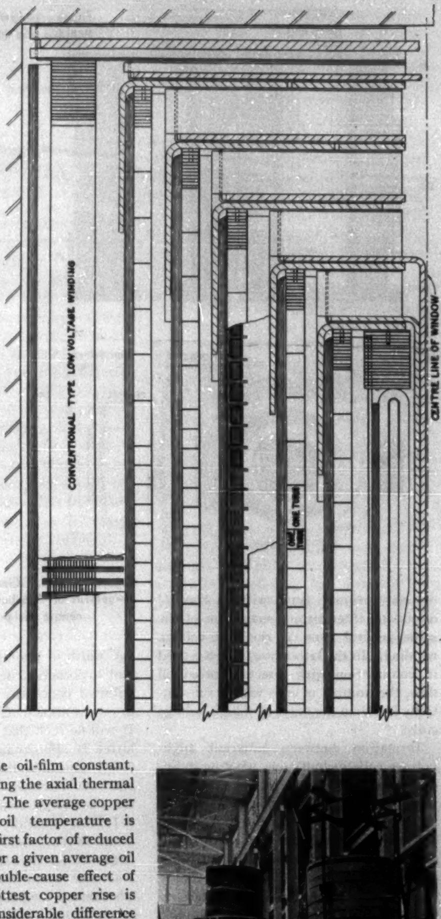

Figure 4 (right). Section through end portion of a typical structure

is apparently due to relatively high velocity eddy currents in the oil flow over the serrated coil surface. In general the relation between copper-oil gradient and load current is similar to that of conventional designs, and accepted rules defining permissible overloading of conventional-type transformers should be equally applicable to the distributed concentric design.

In order to reduce the consumption of critical materials, the application of external forced-flow heat exchangers to large transformers is rapidly increasing. With this type of cooling, a further reduction in materials results from directing the existing forced oil flow through the coil ducts, a condition readily attained in the distributed concentric construction merely by adding a simple pressboard barrier, as illustrated in Figure 7. It is evident from this drawing and Figure 4, that the inherent construction of the distributed concentric design is uniquely suited for directed-flow cooling, as contrasted with the unduly elaborate means obviously necessary in the case of conical-shaped stacks of horizontal disc coils.

The increased cooling effect of directed flow as compared to natural flow is illustrated by Figure 8, in which average and hottest copper rise above average oil are shown for both types of cooling. The curves are plotted for a typical coil, in which the increased insulation of the buffer wire is overcompensated by doubling the cooling surface per watt of the end turns, as shown in Figure 4, and the hottest strand is located in that turn of main wire which is adjacent to the upper buffer portion of the coil. Directed flow reduces the rise of the hottest strand above mean oil temperature in two ways, first by lowering the oil-film constant, and second by reducing the axial thermal gradient in the oil. The average copper rise above mean oil temperature is only affected by the first factor of reduced oil-film constant. For a given average oil temperature, the double-cause effect of directed flow on hottest copper rise is indicated by the considerable difference between curves B and B' of Figure 8, and the single-cause effect on average copper rise is indicated by the lesser difference between curves A and A'.

This substantial reduction in hot spot—average oil gradient resulting from directed flow—is important, since the actual thermal capacity of a transformer is determined by permissible hot-spot temperature. For example, a given transformer rated at 100 per cent continuous load with natural internal circulation and a given external cooler could be rerated by the addition of a simple pressboard barrier to approximately 120 per cent continuous load at the same hot-

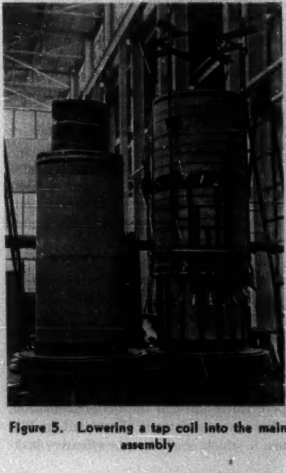

Figure 5. Lowering a tap coil into the main assembly

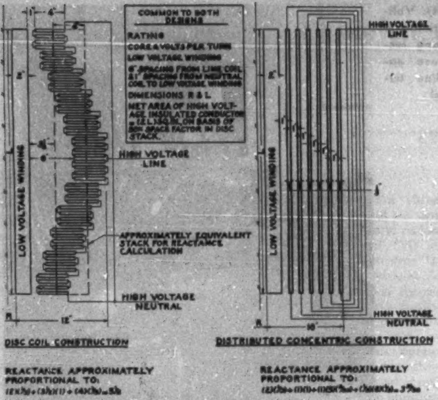
1"
4"
6"
2"
½"
0"
5"
LOW VOLTAGE WINDING
COMMON TO BOTH DESIGNS
RATING
CORE & VOLTS PER TURN
LOW VOLTAGE WINDING
6" SPACING FROM LINE COIL & 1" SPACING FROM NEUTRAL COIL TO LOW VOLTAGE WINDING
DIMENSIONS R & L
NET AREA OF HIGH VOLTAGE INSULATED CONDUCTOR = (2 L) SQ.IN. ON BASIS OF 50% SPACE FACTOR IN DISC STACK.
HIGH VOLTAGE LINE
APPROXIMATELY EQUIVALENT STACK FOR REACTANCE CALCULATION
HIGH VOLTAGE NEUTRAL
R
12"
DISC COIL CONSTRUCTION
REACTANCE APPROXIMATELY PROPORTIONAL TO:
HIGH VOLTAGE LINE
HIGH VOLTAGE NEUTRAL
R
10"
DISTRIBUTED CONCENTRIC CONSTRUCTION
REACTANCE APPROXIMATELY PROPORTIONAL TO:

Figure 6 (left). Illustrative comparison of inherent reactance—distributed concentric versus disc coil construction

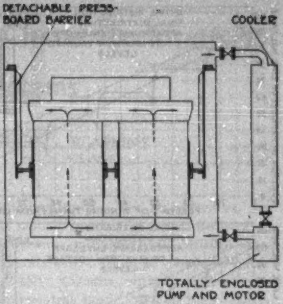
DETACHABLE PRESSBOARD BARRIER
COOLER
TOTALLY ENCLOSED PUMP AND MOTOR

Figure 7 (right). Baffle arrangement for directing forced oil flow through the vertical coil ducts

spot temperature. Conversely, directed flow would result in a reduction of approximately 15 per cent in the total weight of critical materials required for a given continuous rating. Accordingly it is evident that the ready adaptability of the distributed concentric construction to directed forced oil flow may be conveniently utilized to substantially increase the kilovolt-amperes obtained per pound of critical materials consumed.

V. Distribution of Voltage Stress

Unless some form of shielding is provided, it is well known that the initial distribution of impulse voltage along a transformer winding departs radically from uniformity, and subsequent voltage oscillations ordinarily raise the potential of intermediate winding points to values which preclude any grading of the insulation. It is clear, however, that shielding to establish a perfectly uniform initial distribution, with absolute elimination of subsequent oscillations, is neither attainable nor necessary in practical design. The degree of shielding to be used in a transformer with graded insulation is therefore a question of economic balance and is determined by that combination of compromise shielding and partially graded insulation which has minimum over-all cost.

With a vertical front impulse applied to the line terminal, the initial or electrostatic voltage distribution along the winding of the distributed concentric transformer is fixed by the combined network of radial shunt capacities and axial series capacities, including the shunt capacities associated with the physical coil crossovers. The initial voltage distribution, corresponding to application of an infinitely long rectangular wave, is shown in Figure 9 for two alternative coil arrangements. Local oscillations of transition from initial to final straight line gradient overshoot the final gradient as an axis, and maximum voltage levels throughout the winding are indicated by the approximate ceiling shown as a straight broken line. In actual design, insulation strength is provided between parts of the winding and from winding to ground, corresponding to the maximum stresses thus created by application of an infinitely long rectangular wave. Since all real waves encountered in service must be of finite length and slope, the values of insulation strength as provided correspond to the upper limit of stresses attainable for a given crest voltage.

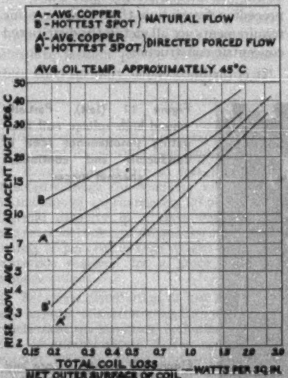

Figure 8. Effect of directed forced oil flow on copper—oil gradient

The re-entrant coil arrangement included in Figure 9 was devised principally to eliminate the objectionable outside vertical crossovers of the alternative straight-coil scheme. Examination of the respective voltage distributions, bearing in mind that the final impulse distribution also corresponds to the low-frequency gradient, will show that each arrangement requires approximately the same coil-to-coil allowances. The shunt capacities associated with the internal vertical leads of the re-entrant coil scheme are controlled by adjustment in physical width of lead conductors, and these capacities are conveniently utilized to substantially improve the initial voltage distribution as compared to that of the straight coil arrangement.

Because of the physical distribution of the high-voltage winding *through* the major insulation, it is evident that both low frequency and impulse voltage stresses, as shown in Figure 9, are advantageously distributed throughout the whole dielectric structure. Adequate control of impulse voltage on each leg of the core is readily secured with a single shield surface of plain cylindrical shape. Also greatly reduced and simplified shield insulation is sufficient, since the maximum voltage from shield to adjacent winding is ordinarily less than one third of line voltage, as compared to approximately full voltage in conventional construction.

VI. Mechanical Construction Including Shop Processing

The practicability of the distributed concentric transformer hinges on the mechanical characteristics of the physical structure, which in turn depend on specific details of construction, as illustrated in Figure 4 and explained below.

In each high-voltage coil the multiple

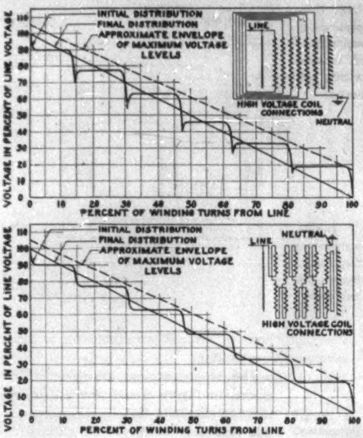

Figure 9 (left). Voltage distribution with infinitely long rectangular wave applied to line terminal

Figure 10 (right). Forming continuous channel spacer from flat pressboard strip

rectangular conductors are encased throughout their entire length by the continuous pressboard spacer of channel section, the conductors and spacer being simultaneously wound directly on a hard composition foundation cylinder. Ordinarily this spacer is $^1/_{32}$ inch thick, and the spacer flanges extend radially at least $^1/_8$ inch beyond the coil. Cross-grain pressboard end rings, in combination with the hard foundation cylinder, permit ready clamping and handling of each individual coil prior to assembly. Individual coils are progressively mounted in the main assembly as independent units, being finally supported and clamped to firm dimension by the end insulation structure which is progressively built around the independent flanged collars. The simple cylindrical shield structure is ordinarily wound on a hard foundation cylinder and also assembled in the main structure as an independent unit.

Under conditions of short circuit, the conductors of any cylindrical coil structure are subject to the formidable combination of pulsating axial force, pulsating circumferential stress, and elevated copper temperature with attendant thermal expansion. Thorough consideration of these factors and the unsatisfactory service record of conventional-type barrel coils lead to the conclusion that (1) positive axial separation of conductors, (2) firm coil clamping, and (3) freedom of coil-diameter expansion, are inescapable requirements for the prevention of conductor squirming and turn-to-turn breakdown. It is clear from Figure 4 and the preceding description that these specific requirements are all met in the distributed concentric construction.

It is further evident that all coils and parts may be disassembled and reassembled as separate independent units of the main structure. Thus, individual items, such as a high-voltage coil, could be furnished by the factory for field replacement and local damage repaired at the site without shipping the complete machine back to the shop. This feature is considered essential by most purchasers on this continent.

These fundamental mechanical characteristics, as specifically enumerated in respect to coil stability and field repair, have been standardized on this continent for many years by conventional disc-coil practice.

As further compared to conventional disc-coil construction however, the unique

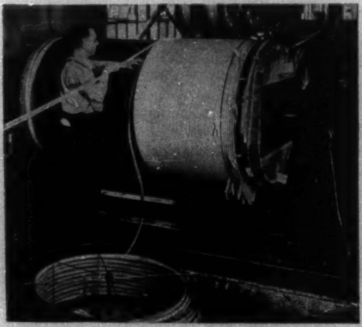

Figure 11 (left). Partially wound high-voltage coil illustrating simultaneous feed of conductors and continuous channel spacer

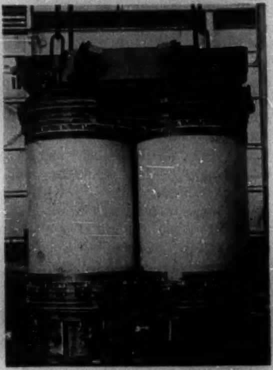

Figure 12 (right). Core and coils of single-phase 30,000-kva distributed concentric transformer, 323-kv test level

coil structure of Figure 4 comprises a solid cylindrical column of copper and insulation in which the shearing and cantilever actions of conical shaped disc-coil stacks are eliminated. Also the bearing surface between turns is continuous and uniform without the compression stress concentrations associated with intermittent radial spacers, and the entire cross section functions in the transmission of axial forces. It is further evident that the continuous-spacer feature eliminates the possibility of misaligned spacer columns, and the hazard of cutting the conductor insulation at the spacer edges. Intermittent radial spacers are used in the supporting-end insulation structure, but the continuous-bearing feature within the coil is maintained at the coil ends by the beam strength of the end ring and adjacent flanged collar combined.

Pre-forming of the continuous channel spacer from flat pressboard strip is illustrated in Figure 10. The automatic machine shown was designed especially for this purpose and normally produces channel spacer at the rate of one hundred feet per minute without an attendant. The advantages of this continuous spacer forming process from a production viewpoint are obvious, particularly when compared with the manufacture of a vast number of intermittent radial spacers which are individually sheared, punched, and assembled.

Figure 11 shows a partially wound high-voltage coil and indicates the manner in which the conductors and encasing channel spacer are simultaneously wound into the coil. It is evident that the winding process is substantially continuous, and the delays necessarily associated with intermittent spacers are eliminated.

Progressive assembly of the individual high-voltage coils as independent units is indicated by Figure 5, which shows a tap coil being lowered into a partially built structure.

VII. Summary

More than one-quarter million kilovolt-ampere capacity of the distributed concentric-type transformer has been manufactured and placed in service up to the present time. Figure 12 indicates the general external appearance of a typical core and coil assembly and illustrates the cylindrical shields, end-insulation structure, and horizontal coil crossovers.

Outstanding characteristics of the distributed-concentric-type design include:

1. Reduced inherent reactance, resulting in a reduction of approximately 10 per cent in total weight of critical materials required for a given transformer specification.

2. Ready adaptability to forced oil flow directed through the windings, resulting in an increase of approximately 20 per cent in thermal capacity of a given transformer and external cooler, or a reduction of approximately 15 per cent in total weight of critical materials required for a given kilovolt-ampere output.

3. Strategic distribution of low frequency and impulse voltage stresses throughout the whole dielectric structure, including a reduction of approximately 70 per cent in the voltage between shield and winding, the advantageous distribution of all voltage stresses resulting from the physical distribution of the high-voltage winding *through* the major insulation.

4. Simple and stable mechanical construction of high-voltage winding, insulation, and shield, including a new and improved form of helical coil structure and associated assembly which are mechanically suitable for application in large high-voltage power transformers.

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Experience With Oil Circuit Reclosers on REA Systems

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Synopsis: For three years surveys have been made of the operation of small single-pole automatic reclosing oil circuit breakers (also called "oil circuit reclosers" and "service restorers"[1]) on rural electric distribution systems financed by the Rural Electrification Administration. Results of these surveys indicate that the ratio of lockouts to operations is less than five per cent in most cases, and that there are from four to nine operations per month per breaker during the spring and summer, with each breaker controlling about 20 miles of line. The number of breaker failures and other breaker difficulties have been a small percentage of the total number of breakers installed. These surveys, together with discussions with system managers, have indicated that automatic reclosing breakers offer many advantages for rural line sectionalizing. Presently available breakers can be much improved in operating characteristics, and new sectionalizing devices are needed. This paper presents suggestions for such improvements.

THERE are now over 800 energized rural electric distribution systems financed by the Rural Electrification Administration, and these systems serve over 1,000,000 consumers. The average number of miles per system is about 470, and usually these lines radiate out of and are fed from a single supply substation. These rural lines extend out 40, 50, and sometimes over 60 line-miles from the power source. In general, the systems follow the radial design. In a few cases alternate feeders extend into the same area, but normally these feeders are not interconnected. The main feeders, two or more in number, consist of three-phase four-wire multigrounded neutral wye circuits with a nominal voltage of 7,200/12,450. Circuits, with two-phase wires and neutral, branch off from these feeders, and these "vee" circuits again split into single-phase circuits with one-phase wire and multigrounded neutral. Approximately 80 per cent of all distribution lines are single-phase 7,200 volts, and the greatest percentage of the consumer load is single phase. Outdoor unattended substations are used principally.

Individual systems will of course vary in detail from the average. Some are smaller and some larger. The larger systems usually divide the area into sections serviced by submaintenance centers. In the western mountainous regions the areas served are usually long and slender, and the lines follow the valleys. In these areas and in hilly regions, the

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The authors express appreciation to the REA co-operatives' operations division for assistance in securing information, to the system managers who furnished the data, and to the numerous REA engineers who gave assistance and suggestions, particularly M. W. Rothpletz.

road distance to outlying sections may be much greater than the line distance.

Although progress has been made in improving rural roads, in many sections of the country road conditions leave much to be desired. During wet weather some of these rural roads become impassable.

Satisfactory telephone communication facilities generally exist in the more thickly populated farming areas, but in the more sparsely settled areas communication is sometimes poor and frequently practically nonexistent.

Table I. Report of Single-Pole Reclosing-Circuit-Breaker Operation on REA-Financed Systems in Iowa From Installation Until November 1, 1940

Number of systems reporting	20
Total number of circuit breakers reported	417
Number of design 1 breaker	163
Number of design 2 breaker	254
Total reported circuit-breaker months of operation	5,098
Total number of operations reported	20,973
Total number of lockouts reported where operations were reported	271
Per cent of lockouts to operations	1.29
Total operations where operations and number of breakers were both reported	20,973
Total number of breakers where operations and number of breakers were both reported	301
Average number of operations per month per breaker	4.11
Total reported breaker failures	59
Percentage of design 1 breaker failures per 12 months, where failures and months of operation were both reported	21.8
Percentage of design 2 breaker failures per 12 months where failures and months of operation were both reported	2.04

Most systems are equipped with open-gap-protected transformers.

All of the preceding information furnishes a picture to haunt the dreams of an operating man responsible for the continuity of service consistent with good operating practice. Rural service must be good. Farmers, using electricity for food-production purposes, need and are entitled to better service than urban resident consumers. In order to provide means for improving rural service continuity, the development of carrier-current communication, hot line work, routine testing, and so forth, for use on systems described, has been carried on. A heavy share of the responsibility for continuous service, however, must be borne by the sectionalizing devices. These must isolate trouble properly with a minimum of attention, and REA has been very interested in adequate sectionalizing apparatus and methods.

Table II. Report of Single-Pole Reclosing-Circuit-Breaker Operation on REA-Financed Systems, May to November 1941

Month	Number of States Represented	Number of Systems Reporting	Number of Breakers Reported	Average Miles of Line Per Breaker	Operations Per Breaker	Operations Per Mile	Per Cent Lockouts to Operations
Systems With Arrester-Protected Transformers							
May	7	10	62	31.3	6.01	0.192	1.88
June	10	17	192	24.2	9.07	0.375	2.53
July	15	28	323	27.8	9.69	0.349	1.73
August	15	35	405	31.9	7.93	0.249	2.71
September	18	36	368	32.2	2.85	0.089	3.63
October	13	18	189	30.7	2.69	0.088	13.4*
Systems With Open-Gap-Protected Transformers							
May	7	23	428	18.4	4.24	0.230	2.70
June	10	31	580	18.0	5.97	0.332	2.31
July	11	41	662	18.9	7.60	0.402	1.73
August	13	46	684	19.7	6.70	0.340	1.55
September	15	47	697	19.5	6.50	0.333	2.50
October	12	20	336	18.6	5.37	0.289	5.00

* Due to a large number of trees breaking on one system.

In the early stages of the REA program, most systems were designed to be sectionalized with fuses. Three-shot repeating cut-outs were installed on main feeders, and single-shot cut-outs served on branch lines. Few substations were equipped with oil circuit breakers; three-shot cut-outs served at these locations. Since then, the fused system has been used very extensively, but, since experience has shown that many unnecessary outages occurred with the single-shot cut-out, the trend has been toward using fewer single-shot cut-outs and using more of the repeating type.

In 1936, 7,500-volt automatic reclosing and recycling single-pole circuit breakers were available, which were recommended for use on circuits up to 7,500 volts. These were installed on single-phase lines on a number of rural systems in conjunction with open-gap-protected distribution transformers. One object of these installations was to reduce construction costs by eliminating lightning arresters, and another object was to determine if such a device would be advantageous in decreasing service interruptions. In particular, many Iowa systems and some Ohio systems used this construction.

Experience with the 7,500-volt breaker installations proved that a device which would automatically recycle to original position after a temporary fault and would reclose two or three times, then lock out on a permanent fault, would do much to reduce system outages. However, because of the low insulation level of the device, a considerable number of breaker failures were experienced, most of these from lightning surges (see Table I). Because of these difficulties, use of this device was largely abandoned.

About 1939, stimulated by REA engineers and others, a 15-kv single-pole self-contained automatic reclosing oil circuit breaker was developed. The breaker opening time was very short (usually less than 0.1 second), and the time between all three reclosures was about three seconds and was unadjustable. Systems along the eastern seaboard decided to try out this device. In about a year's time 1,000 such circuit breakers had been installed. Each of these systems was separately studied for proper application of these devices.[3] Following a thorough study, taking into consideration the proper application of these breakers from the viewpoint of safety, accessibility,

Table III. Report of Single-Pole Reclosing-Circuit-Breaker Operation on REA-Financed Systems Which Reported the Same Number of Breakers for the Three Consecutive Months of July, August, and September 1941

	All Systems	Systems With Gap-Protected Transformers	Systems With Arrester-Protected Transformers
Number of states represented	17	9	14
Number of systems reporting	35	18	21
Number of breakers reported	450	204	246
Miles of line controlled	11,441	4,150	7,291
Average miles of line per breaker	25.4	20.4	29.6
Number of breaker operations	8,094	3,228	4,866
Total operations per breaker	18.0	15.8	19.8
Total operations per month per breaker	6.0	5.3	6.6
Total operations per mile	0.706	0.778	0.668
Total operations per month per mile	0.235	0.259	0.223
Number of breaker lockouts	208	77	131
Lockouts per month per breaker	0.154	0.126	0.177
Lockouts per month per mile	0.00607	0.0062	0.00598
Per cent lockouts of operations	2.57	2.38	2.69
Operations per lockout	38.9	41.9	37.1

co-ordination, and cost, a definite program was recommended for breaker installations so as to attain the maximum benefit from their use. This study indicated that the breaker investment would pay greater dividends when the device was located at the more remote sectionalizing points, and hence the first breakers were installed at a distance from the power source, and as additional breakers were obtained, installations were made progressively nearer to the power source. No discrimination was made as to the type of transformer protection. Existing cut-outs were left in place at sectionalizing points on the load side of the breakers and used as disconnects. Experience with these circuit breakers has been so favorable that since then many additional systems have installed such devices and REA-financed systems now have over 5,500 such single-pole automatic circuit breakers.

The first detailed survey of circuit-breaker performance was made in 1940, when the Iowa systems were asked to give their experience from the date of installation to November 1, 1940. Unfortunately, most of the systems kept no adequate records of some phases of such breaker operation, and the investigation was made more difficult by the fact that the counters of the older 7,500-volt breakers were internally placed. However, out of the replies furnished, the most adequate data were assembled as shown in Table I. It is remarkable how closely such data check with the more accurate information obtained during the next two years.

Most of the failures on the design 1 breaker were due to lightning damage to the pocket-type bushing and insulated lead. The next greatest failure cause was overload, which in turn was due to incorrect application. The design 2 breakers showed a very much lower failure rate.

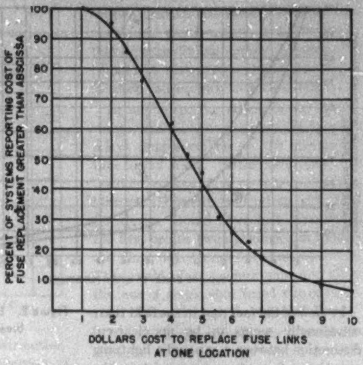


Figure 1. Estimates of average cost to re-fuse one cutout by 132 REA-financed systems with 1,337 breakers. January 1 to October 1, 1942

In order to obtain more detailed and accurate data and to cover more cases, during the 1941 lightning season all REA-financed systems with circuit breakers were asked to make breaker counter readings each month and to report the number of lockouts and the cause of each. Tables II through V give the results of this survey. (The term "Gap" means an open air gap of definite dimensions which has no means of extinguishing the flow of power follow current.)

Table IV. Report of Single-Pole Reclosing-Circuit-Breaker Operations on REA-Financed Systems, May to November 1941

Cause of Breaker Lockouts

Number of states reporting	20
Number of systems reporting	72
Total lockouts reported	773
Percentage lockouts due to:	
1. Conductor break	7.3
2. Pole break	3.1
3. Insulator failure	7.5
4. Arrester failure	4.5
5. Transformer failure	9.8
6. Trees	30.7
7. Other	21.1
8. Not known	16.0

Systems which reported the same number of breakers for the three consecutive months of July, August, and September were segregated, and Table III gives the breakdown or analysis of data for these systems.

Table IV shows the cause of all the reported lockouts, irrespective of month or system, and Table V shows the breaker difficulties experienced.

Since any failures or other difficulties with breakers were reported separately, and since systems which did not furnish operation and lockout data often did report such difficulties, the tabulation in Table V represents many more breaker installations than reported in the other tables and covers the entire year. Hence, the percentage of failures to installations could not be determined, although apparently it is very low.

During 1942, because of the gasoline and tire shortage, REA-financed systems were asked to make complete counter readings only on April 1 and on October 1 but to make lockout reports each month. In 1941 two additional manufacturers had breakers available, and some systems with these makes were included in this survey. These new makes have different characteristics and construction from the older makes, but in general the operation is the same. Table VI gives a summary of the data collected in the 1942 survey.

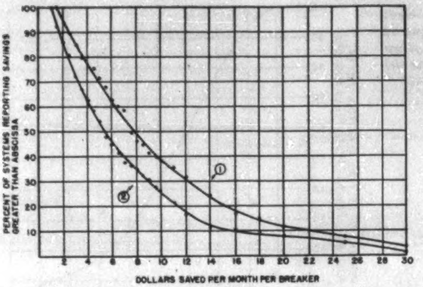
In the 1942 survey, lockouts were reported each month, and, if no report was obtained after a letter follow-up, it was assumed that no lockouts occurred. Hence, some lockouts may have been omitted, accounting for the lower percentage of lockouts to operations than in 1941.

Table V. Report of Single-Pole Reclosing-Circuit-Breaker Operation on REA-Financed Systems

Reported Difficulties Experienced With Breakers—1941

State	Number of Breakers Affected	Description of Difficulties	Probable Cause of Difficulties
Florida	5	Oil gauges did not work	Fiber gauge rod injured in shipment
	1	Breaker jacket injured	Lightning
Georgia	2	Did not reclose	Rust on trip counters
Iowa	2	Gears stripped on recycling device	Breakers improperly located on system
	1	Coil lightning protector failed	Lightning
	1	Failure to operate	Not known
Minnesota	4	Failure to operate	Lightning*
North Carolina	1	"Burned out"	Not known
South Carolina	2	Registers did not operate	Not known
Texas	1	Coil protector failed—breaker case shattered	Lightning
Wisconsin	1	Breaker case shattered	Not known
Total	21		

* These are an older model.



Curve 2 shows estimates of savings in operation and maintenance costs by 100 REA-financed systems with 1,078 breakers. Curve 1 shows estimates in savings in operation and maintenance plus savings in retail sales by 53 REA-financed systems with 507 breakers. January 1 to October 1, 1942

Figure 2. Estimates of monetary savings using breakers over previous practice

In view of the fact that in the field there occasionally seems to be no clear-cut distinction between a gap and a lightning arrester, a few errors may exist in the 1941 and the 1942 surveys in separating the systems with gap-protected transformers and the systems with arrester-protected transformers. However, errors from this cause are small and should have little effect in the over-all results as shown on the summary for the period covered. The surveys indicate that there were about 26 per cent more operations of the breaker per mile of line with gaps than with arresters in 1942 and about 16 per cent more in 1941. Lightning protection is provided only at each distribution transformer, or on about 20 per cent of the poles on rural circuits. Each unprotected pole provides a potential point of flashover, and each such flashover will probably cause an operation of the breaker. Since the arrester will usually stop follow current without breaker operation, while the gap will not, the additional operations per mile with gap-protected transformers can be expected.

Table VII shows the reported cause of all reported 1942 lockouts. Table VIII gives a summary of the breaker difficulties experienced. As in 1941, Table VIII represents more breaker installations than reported in the other tables, and covers the entire year.

Records for circuit-breaker operation have not been obtained for winter months. Experience has shown that during this period the number of operations reduces considerably. Numerous letters and reports from system managers indicate that the oil circuit breaker is very effective in preventing outages caused by unequal snow loading and tree limb contacts. Particular attention is called to the fact that these systems have been operating with reduced personnel under national defense and wartime conditions. Because of this situation an interesting aspect of the use of the circuit breaker which showed up on some systems was that tree trimming and right-of-way clearing were often deferred if other work was pressing, allowing the breakers to take care of the situation.

In the 1942 survey an attempt was also made to ascertain the monetary savings experienced by REA-financed systems using breakers. Systems were asked to furnish estimates on such savings. Figures 1 through 3 give the results of this survey. Figure 1 shows a percentage distribution of estimated savings for replacing fuses in a cut-out at one location. Experience has shown that circuit breakers usually show a greatly higher operation rate than the previous rate of fuse blowings at the same location. This may be caused by breaker operations occurring during the time which was formerly taken to replace the blown fuses. It is therefore not proper to multiply the number of breaker operations by the cost to replace fuses at a given location in order to obtain the total savings. Hence, the systems were asked to give estimates on the monthly savings effected by breakers, and these are shown in Figure 2. Figure 3 shows the estimated breaker maintenance and replacement cost. These three charts are for the period of January 1 to October 1, 1942. Maintenance costs on the breakers, now low, will probably increase as the units become older.

Correspondence and discussions with various REA system managers indicate that the continuity of service brought about by the breakers is of far more importance than the savings in maintenance and operations expenses afforded by them. It is difficult to get at any

Table VII. Report on Operation of Reclosing Oil Circuit Breakers on REA-Financed Systems April 1 to October 1, 1942

Cause of Lockouts

Number of states reporting	31
Number of systems reporting	163
Total lockouts reported	1,930
Percentage of lockouts due to:	
1. Conductor break	10.1
2. Pole break	1.9
3. Insulator failure	11.8
4. Arrester failure	5.4
5. Transformer failure	5.6
6. Trees	25.5
7. Other	18.5
8. Not known	21.2

Table VI. Summary of Survey of Single-Pole Reclosing-Circuit-Breaker Operation on REA-Financed Systems, April 1 to October 1, 1942

	All Systems	Systems With Gap-Protected Transformers	Systems With Arrester-Protected Transformers
Number of states represented	31	23	29
Number of systems reporting	162	88	99
Number of breakers reported	2,455	1,314	1,141
Miles of line controlled	55,238	26,680	28,558
Average miles of line per breaker	22.6	20.3	25.0
Number of breaker operations	98,858	53,714	45,144
Total operations per breaker	40.3	40.8	39.5
Total operations per month per breaker	6.7	6.8	6.6
Total operations per mile	1.79	2.01	1.58
Total operations per month per mile	0.298	0.335	0.264
Number ot breaker lockouts	1,840	877	963
Lockouts per month per breaker	0.125	0.111	0.141
Lockouts per month per mile	0.00555	0.00548	0.00562
Per cent lockouts of operations	1.87	1.63	2.14
Operations per lockout	53.7	61.2	46.8
Per cent consumer hours outage time	0.105	0.097	0.111

Table VIII. Report on Survey of Single-Pole Reclosing Circuit Breakers on REA-Financed Systems, 1942

Summary of Reported Difficulties

Probable Cause of Difficulties	Number of Breakers
Lightning	13
Defective or broken ratchet mechanism	7
Warped or defective lockout mechanism	8
Overload	3
Burned contacts or worn parts	4
Dirty	3
Excessive or insufficient fault current	4
Noninterruption of fault	1
Other	3
Not known	10
Total	56

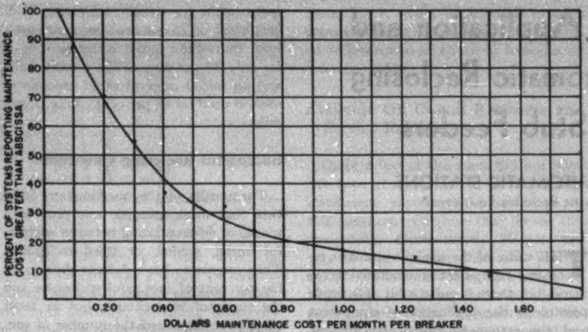


Figure 3. Estimates of breaker maintenance costs by 47 REA-financed systems with 624 breakers, January 1 to October 1, 1942

figures on the number of consumer-hours outage time saved by the breaker system over the fused system, as few of the fused systems kept any detailed records of outage time. Indications are, however, that the improvement in most cases is substantial.

These surveys and other experience have indicated that the automatic reclosing oil circuit breaker offers great possibilities for rural electric system sectionalizing. Since most of the REA lines are single phase, the greatest requirement is for single-pole breakers. Some systems, particularly those serving a large amount of irrigation load, use automatic reclosing three-pole breakers. In order to prevent voltage from appearing on an opened phase through ungrounded wye transformer banks and also to prevent single-phase power being applied to three-phase motors, it appears that three-pole breakers on feeders may become more necessary as the many small industries continue to spring up around established REA systems requiring more and more three-phase power.

The circuit breaker will probably not entirely replace the fused cut-out immediately or even in the future. In some locations, where the routine patrolling is of sufficient frequency, the fused cut-out may be more economical and the service maintained as satisfactorily as with the automatic breaker. Also, if a breaker such as described hereinafter can be obtained, single-shot fuse cut-outs can be used on short branch lines beyond such breakers.

One of the major questions in the future development of the small distribution-type circuit breaker is the question of time delay in opening. From the standpoint of protection to equipment and prevention of conductor failures caused by arc-overs, the breaker should open as rapidly as possible. On the other hand, one of the objections to the fast-opening breaker has been that occasionally a damaged distribution transformer takes a line section out of service because the breaker locks out prior to the blowing of the primary transformer fuse. Lockouts from this cause seldom occur, but, when they do, a large number of consumers may experience a long outage. Also, it is believed that time delay should afford an opportunity to co-ordinate a greater number of breakers in series and should provide better operation when picking up motor loads.

Limitations are often imposed by the power supply agency, which usually specifies the supply side fuse size on the substation. If the purpose of this limitation is to insure co-ordination with relays further back on the transmission system, it can be understood, but unfortunately in many cases the supply side fuse size seems to be specified on no particular technical basis. Proper sectionalizing depends on system fault currents and time-current characteristics of apparatus, and the supply side fuse or other device is intended to operate only for short circuit in the substation or in case the load side device fails. As pointed out by Marsh and Dodds,[3] it is not always possible to expect complete co-ordination of the substation fuse with transmission-line relays over the entire range of fault currents and still allow adequate sectionalizing by the consumer. In particular instances, discussion of the problem with supply agencies has usually resulted in a revision of requirements, and it is hoped that eventually all supply agencies will co-operate with consumers in reaching a solution satisfactory to both parties.

In any case, the protection of equipment and the increasing use of high-speed relays puts a ceiling on the amount of time delay used, particularly since the breaker reopens three or four times for a lockout. Present indications are that the device should have fast opening on the first one or two openings and moderate time delay on succeeding openings. This would effect a compromise and allow the use of single-shot fused cut-outs beyond the breakers on short branch lines.

A possible specification for a small distribution circuit breaker would have it satisfy the following requirements:

(*a*). The breaker be rugged and dependable.

(*b*). Be self-contained, requiring no current or voltage transformers.

(*c*). Be entirely automatic.

(*d*). Be equipped with two instantaneous openings and two subsequent time-delay openings to lockout, with recycling to original position for a temporary fault.

(*e*). Contain lightning and overload protection.

(*f*). Have a high insulation level.

(*g*). Have a fairly simple mechanism, easy to repair.

(*h*). Be inexpensive.

Future developments in rural electrification point to increased use of three-phase lines and interconnection of lines into networks. This trend will call for some sort of directional control on breakers. Devices which will distinguish between load and fault currents of the same magnitude are required. There is much to be done in the rural electric distribution field, particularly in circuit control.

References

1. Interim Report on Application and Operation of Automatic Reclosing Equipment on Stub Feeders, AIEE subcommittee on a-c automatic reclosing equipment. AIEE Transactions, volume 62, 1943, August section, pages 536–40.

2. Procedure for Making a Sectionalizing Study on Rural Electric Systems. *Technical Standards Bulletin* 4, United States Department of Agriculture, Rural Electrification Administration.

3. High-Voltage Fusing of Transformer Banks, H. H. Marsh, Jr., G. B. Dodds. AIEE Transactions, volume 61, 1942, July section, pages 533–5.

Interim Report on Application and Operation of Automatic Reclosing Equipment on Stub Feeders

AIEE COMMITTEE ON AUTOMATIC STATIONS
Subcommittee on A-C Automatic Reclosing Equipment

Preface: The present war emergency requires that the maximum use be made of existing equipment and systems and that a minimum of critical material be used for new equipment.

This publication and other guides and reports in this series have been prepared for the information of users during the war emergency. Upon termination of the war emergency they will be reconsidered by the Standards committee and the committees which prepared them, and will be approved, revised for normal use, or rescinded.

This procedure is being followed in preference to the preparation of special emergency standards which might involve redesigning and drastic changes in manufacturing practices. These guides will accomplish the maximum conservation of critical materials, since they provide for the maximum use of existing equipment and systems, as well as new equipment without changing the fundamental basis on which the present standards have been prepared.

Synopsis: This report presents the results of a survey which has been made to obtain data on typical present-day a-c automatic reclosing equipment and practices as applied to stub feeders having a single end source of power. The data received provide a good summary of the reclosing practices in general use and the performance being obtained. It also gives an indication of the possibilities of using immediate initial reclosure more extensively for some types of loads. This subject is of timely interest because of the possibilities of conserving critical materials in such cases where the application of automatic reclosing equipment will defer the necessity of providing additional circuit facilities.

THE value of the use of automatic reclosing equipment is generally recognized, but there is quite a bit of latitude possible in the application of equipment and the extent to which the greatest benefit will be derived from it. Operating experience plays an important part in determining the most suitable type of equipment and the optimum settings.

The scope of this survey was limited to stub feeders which are energized from one source only and which do not require voltage-checking devices nor synchronizing relays.

The data presented in this report are based on information supplied by 41 users and represent a total of over 100 typical applications covering a little more than 70,000 miles of circuits in use by various companies or governmental units in the United States and Canada. The operating voltages range from 2,300 volts and above, and the data submitted cover both isolated and grounded neutral systems. A summary of the data regarding types of equipment used, reclosing cycles, and performance is given in Table I.

Reclosing Cycle

In analyzing the data regarding the reclosing cycle most commonly used, it was felt that it would be most significant to study the information on the basis of the interval of time used for the first reclosure and to apply a weighting factor proportional to the circuit miles for which any particular initial reclosing cycle is used. The time interval used for the second and third reclosures varies rather widely and depends on such factors as allowable breaker duty, relaying requirements, and allowance for clearing of temporary faults caused by tree limbs and swinging conductors. An analysis of the data received made on the aforementioned basis is shown in Figure 1. Immediate initial reclosing was reported for 70 per cent of the circuit miles for all types of loads. For circuits supplying 75 per cent or more residential load, immediate initial reclosing is used for 88 per cent of the circuit miles, while for circuits supplying loads comprised of 25 per cent or more motor load, immediate initial reclosing is used for only 30 per cent of the circuit miles and an initial interval of 15 seconds or more is used for 68 per cent of the circuit miles.

Successful Reclosing Operations

The results of the questionnaire indicate that most users do not provide a means of differentiating between successful initial, second, or third reclosures. They appear to have definite data as to the per cent of successful reclosures and the per cent of lockouts, but in most cases have estimated the number of successful reclosures which were second and third intervals. Figure 2 has been prepared to show the successful initial reclosing performance in terms of circuit miles, and Figure 3 gives the average for all companies weighted in proportion to the circuit miles. From Figure 2 it will be seen that 80 to 90 per cent successful initial reclosing performance was reported for 53 per cent of the circuit miles and 70 to 80 per cent successful reclosure for 34 per cent of the circuit miles. Figure 3 indicates that service is restored on the initial reclosure for 76 per cent of the circuit miles.

Change of Relaying Time After Initial Reclosure

For about 75 per cent of the types of circuits reported, no provision is made for

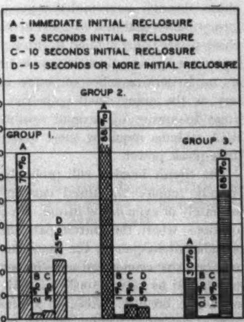


Figure 1. Summary of initial reclosing intervals

Group 1. Average for all types of loads

Group 2. Average for circuits having 75 per cent or more residential load

Group 3. Average for circuits having 25 per cent or more motor load

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Personnel of the subcommittee on a-c automatic reclosing equipment: J. A. Elzi, *chairman;* J. T. Logan, G. S. Whitlow.

This report was prepared by the AIEE subcommittee on a-c automatic reclosing equipment of the committee on automatic stations for the purpose of making essential information immediately available to war industries, thus furthering the conservation of valuable material for the war emergency. It is educational and in no way mandatory. It is not intended as a "Standard" and has not been approved formally by the Standards committee or the board of directors.

It will be noted in the tabulation that 41 users furnished the data summarized in this report. The committee on automatic stations expresses its sincere appreciation to these organizations for their co-operation.

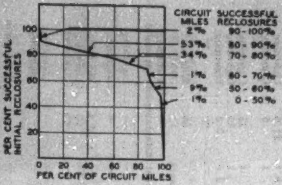

Figure 2. Summary of successful initial reclosures

changing the relaying time after the initial reclosure which would permit the circuit breaker or service restorer to clear the fault initially, providing selectivity with fuses or other fault clearing devices for subsequent reclosures. Of the 25 per cent of the users who do use this type of relaying, several have kept quite complete records and appear to be obtaining satisfactory results. Various schemes of providing this type of relaying were described. Some companies make use of a contact on the reclosing relay, others provide separate plunger-type relays, and in some cases this feature is incorporated in the service restorer.

Available Reclosing Relays

A large majority of the users reported that the present available reclosing relays are satisfactory for their systems. There was an expression on the part of some users that it would be desirable to have provision made for a quicker reset time on reclosures so as to avoid lockouts on recurring faults.

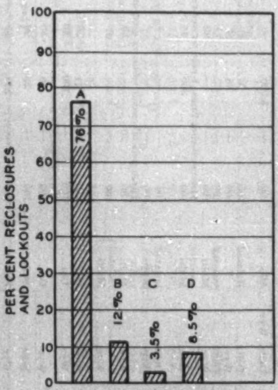

Figure 3. Summary of successful initial reclosures

- A. Successful initial reclosures (including immediate and time delay)
- B. Successful second reclosures
- C. Successful third reclosures
- D. Lockouts

Available Oil Circuit Reclosers and Service Restorers

Quite a few of the users did not have this type of equipment in service or had insufficient experience to comment on this question. Of those that do use this equipment, approximately 50 per cent express the opinion that the present equipment is satisfactory and adequate, while the remainder indicated desired modifications. The features most frequently mentioned as being required are as follows:

(*a*). Greater interrupting capacity.

(*b*). Better time-delay features to permit co-ordination with fuses and other fault-clearing devices.

(*c*). Higher continuous current ratings.

Most of the users indicated a decided preference for three-pole devices when used on three-phase circuits and single-pole devices on single-phase circuits.

Repeater Fuses

Some 90 per cent of the operators using repeater fuses do not seem to find that the lack of the self-resetting feature results in excessive lockouts. Most of the users report that customary patrols of circuits are adequate to prevent excessive lockouts from this cause and do not indicate an excessive amount of patrolling being required to obtain this performance.

Maximum Time of Circuit Interruption Without Dropping Motor Load

Only a relatively few users have factual data regarding the maximum time a distribution circuit can be de-energized without dropping an appreciable part of motor load. However, those who did present data appear to have analyzed the situation rather carefully, and it is felt that the information given is therefore quite significant. Figures of 0.5 to 2 seconds appear the most frequently. One user reports the results of tests which indicate that for 98 per cent of the motors tested, an interruption of from 1 to 2.2 seconds would not result in dropping the induction motor load.

There is quite a divergence of practice in the application of immediate reclosure for motor loads. Some users have adopted the practice of providing no immediate reclosure for any motor loads, but many report satisfactory performance with immediate reclosure for induction motors, and a few use this type of reclosure for synchronous motors as well. In this connection there are several considerations mentioned which are worthy of note:

(*a*). For the successful application of immediate reclosure for motor loads, special consideration may need to be given to the type of load served and the product being manufactured.

(*b*). Time delay is provided for undervoltage release devices where used.

(*c*). For some synchronous motor loads it may be necessary to provide suitable equipment such as field removal relays, unloading devices, and so forth, to insure satisfactory operation.

The replies to this part of the questionnaire indicate that there is a need for special study in applying reclosing equipment to some motor loads, especially synchronous motors and that there is a definite possibility of utilizing the benefits of immediate reclosure on motor circuits to a much wider extent than is now being done.

Minimum Permissible Reclosing Cycles

Here again it was found that there is a dearth of factual data regarding the minimum time required between reclosures to assure deionization of the arc path. Many of the users reporting on this part of the questionnaire expressed the opinion that an interval of approximately 0.5 second is satisfactory, while it was pointed out also by several that the time interval obtained by the use of normal speed breakers and immediate reclosing relays has resulted in no trouble because of restriking of the arc. Some do, however, feel that a period of 3 to 5 seconds should be allowed to permit swinging conductors to separate and falling tree branches to clear from the lines before they are re-energized. Other users rely on the second reclosing interval to take care of these types of faults.

Co-ordination Between Reclosing Equipment and Fuses

Many of the users who replied to the questions regarding experience in obtaining successful co-ordination between reclosing equipment and fuses or reclosers and fuses report satisfactory results if care is taken in making the co-ordination study before the equipment is applied.

Table I. Summary of Equipment and Performance Data

		Type						Load (Per Cent)			Equipment			Performance				
					Per Cent									Per Cent Successful Operations				
Company Number	Service Voltage	Number of Wires	Connection	Grounding	Open Wire	Cable	Circuit-Miles	Residential	Induction Motor	Synchronous Motor	Type	Poles	Reclosing Cycles (Seconds)	Initial	Second	Third	Lock-out	Most Common Cause of Outage
1	4,160	4	Wye	Multi	95	5	—	90	10	—	Service restorer	3	—	90	—	—	—	—
	12,000	3, 4	Delta, Wye	Multi	95	5	—	90	10	—	Oil circuit breaker	3	15-30 urban 30-60 suburban	90	—	—	—	—
2	16,000	3	Wye	Uni	100	—	—	50	50	—	Oil circuit breaker	3	15-30-45	—	—	—	—	Wind
	11,000	3	Wye	Uni							Service restorer	3	—					
	7,000	4	Delta	Uni							Oil circuit recloser	3	—					
	4,000	4	Wye	Multi	Sensitive ground protection on some 7-kv, 4-kv, 11-kv isolated neutral circuits													
	2,400	3	Delta	Uni														
3	13,000	3	Delta	Uni	100	—	5.8	10	60	30	Oil circuit breaker	3	Instant	100	—	—	—	Lightning
	13,000	3	Delta	Uni	100	—	9.5	40	60	—	3-shot fuses Oil circuit breaker	3	5-65-60	100	—	—	—	—
	13,000	3	Delta	Uni	100	—	33.3	80	20	—	Oil circuit breaker	3	—	60	20	—	20	—
	4,000	4	Wye	Multi	100	—	52.7	—	—	—	Oil circuit breaker	3	50-30-95	80	—	—	20	Wind and trees
	4,000	4	Wye	Multi	100	—	196.8	—	—	—	2-shot fuses 3-shot fuses	—	— —	83 100			17 0	
	2,300	3		Multi	100	—	7.9	25	75	—	Oil circuit breaker	2	15-30-30-60	—	—	—	100	
4	13,800	3	Delta	Uni	100	—	11	75	25	—	2-shot fuses		—	50	40	—	10	Lightning
	13,800	3	Delta	Uni	100	—	?	80	20	—	3-shot fuses		—	50	20	20	10	Lightning
	13,800	3	Delta	Uni	100	—	3	90	10	—	3-shot fuses		—	70	10	10	10	Lightning
	13,800	3	Delta	Uni	100	—	112	75	25	—	Oil circuit breaker	3	—	58	21	8	13	Lightning
	13,800	3	Delta	Uni	100	—	98	75	25	—	Service restorer	3	10-10-30	64	14	5	17	Lightning
	13,800	2	1-phase	Uni	100	—	11	95	5	—	3-shot fuses		—	50	20	20	10	Lightning
	2,300	3	Delta	Iso	100	—	9	60	40	—	Oil circuit breaker	3	15-15-30	90	0	0	10	Lightning
	2,300	3	Delta	Iso	100	—	21.5	80	20	—	Oil circuit breaker	3	15-30-75	25	25	25	25	Lightning
5	33,000	4	Wye	Uni	—	—	—	—	—	—	Oil circuit breaker Oil circuit reclosers	3 3	1st 0-18 2nd 20-52	—	—	—	—	Lightning
	4,500	4	Wye	Multi	90	10	—	—	—	—	Oil circuit breaker	1	Instant 0-30-90 5-30-90 5-30-125	—	—	—	—	Lightning
					Also use one-pole and some three-pole oil circuit reclosers													
6	7,200	2	Wye	Multi	100	—	35	—	—	—	3-shot fuses		—	—	—	—	14.8	
	7,200	2	Wye	Multi	100	—	204	—	—	—	Oil circuit reclosers	1	—	—	—	—	12.5	
7	4,400	4	Wye	Uni	95	5	1,192	50	50	—	Oil circuit breaker	1	15-30-75	72	7.9	1.4	18.7	—
				Total for 84 circuits—7 per cent of circuits have instant reclosure.														
	11,950	4	Wye	Uni	100	—	79	88	12	—	Oil circuit recloser	1		95			5	—
	11,950	4	Wye	Uni	100	—	1,388	88	12	—	Oil circuit breaker multigrounded in 1938–1942	1	0-45-75	76.3	7.4	1.4	14.9	Lightning, trees
				Total of 23 circuits of 12-kv.														
	33,000	3	Wye	Uni	100	—	263	60	30	10	Oil circuit breaker	3	0-45-75	92.4	2.3	0.6	4.7	Lightning
	11,950	4	Wye	Uni	100	—	150	95	5	—	3-shot fuses		—	55.6	11.1	—	33.3	—
				Nine 12-kv circuits, unigrounded in 1938, multigrounded by 1942.														
8	13,200	4	Wye	Multi	99.6	0.4	245	55	40	5	Oil circuit breaker	3	0 — —	90.1	4.0	—	5.9	Lightning
	4,160	4	Wye	Multi	99.4	0.6	360	74	25	1	3-shot fuses Oil circuit breaker	3	0 — —	67.6	2.7	8.1	21.6	Lightning
9	All	3, 4	Delta, Wye	All	100	—	5,000	—	—	—	Oil circuit breaker	3	0-15-75	84	10	1	5	Lightning
											Three-pole service restorers and 1-pole oil circuit reclosers used.							
10	22,000	3	Delta	Isolated	100	—	73	80	20	—	Oil circuit breaker	3	0-30-60	69	4	4	23	Lightning
	11,000	3	Wye	?	100	—	38	87	13	—	Oil circuit breaker	3	0-60-60	53	10	1	36	Lightning
	44,000	3	Delta	Isolated	100	—	62	75	25	—	Oil circuit breaker	3	0-60-60	43	52	2.5	2.5	Lightning
11	4,160	4	Wye	Multi	100	—	—	80	20	—	Oil circuit breaker	3	6-15-75	—	—	—	—	—
					Also use 3-shot fuses, 1-pole service restorers, and 3-pole oil reclosers.													
12	4,150	4	Wye	Multi	100	—	300	95	4	1	Oil circuit breaker	3	0-20	61	11	—	38	Wild fowl
											(For many old oil circuit breakers	3	17-20)	Also use 3-pole service restorers.				
13	2,300	3	Delta	Isolated	100	—	—	—	—	—	Oil circuit breaker	3	15-30-75	—	—	—	—	Lightning
	11,950	4	Wye	Multi	100	—	—	—	—	—	Oil circuit breaker	3	0-30-75	Approximately 75-90 per cent instantaneously successful.				
					Three-pole oil circuit reclosers used on 12 kv.													
14	44,000	4	Wye	Multi	80	20	—	30	70	5	Oil circuit breaker	3	10	—	—	—	—	Lightning
15	2,300	3	Delta	Isolated	100	—	—	67	33	—	Oil circuit breaker	3	1-30-90	—	—	—	—	Lightning
	11,000	4	Wye	Multi														
16	110,000	3	Wye	Uni	100	—	66	—	—	—	Oil circuit breaker	3	2	93.6	—	—	6.4	Lightning
	26,400	3	Wye	Uni	100	—	41	—	—	—	Oil circuit breaker	3	1.7	71	—	—	29	Lightning
	8,000	4	Wye	Uni	100	—	190	90	10	—	3-shot fuses		—	23	32	—	45	Lightning
					Two 40-ampere and one 75-ampere links used at station with 50-ampere and 60-ampere branch fuses.													
17	13,200	3	Wye	Uni	100	—	16	50	50	—	3-shot fuses		—	—	—	—	—	Lightning
	4,000	4	Wye	Multi	100	—	25	90	10	—	Oil circuit breaker	3	0-15-60 15-30-60	—	—	—	—	Lightning
18	4,160	3	Wye	Uni	100	—	200	65	35	—	Oil circuit breaker	3	10-30-60	—	—	—	—	Wind storm
	4,160	—	—	—	—	—	100	—	—	—	Oil circuit breaker	—	—	18.2	18.2	13.6	50	Trees
	11,000	—	—	—	—	—												

Table I (continued). Summary of Equipment and Performance Data

		Type			Per Cent			Load (Per Cent)			Equipment			Performance				
														Per Cent Successful Operations				
Company Number	Service Voltage	Number of Wires	Connection	Grounding	Open Wire	Cable	Circuit-Miles	Residential	Induction Motor	Synchronous Motor	Type	Poles	Reclosing Cycles (Seconds)	Initial	Second	Third	Lock-out	Most Common Cause of Outage
19	All	—	—	—	100	—	4,000 10,000	Transformer Distribution	…	…	—	—	—	85	10	3	2	Lightning, tree
20	4,150	4	Wye	Multi	100	—	17.5	100	—	—	Oil circuit breaker	3	10–20–60	—	—	—	—	
	4,800	3	Delta	Isolated	96.3	3.7	111.5	62	38	—	Oil circuit breaker	3	10–20–60	—	—	—	—	
							Also use 3-pole oil circuit reclosers.											
21	4,160	4	Wye	Multi	100−	0+	3,000	75	20	5	Oil circuit breaker Older oil circuit breaker	3 3	0–15–60 10–30–36	74	9	2	15	Lightning
	13,000 6,600	3	Delta	Isolated	100	—	1,100	—	—	—	Oil circuit breaker	3	?	70	13.5	1.5	15	Lightning
22	2,080 4,160	3	Delta	Isolated	90	10	2,150	—	—	—	Oil circuit breaker	3	15–30–60 7–7–7 20–20–20	80			20	—
23	All	—	—	—	100	—	—	Induction motor predominates			Oil circuit breaker 2-, 3-shot fuses	3	0–30–60	80	5	1	14	—
24	4,000	4	Wye	Uni	90	10	2,000	20	50	30	Oil circuit breaker	3	0–10–150 0–60–150	87	3	1	9	—
			Three-pole service restorers and oil circuit reclosers used.															
25	12,000	4	Wye	Multi	99	1	800	100−	0+	—	Oil circuit breaker	3	0–30–60	80	15	4	1	—
26	33,000	3	Delta	Petersen coil	100	—	31	30	40	30	Oil circuit breaker	3	Instant	95	—	—	—	Lightning
27	2,300	3	Delta	Uni	100	—	Short	—	—	—	Oil circuit breaker	3	0–20–40	80	20	—	*1	—
			Three-pole service restorers and 3-pole oil circuit reclosers used.															
28	12,000	4	Wye	Multi	100	—	5	100			3-shot fuses		—	—	—	—	0	Trees
29	34,500	3	Wye	Uni	100	—	1,500	50	40	10	Oil circuit breaker 2-shot fuses	3	0–15–85	75	20	0+	5	Lightning, trees
	12,000	4	Wye	Multi														
	2,400	3	Delta	Uni														
30	4,160	4	Wye	Multi	88.8	11.2	647	18.3	50.3	5.6	Oil circuit breaker	3	15–45–90	52.8	2.2	3.4	41.6	Lightning
31	6,900	3	Delta	Isolated	100	—	100	80	20	—	2-, 3-shot fuses		—	—	—	—	1	Lightning, wind
			One-pole service restorers used.															
32	34,500	3	Delta	Uni	100	—	26	95	5	—	Oil circuit breaker	3	5–15–30	—	—	—	—	Lightning
	34,500	3	Delta	Uni	100	—	29	85	15	—	3-shot fuses		—	70	5	5	20	Lightning
	11,000	4	Wye	Multi	100	—	42	90	10	—	Oil circuit recloser	3	3–3–3	80	10	5	5	Lightning, wind
33	4,325	4	Wye	Multi	95	5	2,643	43	57	—	Oil circuit breaker	3	0–30–60	80	7–8	3–4	8–10	Trees
34	4,000	4	Wye	Multi	100	—	400	70	30	—	Oil circuit breaker	3	15–45–90	90			10	Lightning, trees
	2,300	3	Delta	Isolated														
35	4,000	4	Wye	Uni	90	10	1,186	65	30	5	45 circuits oil circuit breaker 35 circuits oil circuit breaker	1 3	15–30–90	75	10	4	11	Lightning
			Have some 2-shot fuses.															
36	4,000	4	Wye	Multi	85	15	250	65	32	3	Oil circuit breaker—1-, 3-phase 2-, 3-shot fuses		Instant only	75	—	—	25	Lightning
	5,000	3	Delta	Isolated	100	—												
37	4,500	4	Wye	Multi	40	60	550	45	55	—	53-oil circuit breaker	1	0–30–60	50	23.5	5	21.5	Wind
			Night loading					90	10	—	86-oil circuit breaker	3						
38	4,800	3	Delta	Isolated	96	4	12,500	25	45	5	Oil circuit breaker	3	15–60–60	80	5	1	14	Lightning
	4,800	3	Delta	Isolated	100	—	870	100	—	—	Oil circuit breaker	3	15–60–60	84	2	0	14	Lightning
					Has sectionalizing fuses.													
	4,800	3	Delta	Isolated	100	—	985	98	2	—	Oil circuit breaker	3	0–15–30–30	75	18	1	6	Lightning
					Has sectionalizing fuses.													
	4,800	3	Delta	Isolated	100	—	345	100	—	—	Oil circuit breaker	1	5–5–5–5	97	2	—	1	Lightning
								Has sectionalizing fuses.										
	4,800	3	Delta	Isolated	100	—	768	100	—	—	Oil circuit breaker	1	1–1–1–1	93	5	—	2	Lightning
39	4,000	4	Wye	Multi	100	—	125	85	15	—	Oil circuit breaker	3	0–30–60 15–30–75	50	—	—	50	Lightning
	22,000	3	Wye	Uni	100	—	12	95	5	—	3-shot fuses		—	—	—	—	100	—
					Fuses unsatisfactory—to be replaced by oil circuit breaker.													
	22,000	3	Wye	Uni	—	—	22	85	13	2	Oil circuit breaker	3	0–30–60	30	—	—	50	Lightning
	6,600	3	Delta	Isolated	100	—	31	75	25	—	Oil circuit breaker	3	15–30–90	—	—	—	100	—
	6,600	3	Delta	Isolated	100	—	23	75	25	—	3-shot fuses		—	100			0	Lightning
	2,300	3	Delta	Isolated	100	—	20	75	25	—	3-shot fuses		—	100			0	Lightning
	4,000	4	Wye	Multi	100	—	2	95	5	—	Oil circuit breaker	3	15–30–75	98	—	—	—	Lightning
40	2,400	3	Delta	Isolated	100	—	4,800	80	15	5	3-shot fuses Oil circuit breaker	3	0–30–90 4–30–90 30–60–90	54	28	14	4	Lightning
	4,160	4	Wye	Multi														
	4,800	3	Delta	Iso, uni														
	7,200	3	Delta	Iso, uni														
					(30–60–90 reclosing cycle for synchronous motor loads)													
41	2,400	3	Delta	Isolated	100	—	13,800	75	15	10	2-shot fuses 3-shot fuses Oil circuit breaker	3	0–30–60	70	20	5	5	Lightning
	4,160	4	Wye	Multi														
	4,800	3	Delta	Isolated														
	8,330	4	Wye	Multi	(Uses time delay for initial reclosures for synchronous motor loads as requested by customers.)													
	7,200	3	Delta	Isolated														
	12,500	4	Wye	Multi														
	11,000	3	Delta															
	14,400	3	Delta	Isolated														

Difficulties which have been encountered in making these applications are:

(*a*). Trouble in obtaining co-ordination with ground relays except in such cases where fuses of low current rating are applicable.

(*b*). Failure to obtain co-ordination between service restorers with instantaneous trip and fuses unless the fuses are of such low current rating that they will blow at currents near the pickup value of the restorer.

Conclusions

As the result of this survey, the following conclusions may be reached:

1. In terms of circuit-miles, immediate initial reclosing is most commonly used for residential loads and 15-second initial reclosing for induction motor loads.

2. Successful initial reclosing was reported for approximately 75 per cent of the trip outs. The second and third reclosures will be successful in approximately 15 per cent of the cases, and lockout can be expected in ten per cent of the cases.

3. Present reclosing equipment appears to meet the requirements of most of the users except that a necessity for changes as noted previously in available oil circuit reclosers or service restorers is indicated.

4. Circuits should be re-energized in 0.5 to 2 seconds if the dropping of induction motor loads is to be avoided.

5. No trouble from restriking of the arc was reported with immediate reclosure with normal speed breakers. Some users, however, feel that an interval of 0.5 second should be provided.

6. The possibilities of using immediate reclosing for motor circuits should receive further study, as there are indications that the advantages of immediate reclosing can be extended to include a much larger percentage of motor circuits than is now being done.

References

1. Engineering report 47 of the Joint Subcommittee on Development and Research of the Edison Electric Institute and the Bell Telephone System. Volume V, July 1941, pages 239–71.

2. FACTORS CONTRIBUTING TO IMPROVING ELECTRIC SERVICE BY MEANS OF HIGH-SPEED SWITCHING AND UTILIZATION OF STORED ENERGY, **J. T. Logan, J. H. Miles.** AIEE TRANSACTIONS, volume 60, 1941, December section, pages 1012–16.

3. EXPERIENCE WITH OIL CIRCUIT RECLOSERS ON REA SYSTEMS, **L. M. Moore, B. O. Watkins.** AIEE TRANSACTIONS, volume 62, 1943, August section, pages 531–5.

Appendix. AIEE Committee on Automatic Stations Questionnaire on A-C Automatic Reclosing Equipment

Please give data for each set of circuit and load conditions for which different reclosing practice is used.

Reporting company............................Location............................

A. Circuit and Load Data

Voltage.........Three-wire.........Four-wire.........Delta.........Wye..........
Isolated........Ungrounded..........Multigrounded........
..........per cent overhead..........per cent cable
Load....per cent residential....per cent induction motor....per cent synchronous motor

B. Equipment

Repeating fuses two-shot...... three-shot......
Circuit breaker........pole (single or three) with........reclosing cycle. Specify most usual reclosing cycle as: instantaneous; 0–30–60 seconds; 15–30–75 seconds; 0–30 seconds, and so forth.
Service restorer..........pole (single-pole or three-pole)
Oil circuit reclosers............pole
Protective relay............ (instantaneous overcurrent, time delay overcurrent, and so forth)
Does the equipment automatically change the relaying time after first, second, or third reclosure? .... Give brief description of sequence of operation.

C. Experience Data

Approximate............years............circuit-miles
Successful reclosures for reclosing cycle given above
Initial immediate ....per cent. Initial time delay........per cent
Second........per cent. Third........per cent. Lockout........per cent
What has been found to be the most convenient way of determining the number of first, second, or third reclosures?
Most common cause of outage cleared fast enough for successful immediate initial reclosing.
Lightning...... trees...... ....
Reclosing cycle found to be particularly suitable for:

(*a*). Lighting loads..
(*b*). Synchronous motor loads..
(*c*). Induction motor load...
(*d*). Combined motor and lighting loads..
(*e*). Selective operation with branch circuit fuses...........................
(*f*). Selective operation with branch circuit reclosers or service restorers..........

D. General

Do available reclosing relays meet operating requirements, or is some modification in the operating cycle desirable? If so, please give suggested modification.

Do available oil circuit reclosers or service restorers meet operating requirements, or is some modification desirable? If so, give suggested modification. Also give reasons for preferring the single- or three-pole device.

Does the lack of the self-resetting feature in the case of reclosing fuses result in excessive lockouts? What steps do you take (such as patrolling) to keep the number of lockouts to a minimum?

What has been found to be the *maximum* time that the circuit breaker may remain open during a reclosing cycle, without dropping any appreciable part of the motor load? State whether load is predominately induction or synchronous.

Give operating data which indicate *minimum* time that circuit breaker may be reclosed with assurance that fault arc path has been given sufficient time to deionize. In answering this question, consideration should be given to the effect of counter electromotive force from synchronous machines (connected to the feeder) which tend to sustain the fault arc while the circuit breaker is open.

What has been your experience in obtaining successful co-ordination between reclosing equipment and fuses, the latter being located in some subdivision of the feeder?

What has been your experience in obtaining successful co-ordination between oil circuit reclosers or service restorers, and fuses, the latter being located in some subdivision of the feeder?

Any additional comments:

This questionnaire is limited to stub feeders which are energized from one source only and which do not require voltage checking devices, synchronizing relays, and so forth.

A Capacitance Bridge for Determining the Ratio and Phase Angle of Potential Transformers

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Synopsis: A capacitance bridge for the calibration of instrument potential transformers up to 300 kv is presented in this paper. This bridge provides measurements which are correct within 0.1 per cent on ratio and 3 minutes on phase angle. There are within the bridge arrangements for testing its accuracy as determined by the stability of its component arms. It is particularly desirable to have this means of self-testing if transformers are to be tested in a laboratory which does not have extensive standardizing facilities. Further advantages beyond accuracy for standardization purposes are flexibility and ease of operation for production testing, and low cost and small volume as compared to a high-voltage resistance potentiometer.

IN the determination of ratio and phase angle of potential transformers, measurements are required which are correct within 0.1 per cent on ratio and three minutes on phase angle. Such measurements are made on transformers with voltage ratings as low as 110 volts and as high as 161 kv. It is probable that higher voltages may be used in the future.

A capacitance bridge has been developed for this purpose. This bridge is connected first as a Schering Bridge in order to establish the relative impedance of its arms; then it is connected one part to the primary winding of an instrument potential transformer and the other part to the secondary winding of the transformer in order to determine the ratio and phase angle between the windings of the transformer.

This method of measuring the properties of transformers may be compared to the resistance potentiometer method[1,2] and to previous proposals for capacitance bridge methods.[2,3,4,5] The most conspicuous advantage of this method over the resistance-potentiometer method is the reduction in weight and space required. The advantage over previous proposals of a capacitance bridge is provision for reconnecting the bridge in order to establish its accuracy in place.

Performance

A bridge of this design was installed in the transformer section of the Pittsfield works' laboratory and used in conjunction with the 132-kv shielded resistance potentiometer[1] already there. It was operated for a period covering both the winter and summer seasons.

The relative size of the capacitance bridge made it very attractive, as it required 200 square feet of floor space whereas the resistance potentiometer required 900 square feet. The saving of factory space allows a saving in testing because it facilitates the handling of transformers during this stage of manufacture.

Transformers have been tested throughout the range 110 volts to 161 kv. The extreme flexibility of the capacitance bridge was an appealing point to the operator. A 1400:1 ratio could be checked as easily as a 20:1. The change from one voltage or ratio to another could be done without changing high-voltage taps on the equipment. Odd ratios were just as simple to measure as were the integral ratios.

A 50 micromicrofarad high-voltage compressed-gas capacitor was used for ratios down to 10:1. Below 10:1 ratio a 200-micromicrofarad shielded air capacitor was used. This combination of standard capacitors provided good sensitivity over the whole voltage range.

After this experience a permanent installation was decided upon. A new bridge was built and installed and has been in use for over a year. This bridge provides measurements which are correct within 0.1 per cent on ratio and three minutes on phase angle throughout the voltage range used thus far. The bridge voltage rating is 300 kv, and no difficulty is expected in extending measurements to this voltage.

The Bridge Circuit

This bridge has two resistance arms and two capacitance arms as shown in Figure 1*a*. Two of these arms, R_3 and C_2, are fixed in magnitude. C_2 is a shielded gas capacitor for high-voltage use. The other arms, R_4 and C_1, are continuously adjustable. If R_4 is adjusted to a conductance $\sqrt{n}$ times that of R_3, where n is the ratio of the transformer to be tested, the bridge can be balanced by adjusting C_1 to a capacitance $\sqrt{n}$ times that of C_2. Now suppose C_1 and C_2 interchanged and the transformer connected so that the high-voltage winding is connected to C_2 in series with R_4 and the low-voltage winding to C_1 in series with R_3, Figure 1*b*. The smaller capacitance, C_2, is now in series with the larger conductance, R_4, but the voltage applied to this combination is n times that applied to the larger capacitance in series with the smaller conductance. Thus the voltage drop across R_3 is again equal to that across R_4 and the bridge again in balance excepting a small phase shift explained in Appendix I. If the transformer ratio differs slightly from n, the small change in R_3 or R_4 required to restore balance is a measure of this departure. Used in this manner, the bridge gives evidence of its own stability before each measurement on a transformer. This self-standardizing feature is particularly desirable if transformers are to be tested where extensive standardizing facilities are not available.

A detailed explanation of this bridge circuit for measuring the ratio and phase angle of a transformer is developed in Appendix I. The bridge connections finally arrived at are shown in Figure 4. In this figure R_{33} is a bridge resistance arm adjustable from 9,900 ohms to 10,110 ohms, set at 10,000 ohms when the ratio correction factor is unity. The adjusting dials are marked directly in ratio correction factor. Adjustment for phase angle is made by changing C_{33}, and C_{33} is marked in such units that it reads directly in phase angle at 60 cycles when R_{33} is at 10,000 ohms. Departure from direct reading in phase angle at other values of R_{33} may be corrected by reference to a curve in any case and is minimized by care in the design of the $R_{33}C_{33}$ arm of the bridge so that no correction is required on the greater portion of transformer tests.

R_{34} in the bridge is made of a conductance decade; that is, of resistors connected in parallel by a switching sequence, marked in terms of $\sqrt{n}$ where n is the nominal transformer ratio. The

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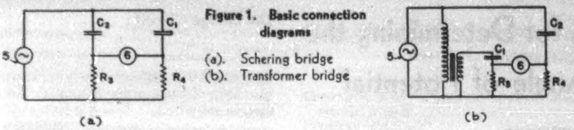

Figure 1. Basic connection diagrams

(a). Schering bridge
(b). Transformer bridge

conductances of the principal resistors in R_{34} are 1, 2, 2, 5, 10, 20, 20, and 50 times the conductance of the 10,000-ohm setting of R_{33}.

Note particularly that errors in the ratio of R_{33} to R_{34} are doubled in their effect on the determination of potential transformer ratio. This is not a serious disadvantage because the conductance decade arrangement, in which most of the conductance of R_{34} is made up of steps of $^1/_2$, 1, and 2 times 10^m where m is an integer, lends itself especially to precise and accurate determination of the ratio between R_{33} and R_{34}.

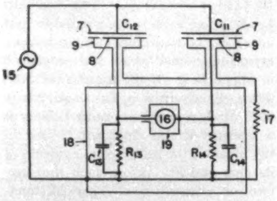

Figure 2. Connection diagram of Schering bridge including shielding

Capacitor C_{32} in Figure 4 is of fixed capacitance and suitable for use at high voltage. C_{31} is an adjustable low-voltage capacitor with worm drive for precise setting while balancing the bridge. Any difference in dielectric loss angle between these capacitors appears double as an error in transformer phase-angle measurement. This error will not exceed one-quarter minute of angle so long as the capacitor at C_{31} is kept fairly dry.[7]

The Bridge

The high-voltage capacitor which constitutes one bridge arm, C_{32} of Figure 4, has shielded concentric cylindrical electrodes operated in carbon dioxide at about 100 pounds per square inch gauge pressure. Figures 5 and 6 show the construction. The essential properties and advantages of standard capacitors constructed in this manner have been set forth elsewhere[6,7] and will merely be summarized here.

(*a*). Change of dimensions with voltage is calculably negligible.

(*b*). Change of dimensions with temperature and its effect on capacitance is calculable.

(*c*). Change of capacitance with alignment is calculable; thus, limits within which it is negligible may be established.

(*d*). The maximum dielectric loss angle encountered in such capacitors is small as compared to usual phase-angle accuracy requirements, that is, much smaller than one minute at 60 cycles.

With respect to factor *b*, the massive tank by its thermal slowness limits temperature gradients inside the capacitor. A steady-state temperature change of five degrees centigrade or a change in the temperature difference between electrodes of two degrees centigrade is required to produce 0.01 per cent effect on measured ratio. In order to produce this effect, these changes must occur between the standardization in terms of bridge resistors and the measurement on the transformer, a matter of a few minutes at most.

This capacitor may be operated at voltages up to 300 kv rms.

The other parts of the bridge are housed in the case shown in Figure 7 and Figure 8. Note at the top of Figure 8 the parallel plate capacitors used at C_{31} of Figure 4. One of these has a worm drive which allows adjustment to 0.005 micromicrofarad. The other two are of fixed capacitance and may be switched into circuit to extend the capacitance range. All have plates supported from a guard structure with the solid insulation shielded from the field between plates.

Dielectric loss angle of these capacitors is ordinarily less than 0.00002 or four seconds of angle. The accuracy of phase-angle measurement depends upon the maintenance of a small loss angle. Proof of such maintenance is obtained from the standardization balance of the bridge before each measurement. Any change in C_{34} of Figure 4 from the customary value is evidence that the relative dielectric loss angle of the air capacitors has changed. In practice, only the low-voltage capacitor changes, and it changes only because of humidity and dirt.[7] Ordinary care in maintenance will avoid such changes.

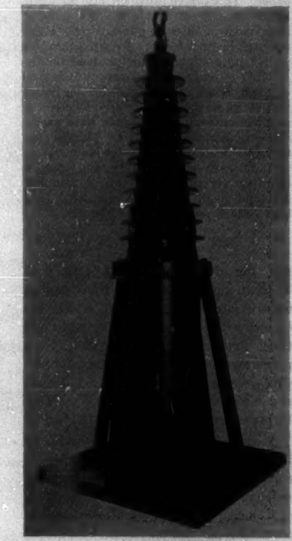

Figure 5. Compressed-gas capacitor, 300 kv

The resistance arms of the bridge are partly visible in Figure 8, behind the galvanometer amplifier case. The principal resistors are of manganin. The one micromho steps are of advance alloy, and the smaller conductance decades are carbon resistors. These latter serve only for fine adjustment of the $\sqrt{n}$ arm, hence errors of several per cent are allowable.

The galvanometer and its amplifier are in a case within the main bridge case.

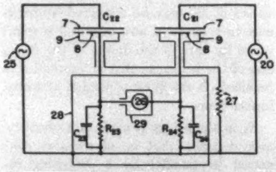

Figure 3. Connection diagram, capacitance bridge for comparing two voltages

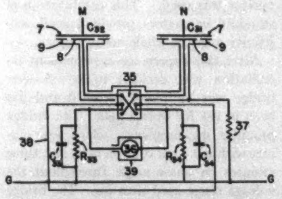

Figure 4. Connection diagram of bridge for measuring the ratio and phase angle of potential transformers

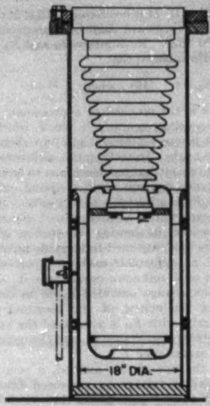

Figure 6. Cross section of the 300-kv capacitor

The capacitance between cases is a part of C_{34}.[8] It is not affected by switching connections and therefore not a factor in the accuracy of the bridge.

The lower part of the bridge case has relays which connect an auxiliary power supply for the calibrating balance of the bridge and which may be used for remote control of high-voltage switching.

Figure 7. Bridge for measuring ratio and phase angle of potential transformers

Measurement Procedure

The operation of the bridge is as follows:

1. Set ratio-correction-factor dials at 1.0000; set phase-angle dials at a value determined by the ratio of the transformer to be tested; set $\sqrt{n}$ dials at the square root of the nominal transformer ratio; then turn selector switch to "calibrate."

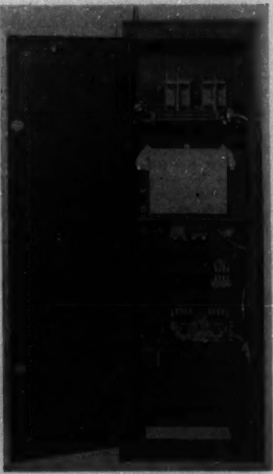

Figure 8. Back view of the bridge, case open

2. Apply power to calibrating circuit, and balance the bridge by adjusting the low-voltage air capacitor C_{31} and the capacitance C_{34} of Figure 4.

3. Connect transformer to be tested and turn selector switch to "measure."

4. Apply power to transformer and balance the bridge by adjusting the ratio-correction-factor and phase-angle dials at each desired burden and excitation. Ratio correction factor of the transformer is read directly from the dials. Phase angle is read directly from the dials on tests at 60 cycles. At other frequencies the dial reading must be multiplied by the ratio of the test frequency to 60.

Standardization of the Bridge Resistance Arms

Accuracy of the transformer-ratio measurement rests on the accuracy with which resistance ratio between the bridge arms is known. The ratios of the various R_{34} settings to R_{32} are preferably determined in place using a step-by-step procedure which imposes conditions equivalent to those of actual use. A schedule has been established whereby these ratios can be determined after about one hour's systematic work. This procedure has been confirmed by measuring the resistances separately in terms of resistance standards which in turn have been compared to standards maintained at the National Bureau of Standards.

Changes in the present bridge since the initial standardization have been confined to the expected trivial slow drift in relative resistance of the arms, but it must be recognized that a significant change is possible at any time, although it is not expected. Therefore, the operator should be alert for any irregularity in the "calibrate" balance before each measurement and, in addition, should carry out occasionally the schedule established for determining resistance ratio between the arms. That this can be done without external standards is an advantage.

Summary

A bridge has been developed for measuring the ratio and phase angle of instrument potential transformers. The measurements are correct within 0.1 per cent in ratio and three minutes in phase angle. The bridge rating is 300 kv, but actual use thus far has been limited to tests at 161 kv and below. This limit was imposed by the transformers requiring test, and extension of tests to higher voltage, when transformer design requires it, offers no difficulty.

Appendix I

Consider Figure 1*a* in which C_1 and C_2 indicate capacitance while R_3 and R_4 indicate electrical resistance. If an alternating current is supplied by generator 5, the condition for no resultant potential across a circuit element 6, such as a galvanometer, is that:

$$C_1/C_2 = R_3/R_4 \qquad (1)$$

When it is sought to realize the circuit of Figure 1*a* in practice, it becomes necessary to take into account any departure of actual capacitors from being pure capacitance and any residual inductance or capacitance of the resistors which may be used where resistance is indicated in the figure. In addition, capacitance from conducting parts of the apparatus to earth and to other apparatus not shown may affect the distribution of current from the generator. Figure 2 shows diagrammatically the arrangement of apparatus which results when these factors are considered.

In Figure 2, C_{11}, and C_{12} are capacitors each having electrodes 7 and 8 and further electrodes 9 which entirely surround electrodes 8, excepting the exposure of electrodes 8 to electrodes 7. Electrodes 7 and 8 are supported from electrodes 9 by separate pieces of solid insulating material so that no solid insulation lies in the region between electrodes 7 and 8. This region is filled with air or other gas.

R_{13} indicates the resistance of a resistor. The residual inductance or capacitance of this resistor, expressed as capacitance, together with any deliberately introduced capacitance, is shown as C_{13}. Similarly, R_{14} and C_{14} indicate another resistor with its associated capacitance, whether deliberately introduced or accidental. A galvanometer or other voltage-detecting means is shown at 16. A conducting case 18 may enclose circuit elements 13, 14, and 16 to shield them from external electric field. If the galvanometer 16 is shielded by a shield 19 connected as shown in the figure, the capacitance from shield 19 to case 18 must be included as part of the capacitance C_{14}. There are also other means of successfully shielding galvanometer 16.

Suppose alternating current is supplied by generator 15, and suppose further that the circuit elements are adjusted until no voltage exists at galvanometer 16. Suppose that impedance 17 has been adjusted until no voltage exists between conductors 8 and 9 of the capacitors. Under these circumstances the current through the impedances indicated by the subscripts 13 and 14 will be exclusively the charging current through the direct capacitance between plates 7 and 8 of capacitors C_{12} and C_{11}, respectively. The necessary and sufficient relationships between the impedances indicated by the subscripts 11, 12, 13, and 14 are:

$$C_{11}/C_{12}=R_{13}/R_{14} \tag{2}$$

and

$$R_{14}(C_{3}+C_{11})-R_{13}(C_{13}+C_{12})=0 \tag{3}$$

combining equations 2 and 3 gives the simple relation between the impedances indicated by subscripts 13 and 14:

$$R_{14}C_{14}-R_{13}C_{13}=0 \tag{4}$$

If, now, an imperfect dielectric is introduced between the plates 7 and 8 of one capacitor, say capacitor 12, the angle A by which the vector representing the charging current of this capacitor lags that representing the charging current of a pure capacitance is given by the expression, valid for angles so small that the angle expressed in radians approximately equals the tangent of the angle:

$$A=\omega(R_{14}C_{14}-R_{13}C_{13}) \tag{5}$$

where $\omega=2\pi$ times the frequency in cycles per second. This is the Schering bridge (Thomas bridge) commonly used for dielectric measurements.[10,11]

Consider now Figure 3, similar to Figure 2 except that generator 25 supplies current to capacitor C_{22} while separate generator 20 supplies current to capacitor C_{21}. Suppose that these generators have voltages in phase with one another, but that the voltage of generator 25 is n times the voltage of generator 20. The condition for no voltage at galvanometer 26 is:

$$\frac{\dfrac{nR_{23}}{1+j\omega R_{23}C_{23}}}{\dfrac{R_{23}}{1+j\omega R_{22}C_{23}}+\dfrac{1}{j\omega C_{22}}}=\frac{\dfrac{R_{24}}{1+j\omega R_{24}C_{24}}}{\dfrac{R_{24}}{1+j\omega R_{2}C_{24}}+\dfrac{1}{j\omega C_{21}}} \tag{6}$$

The expanded statement of the relation between the impedances indicated by the subscripts 21, 22, 23, and 24 necessary to satisfy this condition is unwieldy in length. If the impedances are selected so that

The impedance of C_{21} is much greater than R_{24}

The impedance of C_{22} is much greater than R_{23}

$\omega R_{23}C_{23}$ is much less than 1

$\omega R_{24}C_{24}$ is much less than 1

certain approximate relations are sufficient. These follow:

$$C_{21}/C_{22}=nR_{23}/R_{24} \tag{7}$$

$$R_{24}(C_{24}+C_{21})-R_{23}(C_{23}+C_{22})=0 \tag{8}$$

If there is a difference B in phase angle between the voltages from generators 25 and 20, small enough that the angle expressed in radians may be assumed equal to the tangent of the angle,

$$B=R_{24}(C_{24}+C_{21})-R_{23}(C_{23}+C_{22}) \tag{9}$$

It is evident that the bridge of Figure 3 may be used for the measurement of the ratio and phase angle between voltages such as the primary and secondary voltages of an instrument potential transformer, since equations 7 and 9 enable the calculation of ratio and phase relationship from the observed values of C_{21}, C_{22}, R_{23}, C_{23}, R_{24}, and C_{24}.

If such comparison is to be made with great accuracy, it is necessary to consider the accuracy with which the value of the several circuit elements may be known. Consider equation 7. Resistors are commonly prepared of alloy wire little enough affected by temperature variations and of such permanence that the resistance may be known within 0.01 per cent. Air capacitors are not made readily with such accuracy. Consider now equation 9. This equation is based on equality of phase angle between current and voltage in the two air capacitors represented by C_{21} and C_{22}. This condition may be realized within some few seconds of angle. However, uncertainties in the value of C_{23} and C_{24} may lead to errors in usual practice nearly as large as one minute of angle in the determination of the phase angle B.

It would be desirable to determine the ratio and phase angle between two voltages in terms of the more accurately definable quantities, that is, the resistances R_{23} and R_{24} and the purity of the capacitances at C_{21} and C_{22}. For a method of accomplishing this end, refer to Figure 4. In this figure, C_{32} is a fixed air or gas capacitor, shielded and guarded in the fashion previously described. C_{31} is an adjustable air capacitor so shielded and guarded. It is required of C_{31} that once it is adjusted, it shall, if not touched, remain constant in direct capacitance between its electrodes 7 and 8 until the measurements described hereafter are completed. It is not required that any particular adjustment shall be reproducible at a later time. Resistances R_{33} and R_{34} shall be adjustable so that

$$R_{33}/R_{34}=r \quad (10) \quad \text{where} \quad r^{2}=n \tag{11}$$

and n is the nominal or approximate ratio of two voltages, nearly in phase, which are to be compared with respect to ratio and phase angle. In making this adjustment it is convenient but not necessary to have the resistance in ohms of R_{33} a round number while R_{34} is adjustable to secure the ratio r. The capacitance associated with R_{33} is, for convenience in showing the working of this test method, assumed to be made up of two parts, an adjustable calibrated portion C_{33} and a small unknown capacitance X, which will not change provided R_{33} is unchanged during the course of the measurements. Similarly, with R_{34} is associated the known adjustable capacitance C_{34} and the unknown fixed capacitance Y.

In the use of this method, a source of alternating voltage is connected from M and N to G, Figure 4, and the switch 35 is positioned to connect C_{31} to R_{34} and C_{32} to R_{33}. C_{33} is adjusted to some arbitrary small capacitance; R_{34} is adjusted in accordance with equation 10; then C_{34} and C_{31} are adjusted until galvanometer 36 indicates equality of the voltages across R_{33} and R_{34}. By analogy to equation 2, this is true when

$$C_{31}/C_{32}=R_{33}/R_{34} \tag{12}$$

whence, from equation 10,

$$C_{31}/C_{32}=r \tag{13}$$

From analogy to equation 4

$$R_{34}(C_{34}+Y)-R_{33}(C_{33}+X)=0 \tag{14}$$

whence

$$C_{34}+Y=(R_{33}/R_{34})(C_{33}+X) \tag{15}$$

using equation 10

$$C_{34}+Y=r(C_{33}+X) \tag{16}$$

Now the switch 35 is changed so that C_{31} is connected to R_{33} and C_{32} to R_{34} while separate voltage sources are connected from M to G and from N to G. The voltage at M is n times that at N and in phase with that at N. Now by analogy with equation 7, equality of voltage across R_{33} and R_{34} will be secured when

$$C_{31}/C_{32}=nR_{34}/R_{33} \tag{17}$$

substituting from 10 and 13, $r=(n/r)$ therefore, $n=r^{2}$ which is simply equation 11. Thus, R_{33}, R_{34}, C_{32}, and C_{31} need not be changed if the ratio between voltages at N and M is actually n. However, C_{33} or C_{34} will have to be changed, as may be seen from analogy to equation 8. Suppose C_{33} is unchanged, but C_{34} is changed to a new value C_{34}' in order to secure equality of voltages across R_{33} and R_{34}:

$$R_{34}(C_{34}'+Y+C_{32})-R_{33}(C_{33}+X+C_{31})=0 \tag{18}$$

$$C_{34}'+Y=r(C_{33}+X+C_{31})-C_{32} \tag{19}$$

Subtract equation 16

$$C_{34}+Y=r(C_{33}+X) \tag{16}$$

$$C_{34}'-C_{34}=rC_{31}-C_{32} \tag{20}$$

but, from equation 13

$$rC_{31} = r^2C_{33} = nC_{32} \qquad (13)$$

therefore,

$$C_{34}' - C_{34} = (n-1)C_{32} \qquad (21)$$

Note that X and Y need not be known. Any departure of the ratio between the two voltages from the assumed value will require a proportionate change in either R_{33} or R_{34}. Similarly, if the two voltages do not agree in phase, either C_{33} or C_{34} will need to be changed. Specifically, if C_{34} is changed to C_{34}' as indicated in equation 21, any further change necessary will be due to a phase difference D, expressed in radians.

$D = \omega R_{33}$ times the change in C_{33}

or

$D = \omega R_{34}$ times the change in C_{34}, whichever is changed.

In the operation of the bridge for instrument potential-transformer calibration, it has been found most convenient to mark C_{33} in minutes of angle at 60 cycles. Then the calibrating balance of the bridge (as a Schering bridge) is made with C_{33} set at such a value (in minutes at 60 cycles) as that required for the change from C_{33} to C_{33}' when the arms are reversed.

$$C_{33} - C_{33}' = [(n-1)C_{32}]/r \qquad (22)$$

This makes C_{33}' the capacitance marked "0 minutes" on the dial.

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This brief bibliography has been selected from numerous published articles in order to show as compactly as possible the present state of the art of measuring potential transformers.

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Carrier-Current Differential Protection for Transformer Banks

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WHEN carrier current was first applied to protective relaying of the directional type, it consisted of a simple telegraph channel. Subsequent developments have expanded the capabilities to the extent that each carrier channel for pilot relaying now may provide one or more of the following additional services:

1. Simplex telephony for testing or emergency use.
2. Telemetering or load control.
3. Supervisory control of unattended substations.
4. Remote control of circuit breakers or sectionalizing switches.
5. Transferred tripping to provide differential protection for transformer banks used with only a low-voltage circuit breaker.

Situations frequently arise where a power-system planning or designing engineer finds it necessary to install a power transformer with only a low-voltage breaker. This is particularly true under present conditions where new plants must be served with as little additional equipment as possible. In many cases, this resolves into extending a line from an existing station or bus and terminating it with a transformer bank. Another common arrangement is to tap the transformer bank on an existing line, using only a low-voltage breaker.

Both of these arrangements present a problem for the relay engineer, especially the protection of the transformer bank. The standard line relays give some protection for transformer faults but usually cannot be set for the required sensitivity. Some conventional type of differential relay can be used to detect faults in the transformer and cause tripping of the local low-voltage breaker, but this does not clear the fault. If the fault is on the low-voltage winding, or close to the ground end of the high-voltage winding, there is a good possibility that the remote relays will not have the required sensitivity to trip the remote circuit breaker.

In this case it becomes necessary to convey the tripping indication from the local differential relay to the remote circuit breaker, and this can be accomplished by pilot-wire circuits, carrier-current channels, and so forth. Another method of tripping the remote breaker is to place a fault on the high-voltage side by means of a shorting switch which will produce a fault of sufficient magnitude to operate the remote trip relays. It is the purpose of this paper to describe a simple and reliable method of remote tripping transmitted over a carrier-current pilot relaying channel.

The accepted practice for carrier-current pilot relaying channels is to operate them as simple telegraph channels which transmit carrier to hold a blocking relay in the nontrip position at the ends of the line section. In this type of operation, the channel is in use only a short portion of the time for relaying or test, and it is customary to include modulation equipment to provide a testing or emergency telephone channel. The addition of the remote tripping function to the carrier-current channel improves the use factor and requires less critical material than other methods.

As previously stated, the pilot relaying equipment for protection of the transmission line operates on a blocking principle. The carrier transmitter is maintained in a stand-by condition at all times, with full voltage on plate and cathode-heater circuits, but the screen grids of the transmitter tubes are maintained at a negative potential with respect to the cathodes, which prevents the transmitter from operating. When the contacts of the fault-detector relays operate, the screen grids immediately change to a positive potential, and the carrier transmitter is started. If the fault is within the range of protection of the relays, the directional relays operate to re-establish

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the grid bias on the transmitter tubes and stop carrier transmission. As soon as the carrier blocking signal is removed, the receiver relay is free to complete the circuit through the contacts of the tripping fault-detector relay to the trip coil. After the blocking signal is removed, the receiver relay drops out in about five milliseconds. The completion of the trip circuit operates a seal-in relay which bypasses all other relay contacts and holds the trip circuit energized until the circuit breaker opens.

It can be seen that with this mode of operation, momentary blocking of the receiver relay by interference produced by extremely short-time transients, such as lightning, will have no appreciable effect on the successful operation of the relaying system.

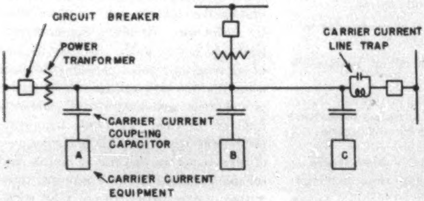
CIRCUIT BREAKER
POWER TRANFORMER
CARRIER CURRENT LINE TRAP
CARRIER CURRENT COUPLING CAPACITOR
A
B
C
CARRIER CURRENT EQUIPMENT

Figure 1. One-line schematic diagram of typical remote-trip installation

A. Transmits and receives remote-trip signal

B. Transmits and receives remote-trip signal

C. Receives remote-trip signal

When the blocking system is inverted and a *tripping* signal is sent instead of a blocking signal, it will be noted that an operation of the receiver relay by interference cannot be tolerated. Since there are no remote fault detectors which recognize the fault in the transformer bank, it is necessary to entrust to the carrier-current channel the complete job of tripping the remote breaker.

There are many possible methods of conveying this remote tripping information over the carrier-current channel. Some of these may be summarized as follows:

1. Keyed code. This requires no change in carrier set but requires mechanical coder and decoder or selector mechanism.

2. Modulating tone applied to carrier. This requires an oscillator at the transmitting end and a filter unit at the receiving end.

3. Frequency shift of carrier. This requires a means for shifting the carrier frequency at the transmitter end and a special receiver to recognize this shift in frequency either by a heterodyne method, by a balanced discriminator, or by simply shifting carrier far enough to avoid interference from the normal carrier frequency.

4. Some combination of the foregoing methods, such as keyed modulation, modulation applied as frequency modulation, or combinations of more than one modulating tone.

Operating time is an important consideration in any protective relaying scheme, and this speed must be accomplished with satisfactory accuracy. Where this tripping signal is being superimposed on an existing channel, also having high speed requirements, it becomes necessary to make some compromise in order to obtain simplicity of equipment. Insofar as the transformer bank is concerned, the tripping time should be instantaneous to limit the extent of the damage. In many cases where no provision is made for tripping the remote breaker, the transformer fault will remain on the system until a telephone call can be put through to the remote station, or events will follow their usual course with the fault spreading until the current is sufficient to operate the remote line-protection relays.

Between the two extremes of instantaneous tripping and not tripping at all, a maximum relay time of 0.5 second was selected as a reasonable value which would make it possible to obtain the required accuracy with simple equipment.

The keyed-carrier method was first investigated since it had the outstanding advantage of requiring no changes in the carrier equipment. There are various factors, however, which offset this advantage since the complications are transferred to the selector mechanism, and these complications increase as the tripping time is decreased. Where this remote tripping function is to be added to an existing carrier channel of older design equipment which did not provide modulation facilities, or a receiver with a satisfactory audio output, there is considerable merit in an arrangement which employs keyed carrier and a selector, although the relay time is increased.

One such arrangement may consist of a transmitter which sends short impulses at a uniform rate of ten per second, and a selector mechanism which will reject any other combination of dots, dashes, or spaces in transmission. Ten consecutive impulses must be received in order to trip the breaker, and the code is either repeated several times or the transmitter runs continuously until the hand-reset auxiliary tripping relay associated with the transformer differential protection is reset. The ten-impulse code thus is repeated several times to assure tripping, since any interference received just before or during a tripping signal may mix up the code and prevent operation. The continued-impulse type of code provides a short recovery time after such an interference.

The carrier-frequency shift or frequency-modulation method requires a separate receiver and introduces some complexity in the transmitter. While this arrangement has been applied in certain cases, the audio-tone method is easily added to existing equipment and has some very practical advantages.

After due consideration of the various points of each method, the arrangement using an audio modulating tone was selected to convey the tripping information over the carrier channel. Factors influencing this decision were:

1. The necessary speed of operation could be obtained with the required selectivity.

2. The equipment could easily be added to the standard equipment for pilot relaying.

The design of this modern carrier equipment provided the necessary modulator and a receiver with satisfactory audio-output characteristics, while the oscillator and filter units were in production as components of other standard carrier-current equipment.

The details of the over-all operation of this system may be summarized as follows: At the sending end, the occurrence of a fault in the transformer bank operates the differential relay, which in turn operates a hand-reset multicontact auxiliary relay. One set of contacts on this relay starts carrier transmission, and another contact starts the audio-oscillator unit which usually is adjusted for a frequency of 3,000 cycles. This 3,000-cycle tone modulates the transmitter output. At the receiving end of the carrier channel, the receiver demodulates the incoming signal, and the resulting 3,000-cycle audio tone is fed into the input of the filter unit. The filter unit consists of a vacuum tube operated as a relay tube with sufficient negative bias to prevent plate current from flowing under stand-by conditions. The tuned circuit is connected across the input to the control grid and adjusted to 3,000 cycles. This arrangement attenuates all frequencies except the 3,000-cycle signal, which, when applied to the grid of the relay tube reduces the bias voltage, and plate current flows in the relay circuit to operate the audio relay. The audio relay is provided with an adjustable time delay of 0.1 to 0.5 second to further improve the over-all selectivity of the equipment by preventing operation on nearby lightning strokes or other causes of interference. When this relay operates, it energizes a hand-reset auxiliary which trips the circuit breaker.

Details of Equipment

The basic carrier equipment for pilot relaying consists of a transmitter-receiver unit, a coupling capacitor to couple the signal to the high-voltage line, and a line trap to terminate the protected line section in a high impedance to prevent the short-circuiting of the carrier channel by external line-to-ground faults on the coupling phase. The equipment is built in units of a standardized width of 19 inches, and a panel space height that is a multiple of $1^{3}/_{4}$ inches. The following paragraphs give a general description of each of the various units which are used to make up a given assembly.

Transmitter-Receiver Unit

This standard equipment is designed for operation directly from either a 125-volt or 250-volt station storage battery, with a carrier frequency range of 50 to 150 kilocycles. The transmitter employs five type-25*B6G* tubes in a master-oscillator power-amplifier circuit. The receiver employs two tubes, one 25*A7G* as a detector and audio tube, and one 25*B6G* as a relay tube.

An important feature of the receiver is the separation of the telephone function from the pilot relaying function. The incoming carrier signal is rectified by a diode unit in the 25*A7G* tube. The resulting voltage is filtered slightly and fed to the grid of the relay tube. This circuit is designed to have a limiting characteristic so that the relay current is essentially constant for all signals within the working range of the receiver. Since changes in incoming signal voltage produce practically no change in plate current, it is obvious that this tube cannot be used to obtain audio output from the receiver. For this reason the multisection type-25*A7G* tube is used since it contains another section which can be used as an audio amplifier. A portion of the a-c voltage developed across the diode-rectifier load resistor is coupled to the input circuit of this amplifier. This arrangement provides the desired characteristics for the relaying and the telephone function and permits obtaining sufficient audio output to operate the audio relay units used for the remote-tripping function.

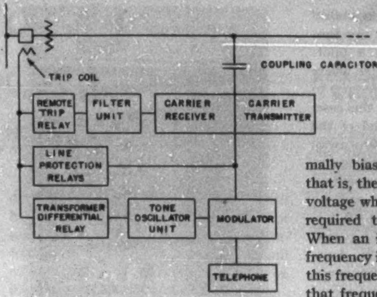

Figure 2. Block diagram of carrier-current equipment showing arrangement for pilot relaying, sending and receiving remote-tripping signal, and telephone

Modulator Unit

The modulator unit employs two type-25*B6G* tubes in a push–pull amplifier circuit, the output of which is applied to the screen grid circuits of the transmitter to modulate the carrier-current output. The 3,000-cycle tripping signal from the oscillator is fed directly into the modulator unit ahead of the speech input volume control in order to obtain independent control of both of these functions. A 3,000-cycle filter is added to the microphone circuit to eliminate this frequency from the voice circuit. This filter has negligible effect upon the intelligibility of the voice signal since the bulk of the voice frequencies lie below 2,000 cycles.

Audio-Oscillator Unit

The oscillator unit required for the remote tripping signal usually is adjusted to 3,000 cycles. This adjustment is made by changing the capacitor combinations and moving the iron core in the inductance. The oscillator tube is a twin triode, one side of which is used for the oscillator, while the other side serves as a buffer amplifier to prevent changes in frequency due to changes in external loading. A filter is provided on the power supply to eliminate the possibility of any ripple on the station battery voltage supply from being amplified and fed into the modulator unit. The oscillator is normally in a stand-by condition and is started by completing the cathode circuit to the negative power supply. The output voltage of the oscillator is adjustable to control the degree of modulation applied to the carrier wave.

Filter Unit

The filter unit is designed to work with the audio-oscillator unit, and contains two tuned circuits which are adjustable over the same range as the oscillator unit. Each tuned circuit is connected across the input circuit of a relay tube which is normally biased so that no current flows; that is, the control grid is maintained at a voltage which is more negative than that required to cut off the plate current. When an incoming signal of the proper frequency is received, the circuit tuned to this frequency offers a high impedance to that frequency and permits a voltage to appear on the control grid circuit of the associated relay tube. This voltage is alternating, of course, and on the positive swings the control grid voltage is made positive thus causing plate current to flow in the relay circuit.

Sockets are provided on the filter unit for mounting two relays. In this case, the relay associated with the 3,000-cycle tone is located on the switchboard while the relay for other tone is mounted on this unit.

Relay Unit and Accessories

The adjustable time delay on the tripping relay is obtained by means of a small auxiliary relay with a copper slug on the coil, and the time is adjustable by varying the air gap and spring tension. This relay is picked up normally, and the time is obtained on the drop-out. The coil of this time-delay relay is energized from the station battery, through normally closed contacts on the audio receiver relay which is mounted in the same enclosing case. When an audio signal is received to produce tripping, the audio receiver relay picks up, thus interrupting the coil circuit of the time-delay drop-out relay, and at the same time it closes one set of contacts in the circuit to the type-*HEA* hand-reset auxiliary relay. When the time-delay relay closes its contacts, it completes the circuit to the *HEA* and trips the circuit breaker.

Every protective device requires provision for test and maintenance, and this is provided for by a test switch, a milliammeter to read the audio-receiver-relay current, and a signal lamp to show when a tripping signal is being received. The purpose of this signal lamp is to warn the operator that the *HEA* relay should not be reset since the tripping signal is still on the line.

Some Details of Operation

Whenever two or more services are placed on any transmission medium, it becomes necessary to consider the possibility of interference between these services. The addition of telephony on a pilot-relaying channel is a good example where it is a simple matter to give preference to the more important service. In this case, fault-detector relays at each end of the channel are arranged to take control away from the telephone function if necessary. The remote-tripping function presents a different problem, since in this case no indication of the fault exists at all terminals; in fact, that is the exact reason the transferred tripping must be done. Since normally only one carrier frequency is used for both transmission and reception, the local receiver is desensitized by the strong signal produced by the local transmitter, and although it is possible to hear the incoming 3,000-cycle tone in the telephone receiver, it is of insufficient strength to operate the audio relay. Upon hearing this audio tone, it is a natural impulse for the operator to shut off his carrier transmitter since he assumes that the remote station is calling him, which of course is correct. As soon as the local transmitter is stopped, the audio signal increases greatly in volume and trips the circuit breaker. This difficulty could be avoided by using two different frequencies for transmission and reception, but this would necessitate changes in the method of protection on lines having more than two terminals and introduces certain other undesirable complexities.

There is no interference between the operation of the normal pilot relaying equipment and the remote-tripping equipment, since a transformer fault of sufficient magnitude to cause the pilot relays to operate will not require the assistance of the remote-tripping equipment.

Overmodulation of any carrier transmitter should be avoided since it produces harmonics in the audio as well as the radio frequency output. Accidental overmodulation of the transmitter at 1,500 cycles will produce some second harmonic output of the receiver at 3,000 cycles which may be sufficient to operate the tripping relay. To eliminate this possibility, the second audio-filter unit is tuned to 1,500 cycles and arranged to desensitize the 3,000-cycle filter when a 1,500-cycle tone is received. With the input adjusted for normal speech, the 1,500 cycles present in the telephone feature is insufficient to operate the 1,500-cycle relay.

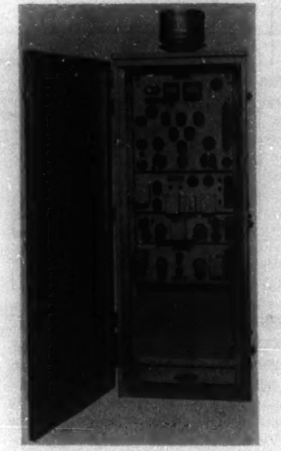

Figure 3. Carrier-current pilot relaying equipment, with oscillator and filter unit for transformer differential protection, in cabinet for outdoor mounting

Other Uses

In cases where there are three terminals of carrier on a line section, each set being equipped with remote-trip transmitter and receiver equipment, it will be found very useful to use a 1,500-cycle tone for telephone calling by using a simple code for each station. By this method the alarm bell will not operate at the third station while the other two stations are using the phone.

In cases where the remote circuit breaker is equipped with high-speed reclosing equipment, it is desirable that this feature should function only when the breaker trips for transmission-line faults. The relay connections should be arranged so that it is impossible for the automatic recloser to operate in any case where the circuit breaker is tripped by the transformer differential relay. After the faulty transformer has been removed from the circuit, a 1,500-cycle tone may be used to release the lockout thus restoring the breaker to normal operation.

Cases frequently occur where it is required to control a single remote circuit breaker or disconnect switch at an unattended substation. From the previous description it is evident that the remote-trip equipment can readily be adapted for this type of service, and by the addition of the proper auxiliaries an indication of the position of the remote device also can be obtained.

Installations

New system interconnections, where the tie points are at different operating voltages, and new loads tapped on existing lines have resulted in a considerable number of installations of this type of equipment. These applications have included the following arrangements:

A. Single circuit line with transformer at one end.

B. Double circuit line with transformers at one end.

C. Single circuit three-terminal line with transformer at one terminal.

D. Single circuit three-terminal line with transformers at two terminals.

E. Single circuit three-terminal line with transformers at all three terminals.

Critical-Material Conservation in Induction-Motor Manufacture

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Present Importance of Avoiding Over-Motoring

In several articles which recently have appeared in the technical press, it has been indicated that, in the interests of the war effort, standards of reliability in service may have to be compromised. As an example, one author advocates that electric motors should, in some cases, be utilized with continuous loads 25 per cent greater than has been considered good practice heretofore, "particularly if the ambient temperature within which the motors are expected to operate, is less than the recognized permissible maximum of 104 degrees Fahrenheit." That author adds the caution that this may not be practicable in all cases, since other factors than the continuous thermal rating sometimes determine the motor size. Then he adds: "However, in most war plants, such cases are relatively few."

Proposed Plan

In the present paper, careful consideration is given to the rating-up amounts which are desirable in the case of induction motors, in view of the acute urgency of conserving critical metals. The conserving of *copper* is of chief importance, but by the plan here presented there is effected at the same time a great saving of other metals. The plan recommended in this paper does not involve any compromise in "standards of reliability in service."

Predominating Importance of Induction Motors

Of all electric motors the induction motor is the one of which the greatest quantity is required by industry. If, for the total number of induction motors which will be manufactured in the year 1943, the per-horsepower consumption of copper and other metals can be reduced by 25 per cent, as compared with heretofore practice, and by plans not involving delays for redesigning and retooling, thousands of tons of copper and other metals will be saved and can be made available for direct military purposes. It is the intent of this paper to describe means for effecting this large saving without the occasioning of any delay in the production program of the manufacturers of induction motors.

The heretofore (and present) basis of rating of induction motors, so far as it relates to the limiting service temperatures, has been extremely conservative, as the result of a determination to ensure

1. Large margins of safety.
2. High service dependability.
3. Long life.

Furthermore, the basis was adopted a good many years ago, when the customarily used insulating materials of reasonable cost and ready application were considered inadequately heat resisting to withstand higher temperatures for long service periods. In recent years, wide experience has been gained with various insulating materials which will endure subjection to much higher temperatures for long periods of service. While usually these materials are more expensive than the early thermally inferior materials, several amongst them are manufactured in sheets, tapes, and all other required forms, so that they may be substituted without any redesigning of the motors, dimensional or otherwise. This substitution is an important feature of the conservation project described in the present paper.

Under the comfortable conditions of peacetime, no appreciable headway was made with rating-up proposals, notwithstanding the known availability of these better insulating materials. Now it is apparent that the conservation of copper and other metals is of such vital importance that all will co-operate eagerly in seeking for sound plans for the accomplishment. It is important to make the change-over with a minimum of delay. Any dimensional changes in the design and in the manufacturing specifications inevitably would interrupt smooth and rapid large-scale production and would be accompanied by disastrous delays.

Reduction of the Per-Horsepower Weight Achieved by Rating Up

Consequently it appears desirable that the *dimensions* of the present low-temperature—but nevertheless highly developed and excellent—designs shall be retained and that the reduction of the per-horsepower weight shall be effected by rating up the motors at present being manufactured. The only change in their construction will be the substitution of the high heat-resisting insulation but of precisely the same *dimensions* as the insulation heretofore used.

33 Per Cent Increased Rating

This plan will permit of increasing the rated output by 33 per cent, and reducing by 25 per cent the per-horsepower weight of copper and other metals required in the construction.

Precedents for Employing Higher Service Temperatures

Railway motors, totally enclosed motors, and the insulation of the rotors of turbogenerators long since have been standardized on the basis of much higher permissible service temperatures than those allowed for general-purpose induction motors. Contrary to predictions these three (and other) higher-temperature products have experienced no undue life impairment, when suitable heat-resisting insulations have been skillfully employed. Similar success will attend the use now proposed of higher service temperatures for the induction motors.

Present Severe Service Conditions Render High Stalling Loads Particularly Desirable

Let us now consider how the case stands with respect to limitations to rating up, other than thermal limitations. Leaving the motor design just "as is" (that is, on the present low-temperature basis), most sizes, particularly those for low speeds, will not have an adequately conservative stalling-load margin by the time their 33 per cent greater (new) rated loads are reached. Unless we do something about it, the margin between the (new) rated load and the original stalling load often will be too small, particularly for the rough and severe conditions of present-day production. The motor will stall at a

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lower load, the greater the drop in the supply-line voltage, and vice versa. It is important to realize that, with the high loading of power circuits now becoming usual, (and the necessarily associated great voltage drops) and with the intense incentive to accomplish tasks in the shortest possible time, there frequently will result the imposition upon the driving motors of much more severe voltage drops and momentary load peaks than are apt to be experienced in normal times. Evidently, while our rating-up plans will not encounter any difficulties as regards liability of the occurrence of breakdowns due to overheating, there is the need that there still shall be provided very liberal reserves of torque, over and above the torque corresponding to the increased rated load, so as to remove all likelihood of production interruptions from motor stalling. Such motor stalling would be more intolerable, in view of the emergency circumstances, than ever it could have been in peacetime.

Inherent Relations Between Temperature Limits Imposed, Stalling-Load Requirements, Periodicity, and Rated Speed

This matter of ensuring that the induction motor should possess a momentary stalling load liberally in excess of the rated load (while sometimes, particularly in the case of low-speed motors, it caused the motor designer to depart from otherwise best proportions) did not constitute a major problem, so long as the limiting service temperatures were required to be very low. That condition tended strongly (in almost all sizes and types), to make the permissible service temperature the limit of rated output. But now, the boot is on the other leg. Going over to the endorsement of higher service-temperature limits for induction motors brings us right up face to face with the condition that the momentary stalling load henceforth usually will constitute the actual limitation on the rated load for which a motor always can be relied upon to be liberally adequate in service. Fortunately, it has become recognized as good practice to employ for induction motors the highest practicable speed. For a given periodicity, the inherent stalling load (broadly speaking) is greater, the greater the rated speed. Obviously, this paper is concerned chiefly with the conditions in 60-cycle systems. For lower periodicities (such as 25 cycles), the provision of an adequately high stalling load rarely will impose a limit on the rating, except for extremely low-speed motors.

The stalling load can be increased by increasing the voltage at the motor's terminals. But the supply-circuit voltages have become so thoroughly standardized, and the motor designs have been standardized for so long a time for these particular supply-circuit voltages that, in order to provide at the motor terminals a voltage higher than the standard supply-circuit voltage, it has been necessary (1) to use special step-down transformers with secondary voltages higher than standard, or else (2) to boost the secondary voltage by interposing an induction regulator between the supply circuit and the terminals of the motor. But, while the higher terminal voltage will increase the motor's stalling load, it will impair both the power factor and the efficiency at light loads. A long time ago, the proposal was made (and used, to a certain extent), to provide a low terminal voltage at light loads and gradually higher voltages with increasing load (so as to provide the best characteristics at *all* loads) by equipping the transformer secondary with several taps and with means for transferring automatically the induction motor's terminals from tap to tap as the load increases. Tap-changing-under-load transformers could, of course, be used, the change being effected by an automatic regulator responsive to load changes. Feeder regulators, or special or complicated transformers, however, would take the gilt off the gingerbread and leave us using as much (or even more) per-horsepower weight of copper and other metals (for induction motor plus regulator) as for the original low-temperature (and high-copper) motors with their high reserves of stalling torque.

Desirability of Retaining (At Any Rate, for the Duration) The Present Excellent Designs of Induction Motors

Actually, it is the volts per turn which we desire to increase, in order to increase the stalling torque. The simplest way to increase the volts per turn obviously is to decrease the number of turns and increase the magnetic flux. This, however, means new designs and the inevitable great delays for development and testing. (*Even then* there will be incurred a certain amount of impairment of the motor's light-load characteristics.) Furthermore, after years of continual improvements effected in the course of many redesignings, most induction-motor manufacturers (on the basis of the standardized low-temperature limits) have arrived at lines of very well balanced designs in which the polar pitch, and the core length, and the volts per turn for each particular rated output and speed and voltage and periodicity are so nicely balanced as to provide about the best practicable combination of high power factor, efficiency, starting torque, and reasonably low starting current.

Clearly, it is of prime importance to salvage these many fine designs (at any rate, for the duration) and thus also have the great advantage of eliminating the enormous and time-consuming task of developing new lines of induction motors to fit the higher permissible service temperatures and decrease the per-horsepower weight of copper and other metals. That is not a task which possibly could be accomplished in a few weeks (or even in less than a good many months), and also it would be likely to require considerable retooling. For each of the usual horsepower sizes there are required designs for various voltages and speeds and degrees of enclosure and other subclassifications. A fresh start would require, in this case, the laborious and time-consuming development and testing of many dozens of new designs of induction motors.

Thus it appears that an effective plan, not involving any delays, for reducing the per-horsepower weight of copper and other metals required in induction-motor manufacture is to continue building the present induction motors without any changes except that which has been indicated, namely, the use of the better insulations with high heat-resisting properties. The plan requires the provision of some *external* means, or some *externally* located material, for increasing considerably the stalling torque so that it shall be liberally adequate for the conditions of the increased rating, and for the more intense and strenuous conditions of service consequent upon the war effort. Especially in war plants the certainty of freedom from even the slightest service interruptions is of vital importance. Already it has been explained that, for this requirement of increased stalling torque, it will be necessary to provide some means whereby any instantaneous or sustained overloads shall be accompanied by an adequate increase in the volts per turn. Furthermore, it will be desirable that the means employed shall embody features contributing toward improving the motor's light-load characterisics by *decreasing* the volts per turn at light loads.

Use of a Hookup With Series and Shunt Capacitors

As an effective means for providing that the volts per turn shall automatically increase with increasing load, there may be employed a plan which was conceived 21 years ago. At that time, there was no urgent need for providing induction motors with this feature of variable volts per turn. Also in other respects, the proposal was regarded as premature. But the present greatly changed conditions indicate the method to be particularly timely and appropriate. The following is a description of it.

Description

Three series capacitors are interposed between the three-phase supply circuit and the three terminals of the windings of the induction motor. In addition, there are provided across the motor terminals three shunt capacitors. Either the series capacitors are designed for a current equal to the current in the motor's windings or they may be located in the secondaries of three small transformers whose primaries are interposed in series with the three motor terminals.

The shunt capacitors are proportioned for a current equal to the lagging component of the motor current at its rated load (that is, they are proportioned for complete compensation for unity power factor at rated load). Consequently the power factor is leading for all loads from no-load up to the rated load. (So far as concerns the employment of shunt capacitors with induction motors, this already is widely practiced, and it so improves the economics of the supply system as usually to amply justify the outlay for the required shunt capacitors.)

The Purpose of the Series Capacitors

For the purposes of the project described in this paper, the series capacitors are so proportioned as to decrease and increase the motor's terminal voltage by desired amounts below and above the constant voltage of the supply circuit. For loads less than some selected value (rated load, in the case being considered), the lagging current into the motor is overcompensated by the leading current in the shunt capacitors. The resultant current from the supply circuit, being a leading current, causes the motor's terminal voltage to be less than the supply circuit's voltage, because of the voltage drop which is occasioned in a capacitor when a leading current flows through it. From rated load upward, the opposite result is obtained, and the motor's terminal voltage increases as the load increases, because the current through the series capacitors is lagging.

Illustrative Example

As an example, let there be considered the case of a three-phase 60-cycle 4-pole 480-volt squirrel-cage general-purpose induction motor, which, for the heretofore usual low-temperature practice, is rated at 75 horsepower. At its rated voltage of 480 volts, such a motor will carry double load (that is, 150 horsepower) without stalling. By providing this 75-horsepower motor with the capacitor hookup recommended in this paper, it may be rated up to 100 horsepower and will then, without stalling, carry momentary overloads up to 200 horsepower. Of the three series capacitors for this purpose, each will have such capacitance as to increase the motor's terminal voltage from the supply circuit's constant voltage of 480 volts up to 555 volts when the load has increased from rated load of 100 horsepower, up to twice the rated load namely, 200 horsepower. Conversely, these series capacitors serve to decrease the motor's terminal voltage down to values below 480 volts, to an extent dependent upon the amount by which the load decreases below the rated load.

Aggregate Improvements Thus Provided

This decrease of the voltage at low loads and its increase at high loads also serve to increase the low-load and high-load efficiencies above the values which can be realized with constant voltage maintained at the motor terminals for all loads. Not only are the improvements resulting from these various effects, accompanied by increased power factor and efficiency at all loads, but also they endow the (motor plus capacitors) combination with a leading characteristic for all loads up to the rated load, and they afford a means for increasing the stalling load above that attainable in a constant-voltage design and consequently also a means for increasing the rated output. Finally, there is thereby effected an important decrease in the per-horsepower weight of critical material required in the motor's construction.

Rating of Required Capacitors

It has been stated already that the provision of shunt capacitors for obtaining power-factor improvement long since has been admitted quite generally to justify their cost. The economies resulting therefrom extend throughout the entire system over which the induction motors are supplied and relate both to reduced capital outlays and reduced losses in all the material interposed between the source and the induction motors supplied therethrough. The further-mentioned advantages of greater motor efficiency at all loads, increased rated output, and decreased per-horsepower weight of critical metals undoubtedly will be agreed to justify the outlay for the series capacitors. In the cited example of a 100-horsepower motor with a stalling load of not less than 200 horsepower, the required amount of series capacitance comes to 30 kva, provided in three 10-kva capacitors interposed betweeen the supply-circuit terminals and the motor terminals. These series capacitors boost the system voltage from 480 volts at a load of 100 horsepower, up to 555 volts at a load of 200 horsepower, the currents per phase for these two loads being respectively 100 amperes and 220 amperes.

Series-Capacitor Compounding Transformer

When, in the interests of decreasing the losses in the distribution circuit, load-center location close to the induction motor is provided for the step-down transformer (or group of transformers), it will be practicable, often, to decrease the outlay for the series capacitors by locating them in the high-voltage side of the step-down transformers. Thus, for the example chosen (a 480-volt induction motor), and a primary voltage of 4,800 volts, the voltage stress across each series capacitor would then be much more favorable for the design of the series capacitors with respect to decreasing their cost. This plan amounts to providing *compounding* transformers. For the particular case of the illustrative example, the secondary voltage of the (nominally) 480-volt transformer would increase with the load from a value of, say, 415 volts at no load of the induction motor up to 555 volts just below the occurrence of the motor's stalling load.

Development of Power Capacitors

In the period from 1919 to 1922, when this project and other *Series*-capacitor power projects were proposed (although *shunt* capacitors already were sufficiently common in power applications to be quite familiar to power engineers), the idea of a *series* use of capacitors with *power* apparatus was considered by many power engineers to be of questionable soundness and in need of demonstration. Another objection was that power-capacitor manufacture was then only in its early beginnings. Paper of the desired quality and with the necessary freedom from objectionable constituents remained to be developed. The paper needed to be of extreme thinness so that weaknesses at various places in its extent could be averaged out by the super position of several paper layers between adjacent conducting foils. Processes of evacuation and impregnation still left much to be desired. The demand for capacitors for power applications was out of all proportion too small to enable the cost to be satisfactorily decreased by large-scale mass production. Furthermore, those were days of extravagance and waste. The conditions were the reverse of propitious for attracting interest to a proposal which was regarded as both premature and unimportant. That, admittedly, then was the case with this proposition.

In 1924, however, presumably out of a spirit of amiable tolerance toward persistent and reprehensible importunacy, the scheme was made the subject of a patent application, and patent 1,595,937 of 1926 finally emerged. It is probable that the method never has been employed in service. The present emergency circumstances and the timely advantages which the project provides would seem to be providentially opportune.

Summary

There follows a list of some of the attendant circumstances and of the advantages which may be gained by the use of the project:

1. Thoroughly reliable power capacitors now are manufactured in great quantities by many firms.

2. Cost is much lower than in 1922.

3. By simple constructions embodying the external-pressure principles by means of which Hochstadter has demonstrated that the service insulation strength of underground high-voltage cables can be doubled for a given insulation thickness, the per-kva cost of power capacitors readily may be decreased much further.

4. By the use of power capacitors as a simple adjunct the power factor and efficiency of an induction motor may be considerably improved for all loads, light and heavy, and the stalling load may be much increased. Throughout a considerable range of load, system economies associated with a leading current will be obtained.

5. Higher-temperature service operation now is recommended, and the rating of the present highly-developed, low-temperature, induction motors may be increased by 33 per cent, without requiring any delays, since no re-designing is involved. There is required merely the substitution of the high heat-resisting insulation now available.

6. The project will permit of the conservation of considerable quantities of critical materials as the result of decreasing by 25 per cent, the per-horsepower weight of the motors.

The recommendations made in this paper (and the means proposed) for increasing by 33 per cent the present service rating of general-purpose induction motors is more conservative than many will realize. It is anticipated that, when the electrical industry has acquired more confidence in the adequacy of the available better insulations (as regards withstanding heat deterioration), practice will advance to the extent of permitting even higher service temperatures and securing further reductions in the per-horsepower weights of copper and other metals which it is of such critical importance to conserve. But rather than jeopardize the acceptance of his recommendations, the author has refrained from advocating still greater amounts of service-rating increase.

WPB L-221

In a note on page 81 of the February 1943 issue of Electrical Engineering, the War Production Board's general conservation order L-221 is stated to contain a provision whereby the horsepower of a new electric motor shall not exceed the requirements of the specific job for which the motor is purchased. Compliance with this and other conservation provisions contained in the order (so far as it relates to general-purpose motors) are expected to conserve annually about 6,000 metric tons of copper and 55,000 metric tons of carbon steel. In the present paper attention has been confined to the specific case of general-purpose induction motors, and means particularly applicable thereto are recommended for effecting savings of amounts of critical materials which should exceed the amounts envisaged in WPB L-221. These recommended means also will improve the operating conditions as respects efficiencies, power factors, stalling loads, and reduced rate of life impairment.

Ambient Conditioning

In conclusion, attention well may be called to the present advanced state of development of systems for the conditioning of factory and workshop premises. While consideration for the health and efficiency of the personnel quite rightly is the main objective, it is not out of place to consider also the extent to which the rate of life impairment of the *motors* may be decreased by decreasing the ambient temperature in the midst of which the motors' existences are spent. The 68 degrees Fahrenheit which is beneficial for the personnel should be capable of being closely approached, even in the immediate surroundings of the motors, rather than any approach to the temperature of 104 degrees Fahrenheit now assumed in the rating regulations as being a temperature of likely occasional occurrence. It will, of course, be necessary to take into account the fact of the relatively great amounts of heat being developed in the motors as a consequence of their losses. But by simple applications of fundamental principles of local air circulation, accentuated when desirable by surface vaporization methods of heat transfer, the desired 68 degrees Fahrenheit temperature should be capable of close approach at all seasons and localities and even at distances of relatively few inches from the external surfaces of the motors.

A New High-Speed Balanced-Current Relay

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NEED for a high-speed, balanced-relay current which will not function when the first half-cycle current is deficient in one of the two parallel circuits being protected, has been shown by the recent paper "The Effect of Current-Transformer Residual Magnetism on Balanced Current or Differential Relays."[1] This is the result of high residual magnetism of the current transformer caused principally by the d-c component of an offset wave on previous short circuits.

A high-speed, induction-cylinder-type relay has been developed in which the operating-coil circuit has an inherent delay of approximately one cycle, due to the time constant of a resonant circuit. The restraining torque of the relay is effective in a small fraction of a cycle and is obtained by using a flux-shifting copper tube acting as a lag ring or short-circuited secondary.

Application

Several AIEE papers[2,3,4] have presented the development and application of high-speed relays in which the speed of operation was recognized as an essential characteristic in obtaining greater system transient stability. Since the standard time for circuit-breaker operation was reduced to eight cycles or less, this meant that relay operating times of approximately one or two cycles were necessary to realize the full benefit of this circuit-breaker operation. In designing a new relay, one must recognize the increasing demand for high speed, but its attainment must not be accompanied by sacrifice of either selectivity or reliability.

The balanced-current relay is most generally used to provide protection against current unbalance at the source end of parallel untapped transmission lines, where the unbalancing is occasioned by phase-to-phase or ground faults in either line. It may also be used for a similar protection at the receiving end of parallel lines provided there is an additional source of power supply (other tie lines, generators, or synchronous apparatus) at the receiving end which will increase the fault current in the defective line at least 10 to 25 per cent. The schematic one-line diagram, Figure 1, illustrates this application and emphasizes its limitation for the load end protection where the currents in breakers *A* and *B* would be equal.

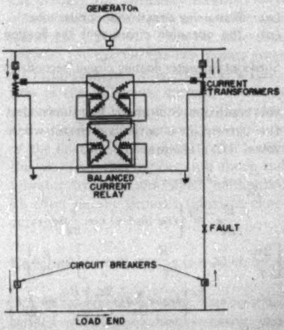

Figure 1. Schematic one-line diagram illustrating balanced-current protection of parallel lines

Operating requirements for the new relay cannot be reduced from that followed on existing high-speed, balanced-current relays. This means that satisfactory protection for simultaneous faults on the parallel lines, protection on single-line operation, high-speed auxiliary switches or interlocking relays, operation with bus tie breaker, proper functioning during magnetizing inrush, and current-transformer characteristics are items for consideration. Besides these factors tending toward faulty operation, if improperly handled, there have been recorded occasional false trippings which, until recently, were without satisfactory explanation.[1,5] Now, since it has been shown that residual magnetism in the current transformer may result in false operations with existing high-speed relays, a relay has been designed to function correctly although this undesirable, yet unavoidable, current-transformer characteristic exists.

Description and Operation of Relay

The relay unit with eight-pole, induction-cylinder construction previously presented before the Institute[6] is inherently a high-speed device and readily lends itself to balanced-current operation. The available poles permit both the operating and restraining functions to be accomplished in a single unit. The single relay unit protects only one phase of one line and the relaying of both lines is accomplished with a two-element relay shown in Figure 2. For complete balanced-current protection at the generating end of two parallel lines, three relays are sufficient; two for phase-to-phase faults and one for ground faults.

Six of the available eight poles are used, and their location is shown in Figure 3. The left three coils, denoted by *A* and *B*, are the operating coils while the right three coils, *C* and *D*, provide the restraining torque. These torque-producing elements exert their force on a common rotor in opposition to one another to give a balance. The production of this torque is like that of a wattmetric

Figure 2. Single-phase high-speed balanced-current relay

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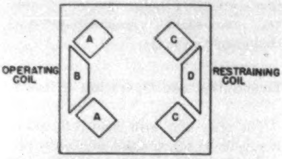


Figure 3. Coil locations in a single element of the balanced-current relay

element, and in the case of Figure 3, the coils B and D produce rotor currents which react with the flux of their adjacent coils. The torque magnitude is a function of the product of the flux and the sine of the angle between them. From this relation and the connections of Figure 1 it is obvious that some means must be provided to obtain a phase displacement between the pole flux for both the operating and restraining circuits.

For the restraining circuit a flux-shifting copper tube is employed. This method is similar to one used in current-polarized directional units of the induction-cylinder construction. The vector diagram, Figure 4*A*, gives an approximate time-phase relation for the restraint circuit showing the flux shift in the corner poles (*C* of Figure 3).

For the operating function, the necessary flux shift is obtained by means of a floating circuit. The purpose of this flux-shifting method is to permit the use of a tuned circuit in which the delayed current build-up retards the action and the operating torque. As can be observed from Figure 6, the impetus imparted to the relay by the difference current occurs during part of the first cycle depending on the nature of the fault, and the delay required does not detract from the high speed (Figure 8) of the relay.

The floating circuit is composed of a many-turn coil to replace the copper tube and tuned with a capacitor mounted inside the relay case. The circuit constants can be modified with the use of a reactor, resistor, or both, as shown in Figure 5, to control the magnitude and time. For this condition, Steinmetz[7] has shown that the current in a series a-c circuit where $R^2 < 4L/C$ is equal to

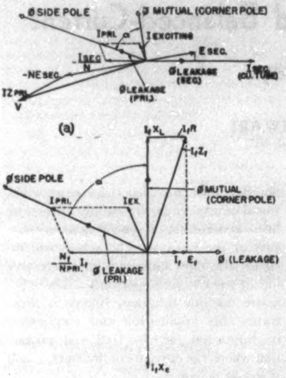


Figure 4. Vector diagrams showing flux relations

(*a*). Restraining circuit with copper tube

(*b*). The operation circuit with the floating circuit

Subscript *f* denotes floating circuit (secondary)

$$i=\frac{E}{Z_0}\cos(\theta-\theta_0-\gamma)-$$

$$\frac{E}{Z_0}\epsilon^{-\frac{R}{2x}\theta}\left\{\cos(\theta_0+\gamma)\cos\frac{q}{2x}\theta+\right.$$

$$\left.\left[\frac{2x_c}{q}\sin(\theta_0+\gamma)-\frac{R}{q}\cos(\theta_0+\gamma)\right]\sin\frac{q}{2x}\theta\right\}+$$

$$\epsilon^{-\frac{R}{2x}\theta}\left\{i_0\cos\frac{q}{2x}\theta-\frac{2e_0+Ri_0}{q}\sin\frac{q}{2x}\theta\right\}$$

Where

E = maximum impressed voltage
R = resistance
x = inductive reactance
x_c = capacitive reactance
$Z_0=\sqrt{R^2+(x-x_c)^2}$
$\theta=2\pi ft$
t = time
f = frequency
$\gamma=\tan^{-1}\frac{x-x_c}{R}$
$q=\sqrt{4xx_c-R^2}$
θ_0, i_0, and e_0 = values at time equals zero

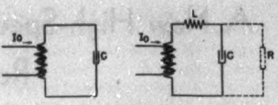


Figure 5. Diagram showing two forms of floating circuits

This equation consists of three parts:

1. The permanent term which is remaining after the transient is completed.
2. The transient term which is dependent on the circuit constant.
3. The third term depending on the initial electrical conditions. This last term disappears if the circuit is dead at the start.

A vector analysis (Figure 4*B*) for the floating circuit represents the flux relation during the steady-state operation. The angle α represents the phase displacement between the flux in coils C and D, and the torque is a function of the sine of this angle.

The oscillogram (Figure 6A) shows the current build-up in this circuit, and the magnitude for the first half cycle is only a fraction of its maximum steady-state condition. This leaves the operating circuit deficient in torque whereas the restraint build-up is immediate and has a restraining bias even though its current supply from the current transformer is affected by the residual magnetism.

Relay Characteristics

In reference to Figure 1 it will be noted that the operating coil of each unit is connected in series with the restraining coil of the other unit. Therefore, the fault current of one line which operates one of the units increases the restraint in the other, thus providing a selective operation. Figure 7 gives typical operating characteristics for both ground and phase relays with a 125 per cent slope. This slope has been used in the application of balanced-current relays to assure sufficient margin for the inequalities of current-transformer characteristics, line characteristics, and other factors af-

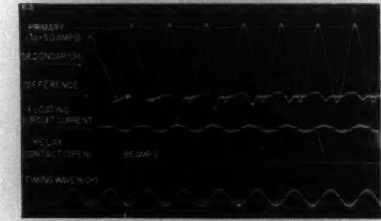
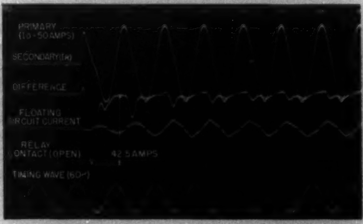


Figure 6. Oscillograms showing operation of relay during difference currents caused by residual magnetism. Two fault conditions are represented

(*A*). With an offset wave

(*B*). Symmetrical current wave



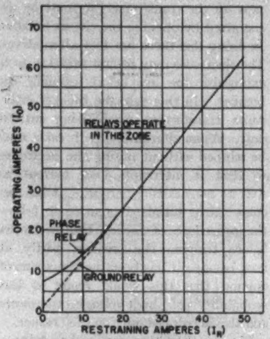


Figure 7. Operating characteristic of phase and ground balanced-current relays

fecting the current balance. When these items are corrected or compensated, it should be satisfactory to use a relay functioning with a slope of 110 per cent.

The high-speed time characteristic is indicated in Figure 8 for this relay.

Tests

The testing program was composed of two parts:

1. A relay having all the characteristics presented in the forepart of this paper had to be developed and tested.
2. This same relay had to be subjected to operating conditions which equaled those found in the field. This part was conducted as a continuation of the work already covered on the current transformers and presented before the Institute.[1]

Typical oscillograms were taken using the test circuit of Figure 9, and these are reproduced in Figure 6 to show the instantaneous relation between the circuit characteristics as they affect the relay. The traces indicate current-transformer primary and secondary currents, the difference (or error) current, the operating-circuit build-up, the relay contacts, and the timing wave. With a 1/1 ratio current-transformer tests were made with the primary and secondary windings each connected in series with an oscillograph shunt and vibrator. Then the difference between the drop in the two shunts gives an instantaneous value which is a function of the difference current, and it is recorded by a third vibrator. Likewise, the instantaneous current magnitude of the floating circuit was recorded with the use of another oscillograph shunt and vibrator by a means which minimized its effect on the circuit's transient characteristic.

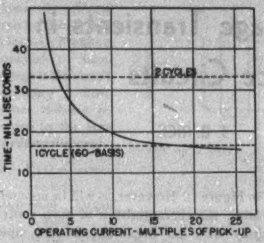


Figure 8. Operating time of high-speed balanced-current relay at zero, 5 and 20 amperes restraint

Conclusions

A balanced-current relay has been described which gives fast, sensitive protection comparable to that previously available yet faultless in operation when energized from current transformers previously saturated and containing residual magnetism. This has been accomplished at the expense of a slight delay of approximately one cycle which is easily tolerated. This has resulted from the application of the floating-circuit build-up to override the error current of the first cycle. Also, it can be expected that this feature will be useful in the design of other high-speed relays.

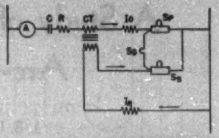


Figure 9. Test circuit used for checking relay operation and for oscillographic measurement of difference current

A—Ammeter
C—Synchronous switch
CT—Current transformer (ratio one to one)
I_0—Relay operating coil
I_R—Relay restraint coil
R—Reactor or load box
S_D—Oscillograph element to record difference current
S_P and S_D—Shunts with oscillograph elements; primary and secondary currents respectively

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A Study of Voltage Transients in Arc-Furnace Circuits

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In the history of the operation of arc furnaces for melting steel there have been occasional evidences of overvoltage transients. These evidences have been chiefly electrical breakdown of equipment associated with arc furnaces which were not attributable to causes other than abnormally high voltage. In the last three years with the increased number of arc-furnace installations and the increased seriousness of interruptions to production, a considerable amount of attention has been focused on this problem. This paper presents the results of voltage surge measurements made on two furnace installations in an attempt to accumulate accurate data as to the magnitude of such overvoltages and the factors which influence them. Before entering into a discussion of voltage surges, however, it seems advisable to review the component electrical parts of an arc-furnace circuit and the procedure followed in its operation.

Furnace Circuits and Their Operation

Figure 1*a* is a simplified diagram showing the parts of a typical arc-furnace circuit, and Figure 1*b* is the corresponding schematic single-line diagram. In all but very large installations the separate reactor *R* is necessary because the inherent reactance of the circuit is not sufficient to produce over-all circuit stability so that an arc can be maintained. The vertical positions of the carbon electrodes in the furnace are individually and automatically controlled so as to maintain as nearly as possible constant current in each electrode. This regulating means is not shown in Figure 1. However, as will be apparent later, the characteristics of such control may have an important bearing upon voltage surges caused by instability of the arc, particularly during the melt-down period when the arc is most unstable. A companion paper[1] describes a new electrode control which it is believed has the advantage of increasing the stability of the arc.

After the furnace has been charged usually with steel scrap, the operator closes the circuit breaker which energizes the furnace transformer. The carbon electrodes are still clear of the charge, and therefore there is no load on the transformer. The operator now lowers the electrodes until they come in contact with the charge and an arc is initiated. The automatic control then takes over and attempts to regulate the electrodes so as to maintain constant current. However, in the initial part of the heat, before a considerable portion of the charge has been melted, it is not possible to maintain constant current, and the load fluctuates quite wildly between short circuit when the electrodes are in contact with metal and virtual open circuit when the arc may be completely extinguished or at least reduced to a very low current.

During this melt-down period the furnace transformer is operated in the upper range of the available secondary voltages with a correspondingly high value of reactance if a separate tapped reactor is used.

After the charge is completely or mostly melted, the operator changes taps on the transformer to obtain a lower secondary voltage and usually a lower reactance, so as to supply power to the furnace at a reduced rate. In doing this the operator must open the circuit breaker to deenergize the transformer. The melt is then continued for perhaps an hour to an hour and a half until such time as it becomes advisable to further reduce the rate of power input to the furnace. At this time the operator again changes taps on the transformer to obtain a still lower secondary voltage, and usually all the reactance other than that inherent in the transformer is cut out.

Each time that the operator changes taps on the transformer, it must be deenergized by opening the circuit breaker. In doing this there are two alternatives. The electrodes may be first raised so as to extinguish the arc before opening the breaker. In this case the breaker interrupts transformer magnetizing current only. As an alternative the breaker may be tripped without raising the electrodes so that it interrupts load current as well as magnetizing current. It has become fairly standard practice to follow the procedure of raising the electrodes before tripping the breaker, partly at least because it has been the concensus of opinion that this would result in less probability of high-voltage transients and also in reduced duty on the breaker.

The length of time required to make a complete heat of steel varies considerably depending upon the size of the furnace and the kind of steel being made. However, a typical figure may be taken as three hours. During this time according to the procedure outlined, the breaker will be closed and opened three times in order to start the heat and change taps. On the average there will be perhaps two or three additional switching operations for various reasons, such as slagging off, lengthening electrodes, and so forth. A total of six switch closings and six trippings per heat is perhaps a reasonable figure. Many installations are at present operating 24 hours per day, and with an average length of heat of three hours, this would mean approximately 40 to 50 switch operations per day. Some instal-

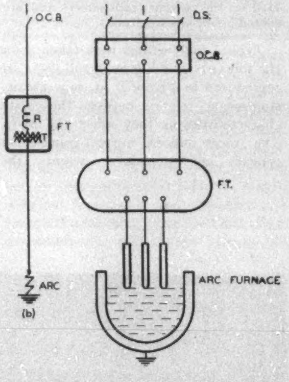

Figure 1. Typical arc-furnace circuit

DS—Disconnect switch
OCB—Oil circuit breaker
FT—Furnace transformer with self-contained reactor

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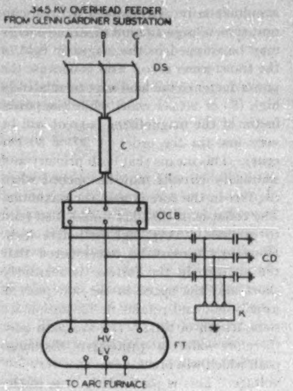

Figure 2. Furnace circuit at location A

DS—Disconnect switch
C—34.5-kv three-conductor type *H* underground cable 650 feet long
OCB—Oil circuit breaker
FT—Furnace transformer with self-contained reactor
CD—Capacitance potential divider
K—Klydonograph

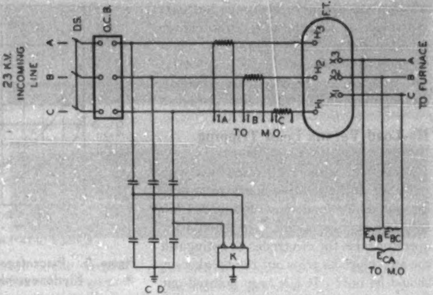

Figure 3. Circuit used for making tests at location *B* without potential transformers in the circuit. Secondary transformer voltages recorded by the magnetic oscillograph

DS—Disconnect switch
OCB—Oil circuit breaker
FT—Furnace transformer with self-contained reactor
CD—Capacitance potential divider
K—Klydonograph
MO—Magnetic oscillograph

lations have as many as 100 switch operations per day.

Possible Causes of Voltage Surges

Following the preceding discussion of furnace operation we may list the following possible causes of voltage transients in arc-furnace circuits:

(*a*). Instability of the arc.

(*b*). Raising or lowering the electrodes to interrupt or start the arc.

(*c*). Switching.

The role played by intermittent arcing in the production of high-voltage transients on transmission systems is well known. It seems logical therefore to expect that a similar phenomenon might be found in circuits in which the load itself is an arc. If this is true, voltage surges should be particularly noticeable during the melt-down period when the arc is most unstable. Furthermore, it is the authors' opinion that the systems supplying most arc furnaces are so large that they offer very low impedance to surges originating on the low-voltage side of the transformer and therefore tend to suppress the transmission of such transients to the high-voltage side of the transformer. This being the case, voltage transients caused by arc instability should be observable, principally, on the secondary side of the transformer. At one of the two installations tested, special steps which are described later were taken to detect the presence of overvoltages on the high-voltage side of the transformer during furnace operation, and it was found definitely that no appreciable overvoltages occurred. On the other installation, no special steps were taken to detect surges on the high-voltage side during furnace operation, but an examination of Klydonograph film records of regular heats indicates, although not conclusively, that overvoltages were not obtained on the primary side except when switching occurred. No tests were made on the low-voltage side of the furnace transformer.

Since it is common practice to raise the electrodes before tripping the breaker and to lower the electrodes after the breaker is closed, it is possible that the interruption and initiation of the arc by this means might result in voltage surges. At one of the installations tested by a procedure described later, it was definitely determined that overvoltages were not obtained on the high-voltage side of the transformer when the electrodes were raised or lowered to terminate or strike the arc in the furnace. This test was not made at the other installation.

It is well known that the operation of switches in electrical circuits may cause voltage surges and that the magnitude of these surges depends upon the characteristics of the circuit and of the switch. Furnace circuits are not essentially different from other power circuits, except for the fact that the load has the peculiar characteristics of an arc. It might be expected, therefore, if there is any difference between the character of switching surges obtained on furnace circuits from those obtained on other power circuits that it would be observed when switching under load. It is advisable, therefore, to consider separately the conditions which exist when switching under load and when switching no-load, and also when closing and opening the circuit.

Although closing the circuit breaker under load is probably not practical for

Table I. Comparison of Two Installations Tested

	Location A	Location B
Primary voltage	34.5 kv	24 kilovolts
Frequency	60 cycles	60 cycles
Transformer (secondary delta connected)	3,000 kva, 3 phase	3,000 kva, 3 phase
Delta to wye primary switch	Internal	Internal
Secondary voltage range with		
Primary winding in delta	220–140 volts	235–160 volts
Primary winding in wye	127–81 volts	136–93 volts
Reactor	Internal, 21 per cent	Internal, 20 per cent
Oil-circuit-breaker current rating	600 amperes	600 amperes
Oil-circuit-breaker interrupting rating	500,000 kva	500,000 kva
Oil-circuit-breaker insulation	46 kv	34.5 kv
System grounding	Petersen coil—26 miles away	Solid—4 miles away
Miles of connected high-voltage line	350	4
Underground cable	650 feet, type *H*, 3*C*, 34.5 kv	None
Lightning arresters	Oxide film	Porous block—25 kv
Distance from lightning arrester to furnace	700 feet (source side of cable)	50 feet (end of line)
Instruments used in tests	Klydonograph	Klydonograph Cathode-ray oscillograph Magnetic oscillograph
Circuit diagram showing instruments	Figure 2	Figures 3 and 4
Secondary grounding	Lamps	Lamps

standard operating procedure, a few tests were made as a matter of interest and are reported under "Test Results." As might be expected, no overvoltages resulted at the transformer high-voltage terminals although current inrushes were obtained.

No-Load Versus Load Tripping

There has been much discussion with reference to the relative merits of switching out under load or with load on the transformer. In other words, should the operator raise the electrodes to extinguish the arc before he trips out the breaker, or should he not? It has been pointed out by some that breaking transformer magnetizing current may cause a voltage surge because of the inductive kick produced by the sudden collapse of the stored magnetic field in the transformer core. However, it must be remembered that the transformer core is a closed magnetic circuit and therefore will retain permanently a residual magnetization which is usually in the order of 60 per cent of the maximum induction. Furthermore, if the current is interrupted in the breaker at normal current zero, without appreciable distortion of the current wave, there will be no inductive kick at all because both flux and current will decrease along their normal paths to their steady-state value corresponding to current zero. In most modern breakers this is usually very close to what actually happens, because such breakers do not greatly tend to force current zero. Although breaking transformer exciting current can undoubtedly produce inductive kicks of two to three times normal (some of the film records obtained show such inductive kicks) it seems unlikely that overvoltages caused by breaking magnetizing current should be a source of serious concern.

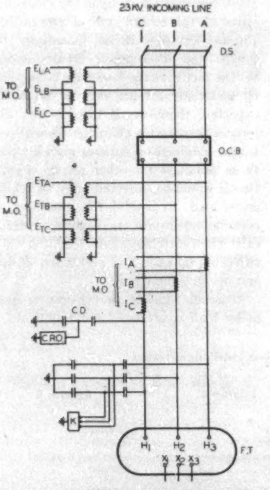

Figure 4. Circuit used in making tests at location *B* in which potential transformers were used. Primary transformer voltages recorded by the magnetic oscillograph

DS—Disconnect switch
OCB—Oil circuit breaker
FT—Furnace transformer with self-contained reactor
CD—Capacitance potential divider
K—Klydonograph
MO—Magnetic oscillograph
CRO—Cathode-ray oscillograph

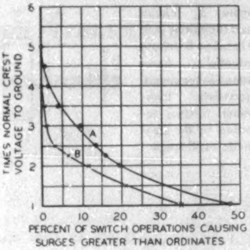

Figure 5. Percentage distribution curves of Klydonograph surge records

Curve *A*—Location *A*
Curve *B*—Location *B*

Those who have advocated no-load tripping as preferable to tripping under load point out that interrupting load current may be dangerous because if the arcs in the circuit breaker and furnace are simultaneously extinguished at load-current zero, a large amount of stored energy may be trapped in the magnetic field of the transformer core. This is because the power factor of the load may be relatively high (85 or 90 per cent) while the power factor of the magnetizing current will be very low (in the order of 20 or 30 per cent). This means that both primary and secondary circuits may be opened when the flux in the core is near its maximum. The collapse of this flux would then tend to produce a very high inductive kick. However, it must be remembered that the arc gap in the furnace is relatively short and the space in the arc path is usually hot and retains its ionization for some length of time. The arc path may therefore offer a protective discharge path which will break down at a very low voltage. This is particularly true in the latter parts of the heat. During the melt-down period, however, when the arc is still comparatively long and when the arc space cools rapidly upon extinction of the arc, the trapping of magnetic flux in the core as has been described is believed to be a possible cause of overvoltages which may produce insulation failures. In considering this it should be remembered that the voltage required to break down the arc in the furnace will be multiplied on the high-voltage side of the transformer by the turn ratio. In the tests a number of surges of several times normal (up to approximately four times normal) were obtained because of tripping under load, chiefly in the early parts of heats. On the other hand, quite a few load trippings made after the charge was melted down produced no appreciable surges.

Summing up, it is believed that tripping

Table II. Summary of Test Results

	Location A	Location B
Approximate total number of switch operations (close or trip)	1,400	1,000
Approximate total number of surges recorded on Klydonograph	650	200
Approximate number of heats recorded on Klydonograph	70	50
Total number of surges recorded on cathode-ray oscillograph	None	110
Total number of surges recorded on magnetic oscillograph	None	150
Maximum switching surge—times normal	5.2	3.8
Surges unidirectional or oscillatory	Unidirectional	Both
Polarity of Klydonograph surge figures	Positive and negative equal	Mostly positive
Surges on primary circuit caused by load	No	Probably not
Surges caused by tripping or closing breaker	Closing chiefly	Both equally
Surges caused by raising or lowering electrodes	No	No test
Effect on switching surges of		
Transformer tap position	Not important	Not important
Supply-system grounding	See Figure 6	No test
Underground cable ahead of breaker	See Figure 6	No cable
Natural frequencies of switching transients		
No-load tripping	No test	6,500 to 10,000
Load tripping	No test	5,000
No-load closing	No test	6,000 to 9,500
Load closing	No test	6,200
Percentage distribution curve of switching surges	Figure 5	Figure 5

under load in the latter parts of the heat with a short hot arc may cause lower overvoltages than tripping at no-load, but tripping under load in the early parts of the heat before melting down is completed definitely should be avoided.

Figure 7. Cathode-ray oscillogram of tripping circuit breaker under load—moving film

Figure 8. Cathode-ray oscillogram of no-load tripping operation — moving film

Circuit Data

Tests were made on two installations identified in this discussion as location *A* and location *B*. The pertinent information on the furnace circuits and the power systems supplying them is presented in tabular form in Table I. It will be noticed that the chief difference between location *A* and location *B* lies in the supply system. The furnace circuits including the transformers and circuit breakers are similar, except for the primary voltage rating.

Important differences in the supply system are:

1. The system at location *A* is 34,500 volts, while at location *B* it is 24,000 volts.
2. Location *A* has 350 miles of connected high-voltage line, while location *B* has only approximately four miles. The 350 miles of line supplying location *A* consist of a 34.5-kv loop. A spur line four miles long supplies the furnace, and there is no other load on this line. The 24-kv line supplying location *B* originates at a step-down substation four miles away. There is one other furnace on this same line, approximately 50 feet beyond the one tested, where the line terminates.
3. The 34.5-kv loop supplying location *A* is normally grounded through a Petersen coil at only one point, about 26 miles from the furnace, while the line supplying location *B* is solidly grounded at a substation four miles away.
4. Location *A* is served by a 650-foot length of three-conductor underground cable just ahead of the oil circuit breaker (see Figure 2), while there is no underground cable at all in the circuit at location *B*.
5. The only surge protection on location *A* is a bank of obsolete oxide film arresters located on the source side of the underground cable, approximately 700 feet from the furnace, while location *B* is protected with modern lightning arresters at the end of the line, approximately 50 feet beyond the test location.

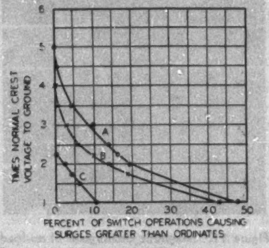

Figure 6. Comparison of Klydonograph tests at location A

Curve *A*—Normal condition with system grounded through Petersen coil

Curve *B*—System solidly grounded at three points

Curve *C*—Underground cable on source side of circuit breaker replaced by overhead line

At both installations the furnace shell, transformer tank, and electrode control have a common ground.

Test Results

The circuit used in making tests at location *A* is shown in Figure 2. The Klydonograph only was used on this installation.

Some of the data obtained at location *B* were made with the Klydonograph and magnetic oscillograph only. The circuit is shown in Figure 3. Other tests were made at location *B* in which the cathode-ray oscillograph was used on one phase as well as the Klydonograph and magnetic oscillograph. The location of the test apparatus in the circuit is shown in Figure 4. Still other tests were made with a circuit similar to Figure 4 except that the potential transformers on the line side of the circuit breaker were connected across the three poles of the breaker to record breaker voltage on the magnetic oscillograph.

A total of approximately 2400 switch operations was recorded on the Klydonograph, resulting in approximately 850 surge figures of such magnitude as to be measurable. In addition a total of approximately 120 normal heats were recorded on the Klydonograph. At location *B* 110 switch operations were recorded on cathode-ray oscillograms, and 150 switch operations were recorded on magnetic oscillograms.

The results are summarized in comparative form in Table II.

In analyzing the results the Klydonograph data only were used for quantitative results such as presented in the percentage distribution curves of Figures 5 and 6. It was not possible to obtain sufficient oscillographic data for such a statistical analysis. However, in general, the magnitudes recorded on oscillograms gave a reasonable check with those from the Klydonograph. The oscillograms were used chiefly to obtain information on the nature of the surge wave shapes, such as natural frequencies, restriking, and so forth. The only reliable information about wave shape yielded by Klydonograms is polarity, and even this must sometimes be questioned, as explained later.

An attempt was made to plan the test procedure so that answers might be obtained to as many as possible of the following questions:

1. *What is the polarity of the Klydonograph surge figures?* (The character of positive and negative surges is quite different, and the two are easily distinguished.[8]) The results obtained in connection with this question are taken up in detail later.
2. *Are overvoltages caused by the furnace load?* At location *A* a number of regular heats were attended, and the Klydonograph film was moved by hand after each switch operation and after each time that the electrodes were raised or lowered. By this means it was determined that the only overvoltages obtained occurred on switching. No such tests were made at location *B*, but an examination of the Klydonograph records leads to the tentative conclusion that here also surges were caused only by switching.
3. *Are overvoltages caused by raising and lowering the electrodes to interrupt or start the arc?* By the same procedure of moving

Figure 9. Cathode-ray oscillogram of no-load closing operation — moving film

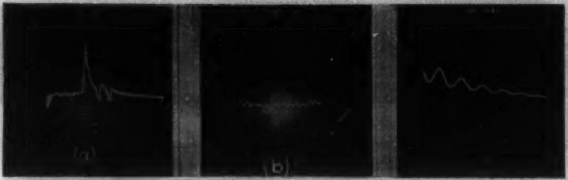

Figure 10. Cathode-ray oscillograms of no-load closing operation using single sweep of the oscillograph

(*a*). 60-cycle sweep
(*b*). 300-cycle sweep
(*c*). 900-cycle sweep

the film as previously described, it was also ascertained at location *A* that lowering and raising the electrodes did not cause voltage surges. (Again no tests were made at location *B*.) This result is not surprising since electrode motion is very slow and interruption probably occurs at normal current zero without any appreciable disturbance.

4. *Are overvoltages caused by closing the breaker on load?* Closing the breaker on load was done at location *B* only. No overvoltages were observed from this operation, although as would be expected, current surges were obtained.

5. *Are overvoltages caused by tripping the breaker underload?* Surges of several times normal were obtained on both installations by tripping underload during the early part of a heat. However as stated previously, it was found that load tripping after meltdown when the arc is short and hot did not produce appreciable overvoltages.

6. *In no-load switching are the severest surges caused by closing or by tripping?* The results are discussed under "No-Load Switching Tests."

7. *What is the effect on no-load switching surges of:*

(*a*). *Transformer tap position?* The tests on both installations showed that the transformer tap position did not have any important effect on the magnitude of switching surges.

(*b*). *Supply-system grounding?* This test was made at location *A* only, and the results are shown in Figure 6 and are discussed in detail later.

(*c*). *The effect of the underground cable at location A?* Results of this test are shown in Figure 6 and are discussed in detail later.

8. *What are the natural frequencies of the switching transients?* The natural frequencies obtained from the cathode-ray oscillograms at location *B* are shown in Table II, and typical oscillograms are shown in Figures 7 to 14, inclusive.

No-Load Switching Tests

Since the voltage surges were found to be caused mostly by switching, and since the usual procedure is to switch no-load, the bulk of the testing was devoted to accumulating data on no-load switching. An analysis of these data reveals some striking differences between the results obtained at the two different locations, and these differences are believed to be explained by the unusual combination of circuit characteristics at location *A*.

It is believed that location *A* is rather unique and is of particular interest for several reasons.

1. It is one of a very few 34,500-volt furnace-transformer installations in this country.

2. The presence of the underground cable ahead of the circuit breaker, together with the great length of connected high-voltage line and the Petersen-coil ground, is somewhat unusual.

It will be noticed in Table II that the maximum surge obtained on location *A* was considerably greater than location *B*. The difference is even more evident in Figure 5 in which curve *A* represents switching surges at location *A*, and curve *B* represents those at location *B*. The difference between typical Klydonograph films from location *A* and location *B* is striking, because of the much greater size and frequency of surge figures on films obtained at location *A*. Figure 15 is a sample Klydonogram from location *A*. Part of the difference between the severity of surges at locations *A* and *B* may possibly be explained by the better lightning protection at location *B*. (See Table I.) However, in both cases the lightning arresters are on the other side of the circuit breaker from the surge-measuring instruments. It is believed that the lightning arresters at location *B* were not operative to any great extent in reducing the maximum value of the surges because there was no tendency of the percentage distribution curve to flatten off near the upper voltage limit. (See Figure 5.)

Several other differences between the results obtained at locations *A* and *B* are of interest.

POLARITY

At location *A* all of the Klydonograph figures for switching were unidirectional in nature and approximately equally distributed between positive and negative. At location *B* nearly all of the figures appeared to be positive. The explanation for this difference is believed to be that surges at location *A* were truly unidirectional, while those at location *B* as evidenced by the supporting oscillographic films, resulted in both positive and negative voltages for each surge in nearly every case. This being the case, the positive figures would tend to cover up completely the negative figures since they are more than twice as large. Also it happens that the 60-cycle normal voltage line which appears in records where the film is clock-driven is sufficiently wide to hide completely all but the very highest voltage negative figures. As it happened at location *A*, a slight unbalance in the line-to-ground voltages caused the 60-cycle normal-voltage line to appear on only one phase, permitting all negative surge figures to be seen easily on the other two phases. Another interesting point is that in most cases at location *A* the surges appeared to be of the same polarity and the same order of magnitude on all three phases indicating that the entire winding rose above ground, while this was not at all the case at location *B*.

COMPARISON OF CLOSING AND TRIPPING SURGES

At location *A* it was surprising to find that nearly all appreciable no-load switching surges were obtained when the breaker was closed, only a very few surges of minor magnitude being obtained for

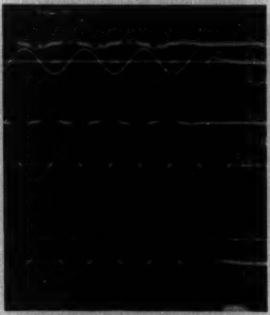

Figure 11. Magnetic oscillogram of no-load tripping recording line to neutral voltages on transformer side and supply side of the breaker—Figure 4

I_A, I_B, I_C—Line currents
E_{TA}, E_{TB}, E_{TC}—Transformer voltage of phases *A*, *B*, and *C*
E_{LA}, E_{LB}, E_{LC}—Voltages on the supply side of the breaker of phases *A*, *B*, and *C*

Figure 12. Magnetic oscillogram of no-load tripping operation recording transformer voltages and breaker voltages

I_A, I_B, I_C—Line currents

E_{TA}, E_{TB}, E_{TC}—Transformer voltages of phases *A*, *B*, and *C*

E_{BA}, E_{BB}, E_{BC}—Voltages across breaker contacts in phases *A*, *B*, and *C*

no-load tripping. At location *B* on the other hand, switching surges were approximately equally distributed between closing and tripping both in number and magnitude, with the balance slightly in favor of tripping.

The closings were distinguished from the trippings by moving the Klydonograph film by hand after each closing or tripping. This resulted in a normal 60-cycle line on the film after each closing and a blank space after each tripping. In Figure 15 all of the important surge figures occur at the start of the 60-cycle line, showing that they were produced by closing the breaker.

Effect of Supply-System Grounding

Tests were made to determine the effect of system grounding at location *A*, and the results are shown in Figure 6 by a comparison of curves *A* and *B*. There seems to be an appreciable reduction in the magnitude of switching surges with the system solidly grounded as represented by curve *B*. For this test the system was solidly grounded at three points, the nearest point being at the junction of the spur line and 34.5-kv loop four miles away.

Effect of Underground Cable at Location A

Fortunately it was also possible to make tests with the underground cable out of service and replaced by a temporary overhead line. The results are shown in curve *C* of Figure 6, and the great reduction in the magnitude of switching surges is evident. The maximum surge was reduced from five to approximately two and one half times normal.

It is believed that the great difference between results obtained at locations *A* and *B* are due chiefly to three factors, namely:

1. Better surge protection at location *B*.
2. Presence of the capacitance of the underground cable and great length of connected line at location *A*.
3. The difference in grounding of the two supply systems.

Of these, based on the result of the tests, it is believed that the presence of the cable is most important.

The great effect of the cable might be explained by the cumulative effect which occurs in capacitive circuits when restriking occurs and which is absent in inductive circuits. The effect of the system grounding may be partly because of more rapid draining of charge from the cable with the solidly grounded condition.

Typical Oscillograms

Figures 7 to 9 are cathode-ray oscillograms made with a moving film, and Figure 10 is a cathode-ray oscillogram in which the single-sweep arrangement on the oscillograph was used. Figures 11 to 14 are magnetic oscillograms.

Figure 7 is a record of a load tripping operation in which restriking occurred twice, and the final interruption resulted in an inductive kick to approximately twice normal.

Figure 8 is a typical no-load tripping record. It will be observed that arcing lasted for approximately one cycle, and this is typical as evidenced also by the magnetic oscillograms. The large lower frequency oscillations are restriking at successive current zeros. It was not possible to resolve the very high-frequency oscillations between these points on any of the film records obtained.

Figure 9 is a typical no-load closing operation in which the shape of the long-time transient is attributed to the fact that the circuit was made at different times on two phases.

Figure 10*a* is a no-load closing in which a 300-cycle sweep was used. A similar closing is shown in 10*b* at a 60-cycle sweep, and at 10*c* in a 900-cycle sweep. The frequency is approximately 6,500 cycles. In 10*b* the effect of making at different instants of time on different poles of the breaker is clearly shown. The maximum voltage obtained, however, is only approximately normal crest voltage.

Natural frequency oscillations obtained on no-load tripping are clearly seen in Figures 11 and 12, and the absence of these oscillations is striking in the load trip magnetic oscillograms in Figures 13

Figure 13. Magnetic oscillogram of tripping underload recording transformer voltages and line to neutral voltages on the supply side of the breaker—Figure 4

I_A, I_B, I_C—Line currents

E_{TA}, E_{TB}, E_{TC}—Transformer voltage of phases *A*, *B*, and *C*

E_{LA}, E_{LB}, E_{LC}—Voltages on the supply side of the breaker of phases *A*, *B*, and *C*

Figure 14. Magnetic oscillogram of tripping under load recording transformer voltages and breaker voltages

I_A, I_B, I_C—Line currents

E_{TA}, E_{TB}, E_{TC}—Transformer voltages of phases *A*, *B*, and *C*

E_{BA}, E_{BB}, E_{BC}—Voltages across breaker contacts in phases *A*, *B*, and *C* and 14. However, it will be observed that in the load trip records, arcing is extended over nearly two cycles while the arcing period is approximately one cycle in the case of no-load tripping.

Most of the film records obtained for both tripping and closing show three or more restrikes and making or breaking of the circuit at different times on the different phases.

Conclusions

1. Voltage surges occurring on the high-voltage terminals of arc-furnace transformers are caused largely by switching.

2. Tripping out under load can cause high overvoltages chiefly in the early parts of the heat when the arc space is long and cools rapidly. Load tripping, during the later parts of the heat may result in lower voltage transients than no-load tripping but in extended arcing. As a general practice it is better to raise the electrodes before tripping the breaker.

3. Since no-load switching is general practice, the problem of switching surges on furnace circuits is not essentially different from that of other power circuits, except that switching is more frequent and the circuit constants may be quite different.

4. Because the problem of overvoltages, at least on the high-voltage side, reduces to one of switching surges, the characteristics of the supply system, as well as of the furnace circuit and the switch, will determine the magnitude of voltage surges obtained. In particular the grounding of the power system is important.

5. The presence of a length of underground cable on the source side of the circuit breaker used for switching may be the cause of excessive overvoltages.

6. It is realized that the data presented here are far from complete or conclusive, and it is believed that further study of the problem of voltage transients in arc furnaces is warranted, particularly with a view to obtaining measurements on the low-voltage side of the transformer.

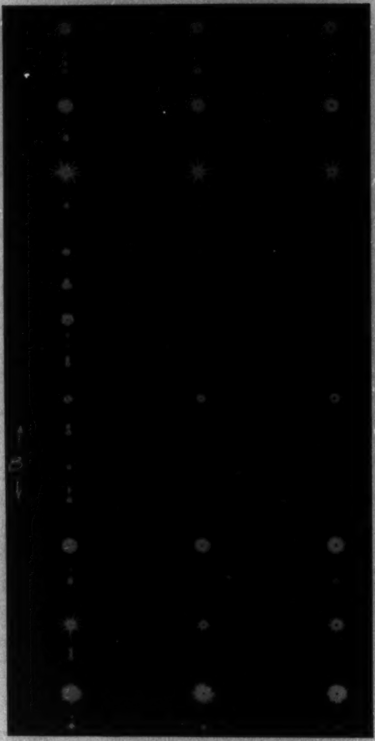

Figure 15. Typical section of Klydonograph film obtained in no-load switching tests at location *A*

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Aircraft Contactors

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THE growing complexity of the electrical systems in use on modern aircraft has given rise to need for a complete line of magnetically operated switches or relays suited to conditions encountered in this type of service. Relays may be classified according to use as control relays and power relays, or in the terminology of the industrial-control engineer, relays and contactors. This paper will be confined to a discussion of power relays or contactors. Most aircraft in use at the present time have 24-volt d-c systems; consequently only low-voltage d-c contactors will be considered.

Commercial contactors of the required ratings are larger and heavier than practical for aircraft partially because

1. Conditions of use do not limit these factors so rigidly.
2. The longer life expected from them.
3. The greater insulation distances required for higher voltages.

A background of experience obtained from commercial contactors can contribute greatly to the design of aircraft switches, but numerous special conditions must also be considered. Changes in barometric pressure from 30 inches of mercury at sea level to about 5.54 inches at 40,000 feet, and temperature ranges from −50 degrees Fahrenheit to +200 degrees Fahrenheit are encountered. Vibration and shock may be met, and the switches must be designed to withstand them. Vibration tests consisting of simple harmonic motion with an amplitude of 0.03 inch (0.06 inch total displacement) and a frequency varied from 10 to 55 cycles per second have been made part of the specifications. Switches must operate in any position. Coil temperature rise must be limited to 70 degrees centigrade above a 25 degrees centigrade ambient.

Some aircraft accessories using contactors are listed in Table I. Loads are, therefore, resistance, inductance, lamps, and motors. Contactors are used on loads as low as 25 amperes and may carry 200 amperes or more. Circuit requirements may be for single-pole single-throw; single-pole double-throw; double-pole single-throw; and double-pole double-throw contactors. When used for the control of reversing motors double-throw contactors have two coils each operating a set of contacts controlling direction and mechanically interlocked to prevent simultaneous operation.

Design of a line of contactors naturally divides itself into consideration of several phases:

1. Contacts.
2. Magnet.
3. Arrangement, mounting, and special features.

These phases will be considered separately and in the order listed.

Contacts

The heart of a contactor is its contacts. The contact material must be one which does not collect a film which offers high resistance to the passage of current either when the contacts are open, while closed and heated by the current flow, or under arcing. They must therefore be made of a material which does not form nonconducting oxides and which, because of the low voltage, has a low resistance. Silver meets these requirements, but, because of transfer and welding problems, additions to the silver, as for example cadmium oxide without alloying, have been found to give superior properties. Various other additions have been proposed including cadmium sulphide, lead sulphide, tungsten, and molybdenum. The contacts must be securely fastened to the contact plates to obtain a permanent low resistance bond. Brazing is a satisfactory method. A superior product may be obtained, however, by a process in which the constituents are granulated or powdered, mixed, and molded under high pressure direct to the backing material. A grid of serrations on the backing plate interlocks with the molded material to form a secure anchor and a permanently low resistance joint. The density of the contact material may be controlled by the pressure applied in molding them. Contacts formed by this process have several important advantages.

1. A low resistance contact having the properties of silver.
2. A contact having in it other metallic materials to obtain less transfer and to reduce welding.
3. A contact with a controlled density affecting its bounce characteristics and permitting welds to be broken.
4. A contact extremely uniform and easy to manufacture.

Contacts opening and closing d-c circuits are subject to several types of failure. Transfer of material from one contact to the other in the electric arc is a well-known phenomena which has caused considerable trouble to the designers of d-c switches using contacts containing silver. Transfer occurs at any time an arc exists between the contact faces and is a function of the current in the arc. It is apparent therefore that transfer may occur during the time that the contacts are arcing when they close and also during the time the contacts are opening before the arc is extinguished. It is important that arcing time be reduced to a minimum to limit the amount of transfer. Numerous readings taken with an instrument designed for measuring arcing time[1] show that the arcing time on closure is influenced by the initial contact pressure. Figure 1*A* is a curve showing the rela-

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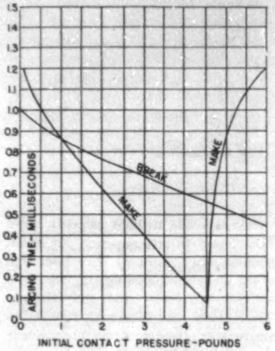


Figure 1A. Curves showing arcing time for various contact pressures

tion of arcing time to initial contact pressure for a typical contactor and shows that as the contact pressure is increased the arcing time on closure is reduced until a critical value is reached. Below this value the bounce occurs when the contact plate itself rebounds while above this value the arcing time increases because the entire moving assembly of contact and armature bounces. The weight of the moving contact plate and the armature must therefore be correlated with the initial contact pressure to obtain the most satisfactory values. Figure 1*B* shows the variation in arcing time of a contactor as the coil voltage is changed. Here again a point is reached where the entire armature and contact assembly bounces as the voltage is decreased.

The design should be such as to prevent "armature bounce" from occurring in the range of voltages over which the contactor is to operate. Reference to the curve shows that the maximum contact pressure is limited by satisfactory operation on low voltage (18-volts hot coil). A minimum value which will keep the arcing time on the lower part of the curve should be used to keep transfer to a minimum.

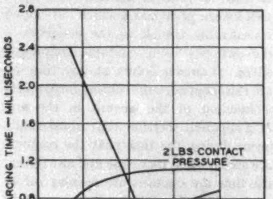


Figure 1B. Curves showing arcing time for various coil voltages

The arc formed when contacts bounce on closing may, if it is of sufficient duration or intensity, fuse the contact material and cause rapid wear. For contacts of the character stated, the molten metal is flattened and in some cases splashed when the contacts reclose. On 30 volts this begins to occur with currents of about 100 amperes. The high points are, under these conditions, leveled off so that the effects of transfer are nullified. Contacts operated above 100 amperes remain flat and have a fine granulated or mottled appearance.

Figure 3 shows typical contacts before and after being used. A typical case of transfer may be seen in Figure 3*c* while a contact which has remained level is shown in Figure 3*d*.

If the current is sufficiently high the degree of fusing may eventually cause welding. Factors which determine the current at which welding takes place include

1. The materials from which the contacts are made.
2. The thermal capacity of the contacts.
3. The degree of flattening of projections thrown up on the contact faces (controlled by the initial contact pressure).

Cadmium oxide mixed into the contacts retards the tendency toward welding and transfer and is therefore beneficial. Contacts of high thermal capacity and conductivity take the heat away from the point of arcing and reduce the amount of molten material formed. The blow which tends to flatten projections is dependent on the initial contact pressure and the velocity of closing, and it is therefore desirable to keep these values as high as possible. On opening, if the density of the contacts is low, the welds will be broken when the contacts part, particularly if a blow or shock is imparted to them. The force necessary to break the weld is determined to some degree by the materials used for the contacts and the form and size of the particles used in making them.

Contacts should have sufficient wear allowance to compensate for the wear obtained during their life and to provide for the unavoidable variations in manufacturing. The actual wear during the life required for aircraft contactors indicates that a wear allowance of 0.03 inch is sufficient to provide ample safety factor. They should have an initial contact pressure high enough to prevent welding on the highest currents which may have to be made and the pull of the magnet designed to permit this pressure to be used without obtaining armature bounce.

The arc gap required is determined by the voltage for which the contactor is to be used. A value of 0.055 inch has been selected as conservative for 24-volt service.

Magnet

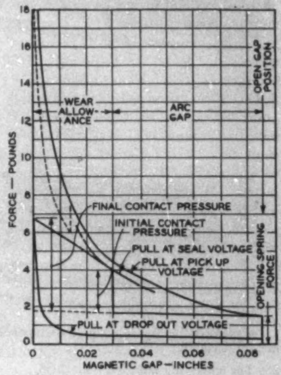


Figure 2. Contactor force analysis

There are several forms in which the operating magnet for contactors may be built. Each has characteristics which determine its suitability for a particular use. All have many common properties and a general analysis of their operation may be made. Figure 2 shows a force diagram for a typical contactor. The curved voltage lines represent the pull of the magnet at various plunger positions. The forces exerted by the springs are shown by the straight lines. For convenience both the magnet-pull curves and the spring forces are plotted as if they were scalar quantities of the same direction. It should be remembered, however, that their direction of application is such as to oppose each other. At the open-gap position the plunger is kept from moving into its sealed position by the balancing or opening spring. The force required is determined by the weight of the moving parts and the acceleration forces to which the contactor may be subjected. A minimum value of $F = (A/G) \times W$ where A is the maximum acceleration to be encountered, G is the acceleration of gravity, and W is the weight of the moving parts. For balanced structures in which accelerative forces are cancelled the force is determined by the energy required to open the contacts with sufficient velocity to insure breaking the arc quickly. As the plunger is moved toward its sealed position the opening spring is compressed so that this force increases. It is desirable that the rate of increase in this force be kept as low as possible, for an increase in its value increases the magnet pull required for a given initial contact pressure. The contacts are arranged so that they close before the sealed position of the armature is reached. The remaining portion of the stroke is wear allowance. The moving contact are fastened to the armature by means of a spring in such a manner that a force is required to separate them from it. This force is the initial contact pressure and the performance of the contacts depends considerably on its value. Reference to the force diagram shows that the upper limit of the initial contact pressure fixes the shape of the magnet-pull curve, for it is necessary if the contactor is to close smoothly that the curve passing through the initial contact pressure point be a lower voltage than the pickup voltage which is the voltage at which the magnet pull is equal to the opening spring force at the open-gap position. The shape of these curves may be altered to some extent by the shape of the pole faces. The load placed on the power system when the contacts close may be high enough to cause an appreciable drop in system voltage. For instance, the current inrush on some motors used in military aircraft has been found to be as high as 2,000 amperes. The resulting drop in voltage at the contactor coil terminals may reduce the pull to an extent that the contact will not seal completely, resulting in additional arcing of the contacts which may cause them to weld. Voltages as low as eight volts have been obtained on 24-volt systems when heavy currents were drawn. There is the further possibility, if the voltage falls low enough, that the armature may drop back even though the armature reached its sealed position. In this case the voltage drop in the arc will limit the current until the motor accelerates and the system voltage builds up enough to reclose the contactor. Operation under these conditions will burn the contacts severely and will probably result in welding. Dropout occurs when the coil voltage is decreased until the sealed pull is equal to the force from the springs tending to open the contacts. The magnet pull should decrease as the magnetic gap increases in a way so that it is less at the point where the contacts just touch than the force exerted by the opening spring to avoid hesitancy in dropping out. In other words, the curve passing through the point of zero wear allowance must be of a higher voltage than that of the drop-out curve.

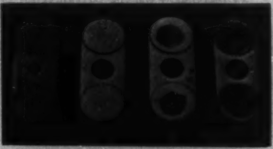


Figure 3. Contact plates

- *A.* Backing plate showing serrations
- *B.* Finished contact plate
- *C.* Contact plate showing transfer
- *D.* Contact plate after 50,000 operations at rated current

The coil must have the required ampere turns to produce the necessary pull at the lowest voltage on which it is to operate. The minimum open-circuit voltage on 24-volt systems has been set at 18 volts, and therefore the pickup voltage must not exceed this value when the coil has attained its ultimate temperature. Since the temperature rise of the coil is limited to 70 degrees centigrade above a 25 degrees centigrade ambient, this final temperature is 95 degrees centigrade or the contactor must pick up on 14.2 volts when the coil is at 25 degrees centigrade if it is to operate on 18 volts with a hot coil.

Table I. Aircraft Accessories Using Contactors

	Load
Battery disconnect	General
Engine starter	Motors
Engine synchronizer	Motors
Propeller pitch control	Motors
Wheel retraction	Motors
Flap control	Motors
Lighting	Lamps
Fuel transfer	Motors
Heating	Heaters
Gun firing	Solenoids
Bomb release	Solenoids
Turret operation	Motors

The maximum voltage encountered on 24-volt systems is 28.5 volts. The coil must not exceed its allowed temperature rise when this voltage is applied. These factors fix the resistance of the coil and its radiating area.

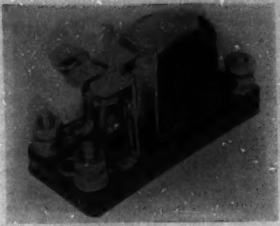


Figure 4. 25-ampere balanced-armature aircraft contactor

Contractors which are not energized for sufficiently long periods to attain their ultimate temperature may have coils designed for part-time duty only. Advantage may be taken of this fact to reduce the size of coils used on intermittent-duty contactors or greater pull may be obtained from coils the same size as continuous-duty coils where special conditions must be met.

An intermittent-duty coil may be protected by the use of a resistor to make it suitable for continuous duty. The resistor is inserted in series with the coil after the main contacts have been operated. A set of auxiliary contacts are used to insert the resistor, which may be additional turns on the coil itself. The auxiliary contacts must be arranged to open after the main contacts close and the resistance value and the shape of the pole faces should be selected to keep the magnet pull greater at all points than the spring force to avoid chattering or telegraphing. See the dotted curve in Figure 3. While this type of construction permits the use of a smaller and lighter coil, the addition of the auxiliary contacts and the additional leads on the coil may completely offset the advantages of this construction. Greater difficulties in adjustment because of the accuracy required on the small switch are to be expected.

The pivoted armature type of magnet consisting of essentially a *U* having the coil mounted on one leg and using the other leg as a return magnetic path and also serving as a support for the armature pivot is a well-known and widely accepted form used a great deal in industrial contactors. The armature lever may be extended in both directions from its pivot and balanced, thereby making it more or less free from shock and the effects of vibration or acceleration. The contacts may be placed at either end of the lever, depending on whether they are to be closed or open when the magnet is energized. Coils are readily replaceable with this type of magnet. The travel of the armature lever in the closed direction is limited by the sealing of the armature face against the core face. In order to insure a reliable contact throughout the life of the contactor and to compensate

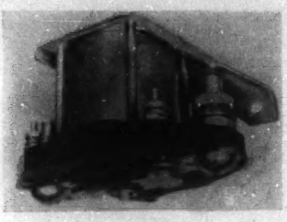


Figure 5. 200-ampere solenoid-type aircraft contactor

for wear of the contacts the contact faces are made to touch before the armature seals, the additional travel being taken up by providing for relative movement between the contacts and the armature lever, either using a spring for this purpose or depending on the flexing of the parts themselves. The latter method has the disadvantage, however, of requiring periodic adjustment of the contact position to compensate for contact wear, thereby causing a very rapid decrease in contact pressure unless the parts are made sufficiently flexible to give the same action as a separate spring. This is a construction frequently used in control relays where the amount of power handled and the wear is small. The pivoted-armature-lever construction can be made to cause a rolling or sliding action between the faces of the contacts which is desirable if the contact material is such as to collect a nonconductive film but which has severe disadvantages for some types of material.

Figure 4 is an illustration of a balanced-armature type of contactor rated at 25 amperes.

The ironclad solenoid type of magnet may also be used. The armature in this case is a plunger moving in the center of the coil and sealing against a stationary core member. The coil is enclosed in a shell which forms the return magnetic path and also serves as protection. The stationary contacts are mounted at one end of the coil and the moving contacts travel in a straight line to engage them.

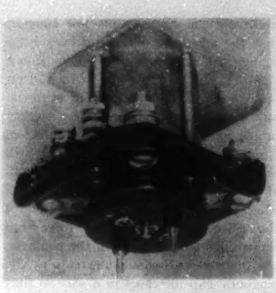


Figure 6. 100-ampere two-pole single-throw aircraft contactor

For the type of contacts being used this is an ideal arrangement because sliding or rolling on contacts of the character under discussion is not desirable. The face of the plunger in this case is conical in form to produce the desired pull curve.

A contactor of the solenoid type rated at 200 amperes is shown in Figure 5.

Arrangement Mounting and Special Features

In the design of the line of solenoid operated switches the coil is enclosed by the shell for protection and the assembly fastened together to make it a readily replaceable unit. The mounting bracket forms part of the magnetic path to conserve material and to reduce the weight. Since it is in intimate thermal contact with the coil shell and the central core, the radiation from the bracket aids in the dissipation of the heat produced in the coil. The stationary contacts are mounted on a bakelite terminal board placed on the opposite end of the coil from the mounting bracket. Iron pole pieces under the contacts serve to concentrate the magnetic field across the contact faces to obtain a blowout action. The plunger is arranged to impart a blow to the contact when the coil is de-energized by having the force from both the contact spring and the opening spring applied to the plunger in the closed position. When the coil is de-energized, the plunger moves a distance equal to the wear allowance before striking the contact plate, thereby tending to break any weld that should be found.

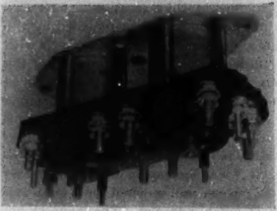


Figure 7. 50-ampere single-pole double-throw aircraft contactor

Double-pole single-throw contactors are similar to the single-pole contactors, except that a second set of contacts is provided. Figure 6 shows the 100-ampere two-pole contactor.

The reversing contactor shown in Figure 7 consists of two single-pole or double-pole contactors mounted on a common bracket and having a common terminal board. An interlocking bar to prevent simultaneous operation permits either contactor to be operated independently.

Conclusion

Contactors designed particularly for use on aircraft may be smaller and lighter than those having comparable current ratings used commercially. In order to obtain the most satisfactory design careful consideration must be given to the basic requirements and to the conditions of operation. Analysis of the functions of the component parts makes it possible to design them to obtain contactors having maximum flexibility in application, minimum maintenance for long and dependable operation, and smallest size.

Reference

1. AN INSTRUMENT FOR THE DETERMINATION OF CONTACT MAKING AND BREAKING TIME, Walther Richter, William H. Elliot. AIEE TRANSACTIONS, volume 62, 1943, January section, pages 14–16.

Interim Report on Application and Operation of Out-of-Step Protection

AIEE RELAY SUBCOMMITTEE

Preface: The present war emergency requires that the maximum use be made of existing equipment and systems and that a minimum of critical material be used for new equipment.

This publication and other guides and reports in this series have been prepared for the information of users during the war emergency. Upon termination of the war emergency they will be reconsidered by the Standards committee and the committees which prepared them, and will be approved, revised for normal use, or rescinded.

This procedure is being followed in preference to the preparation of special emergency standards which might involve redesigning and drastic changes in manufacturing practices. These guides will accomplish the maximum conservation of critical materials, since they provide for the maximum use of existing equipment and systems, as well as new equipment without changing the fundamental basis on which the present standards have been prepared.

Synopsis: Out-of-step protection of synchronous machines and system interconnections is now demanding serious consideration, first because the heavy loading of machines and transmission lines has introduced many stability problems and secondly, because outages resulting from out-of-step conditions may interfere seriously with the war effort. The relay subcommittee has, therefore, considered it timely to prepare a report on the subject and has appointed a working group for that purpose.

The report describes briefly out-of-step phenomena, the methods of protection that are available, and a cross section of the practices and requirements of representative utility systems.

Out-of-Step Phenomena

It is well recognized that when a synchronous machine pulls out-of-step with another synchronous machine or group of machines, a violent disturbance is set up which may damage the machines and cause undesirable operation of relays used to protect against overload or short circuit. Detailed explanations of the actual phenomena have been published.[5–9] It is believed, however, that a brief review of the behavior of the current and voltage during an out-of-step condition may enlighten the discussion on the application of devices and methods for protecting machines, for blocking undesirable tripping of fault protective relays, and for opening interconnections at selected points during out-of-step conditions.

A synchronous machine can be represented by an equivalent reactance and an internal voltage. For steady-state conditions, this equivalent reactance is equal to the synchronous reactance modified in accordance with the machine excitation. For sudden changes in armature current, such as those that occur during an out-of-step condition, the equivalent reactance is generally more nearly represented by the transient reactance of the machine.

When two synchronous machines are paralleled, the current interchange between them is determined by the vector difference of their internal voltages applied to the interconnecting impedance, which includes the reactance of the machines as well as any impedance between machines. Neglecting charging current and any tapped-off load current, the current is the same at all points in the interconnection and lags the difference voltage by the impedance angle of the interconnection. The voltage at any point is equal to the internal voltage of the leading machine minus the impedance drop to the point. See Figure 1. With this basic understanding it is possible to determine either analytically or graphically the magnitudes and relative phase angles of the current and voltage at any point in an interconnection for any magnitude and angular separation of internal voltages.

When the machines pull out of step, the internal voltages swing apart through an angle of 360 electrical degrees for each complete slip cycle. As this angle of separation progresses, it causes a difference voltage which starts at a minimum for the inphase position, builds up to a maximum at 180 degrees out-of-phase, and decreases again to a minimum at 360 degrees. Therefore, there will be pulsations in current and voltage at every point in the interconnection. The current will pulsate in magnitude similarly to the difference voltage; that is, it will be a minimum for the inphase position and a maximum at 180 degrees out-of-phase. The voltage at any point will also pulsate in magnitude, but it will be a maximum for the inphase position and a minimum at or near 180 degrees out-of-phase. The minimum value will vary throughout the interconnection, reaching zero at one point which, for equal internal voltages, would be the electrical center of the interconnection. If the internal voltages are unequal, the point of zero minimum voltage will be proportionally nearer the end of lowest internal voltage.

The curves of Figure 1 show how the interchange current, the voltage, and the angle between the two vary during a complete slip cycle at two points in a typical interconnection. Equal internal voltages and no intermediate load are assumed. The points shown are the high-voltage busses at each end of an interconnection consisting of a synchronous machine and transformer at each end with a 60-degree impedance line between. From these curves, it is possible to predict the performance of voltage-, current-, watt-, or distance-type relay elements located at each end of the line, taking into account the relay connections and the period of a slip cycle.

Protection of Individual Synchronous Machines

Individual synchronous machines can be protected against out-of-step operation by devices that recognize a condition which may cause pull-out unless corrected or by devices that detect an actual out-of-step condition. The more complete protection would include devices of both types. In general, the degree of protection warranted depends on whether the station is automatic or attended, whether the machines are generators or motors, and upon the importance of the machine.

Before Loss of Synchronism

A synchronous machine may pull out of step because of any one or a combination of the following conditions:

1. Load in excess of pull-out with normal excitation.

Paper 43-119, recommended by the AIEE committee on protective devices for presentation at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943. Manuscript submitted April 22, 1943; made available for printing May 19, 1943.

Personnel of the working group on out-of-step protection of the relay subcommittee: H. R. Vaughan, *sponsor;* C. R. Mason, G. Steeb, J. H. Vivian, E. E. George, E. W. Knapp, J. T. Logan, J. J. Samson, E. C. Sawyer.

This interim report was prepared by the working group on out-of-step protection of the relay subcommittee of the AIEE committee on protective devices for the purpose of making essential information immediately available to war industries, thus furthering the conservation of valuable material for the war emergency. It is educational and in no way mandatory. It is not intended as a "Standard," and has not been approved formally by the Standards committee nor the board of directors.

Acknowledgment is given to L. N. Crichton (deceased) and to C. R. Mason for the preliminary report that each prepared while he was serving as sponsor of the group.

Table I. Summary of Replies to Questionnaire

Operating Company	Question Number from Questionnaire										Comments
	1	2	3	4	5	6	7	8	9	10	
A	Yes	Field failure and reverse current relays on field feeders—record good	Trip	None. Operators instructed to trip if out-of-step exists for extended time	Alarm	Yes	Yes	Time delay on carrier tripping	c	Incorporated as part of relay scheme	Stability problem has been reduced by carrier relaying, set to give about 15 cycles clearing time. Carrier relaying has adjustable time delay to allow electrical center of out-of-step condition to shift through line section. Some difficulty in trying to set for selective tripping
B	Yes (Frequency changers)	Overload relays used to disconnect frequency changers in case of pull-out	Trip	None	Trip	No	No	None	a	Keep relaying schemes separate	Steam stations rigidly interconnected
C	Yes	Detection of condition left to operator	Trip	Operator must identify condition	Trip	No	Yes (on swing)	None	a	—	Closely coupled system. No cases of actual instability in 15 years. Severe load swings with interconnected system have caused line relays to trip
D	Yes	None	Alarm	None	Alarm	Yes	Yes	Carrier relaying with out-of-step blocking	c	Separate	Blocking relays available with carrier relays but not necessary where located at present time
E	Yes	Some generators equipped with overload relays with long time setting	Alarm	Same as (2)	Alarm	Yes	Yes	Carrier relaying with blocking	b	—	In one case of loss-of-field, overload relays tripped generator. In another case, generator slipped poles for a minute or two without relays tripping and pulled back in step when excitation was gradually restored
F	Yes	None	Trip	None	Trip	Yes	Yes	None now. Carrier relays with blocking being installed	c	No experience	Hope to arrange out-of-step blocking so that system will separate at point where it can be conveniently resynchronized
G	Yes	None at present	Alarm	None at present	Trip	Yes	No	None	c	Separate	Loss of or underexcitation considered serious subject. Different machines behave differently. Protection should probably be made to fit characteristics of machine
H	Yes	Generators none. Synchronous condensers have overload protection	Trip	None	Trip	Yes	Yes	None	c	Incorporate in line relays if possible	In some instances might be desirable to block tripping for one or more slip cycles and then permit tripping if synchronism is not regained. Number of slip cycles of blocking should be adjustable. Relays should be blocked from tripping caused by power swings on electrically long lines
I	Yes	None	Trip	None	Trip	Yes	Yes	None at present		Separate	Numerous cases of pull-out between steam plant and small hydroelectric plant and with neighboring system interconnected through long tie. Relays that normally operate during out-of-step split system at neutral load point without serious inconvenience
J	Yes	None on generators. Overcurrent protection on large synchronous motors	Alarm	None except overcurrent relays on large sychronous motors	Alarm	Yes	Yes	At tie points between load areas, line relays are induction type with long time reset to act as notching relays during out-of-step	c	Separate if possible and practicable	Line relays not blocked during out-of-step, but settings are graded to favor tripping at preselected points. System being essentially metropolitan with high load densities is relatively stable
K	Yes	None. Operators instructed to recognize condition	Trip	None	Trip	Yes	Yes	None	c	Incorporate in line relays where possible	Most cases of machine pull-out caused by underexcitation. Line relays always operate at one or more locations in case of system instability. Neglecting economics of installation, would prefer to block all line relays subject to out-of-step tripping except at selected locations where tripping causes the least loss of load and permits quickest restoration of ties
L	Yes	One machine has instantaneous overcurrent relay set above load current to cut out portion of field resistance	Alarm	None	Trip	Yes	Yes	None	c	No opinion	Recent extended interconnections expected to add to out-of-step problems
M	No (Large generators)	None except to trip large generators off line in case of loss of excitation	Trip	None	Trip	Yes	Yes	Blocking and selective tripping scheme which operates with line impedance relays without carrier current satisfactory	c	In some cases desirable to have blocking devices separate. Most desirable line-relay settings may not be best for blocking devices	Operating record for out-of-step blocking scheme for three years. 13 correct desirable, 6 undesirable but correct for the conditions existing, 3 incorrect

N	Yes	None. Unusual operation of voltage regulator gives audible signal that trouble exists	Trip	None except voltage regulator as in (2)	Trip	One case	Yes	None	a	Incorporate in line relays if it does not unduly complicate the protective relays	Operators are educated by discussions, illustrated by moving pictures of meter action during actual out-of-step, to recognize machine causing trouble. This system is relatively compact so that instability is not probable if faults are cleared reasonably fast
O	Yes	On small semiautomatic station use current relay in series with field of alternator to shut down machine after time delay in case field current drops to zero. Not used on major generators	Alarm	None	Trip	Yes	Yes	None	b	Incorporate in line relays	A reliable scheme of positive detection that a particular machine is out-of-step would be desirable
P	None lately	Transient load anticipator on one machine only	Alarm	None	Alarm	Yes	No	Blocking in conjunction with carrier where carrier relaying is used. Record is good	c	No experience except in conjunction with carrier	System is operating near stability limit because of heavily loaded generators at high power factor and highly fluctuating load

2. A sudden change of large magnitude in load.

3. Drop in applied voltage (motor).

4. Abnormally low or complete loss of excitation.

Complete protection against probable loss of synchronism involves devices that will detect any one of the aforementioned conditions.

LOAD IN EXCESS OF PULL-OUT WITH NORMAL EXCITATION

In general, the steady-state pull-out torque of a synchronous machine with normal excitation involves currents in excess of the thermal capacity of the machine; so devices provided for overload protection will also protect against steady-state pull-out caused by overload. Such devices are usually thermal- or overcurrent-type relays set either to shut the machine down or to sound an alarm. Where the overload device does not provide protection against steady-state pull-out with minimum operating excitation, such protection may be provided by a kilowatt relay set below the minimum pull-out load or by a load limiting device on the prime mover.

SUDDEN CHANGE IN LOAD

Sudden load changes on generators are usually caused by system faults, loss of large blocks of power, or by tripping off of other generating equipment. When the load change exceeds the transient stability of the machine, the machine will usually pull out of step so quickly that an overload device for detecting probable loss of synchronism is of little value. Automatic voltage regulators and exciters with high speed of response are used as corrective devices. Also in operation are load anticipators for steam turbine generators, which respond to sudden changes in load but which are not affected by gradual changes. The normal function of this device is to initiate a change in steam flow to the prime mover corresponding to a sudden change in load without interfering with the normal functioning of the governor for gradual changes in load.

An inertia relay[1] which responds to rate of frequency change has been developed primarily to expedite immediate circuit reclosure. Two out-of-step protective schemes are being installed which make use of this relay to detect approaching instability through the rate in frequency change as reflected in voltage phase shift. A watt relay is employed to block the inertia relay for system swings resulting from high-speed clearing of faults which do not cause loss of synchronism.

DROP IN APPLIED VOLTAGE

The pull-out torque of a synchronous motor varies almost directly as the applied voltage. A substantial drop in voltage of more than a few cycles duration is therefore quite likely to cause loss in synchronism. Synchronous motors are usually provided with undervoltage protection to prevent continued operation at reduced voltage or reapplication of improper voltage after shutdown. However, this undervoltage protection should have time delay to prevent unnecessary trip-outs on momentary voltage dips and therefore should not be depended on to anticipate an out-of-step condition.

LOSS OF EXCITATION

The field excitation of a synchronous machine has an important bearing on its pull-out torque. Figure 2 shows how the steady-state pull-out between a typical turbine generator and power system varies with excitation. Protection against field failure, or inadequate excitation for a given generator output, is desirable from the standpoint of pull-out if it does not otherwise detract from operating reliability.

The American Standards Association Standards for Automatic Stations recommends loss-of-field protection for all types of machines having fields. This is usually provided by an undercurrent relay in the field circuit to trip and lock out a machine that has lost its excitation. If the station is attended, an alarm may be sounded instead. The undercurrent relay should have sufficient time delay to ride through transient conditions of low field current immediately following external short circuits. Either the drop-out current of the relay must be low enough to allow generation at minimum excitation requirements, or its contact circuit must be disconnected during this operating condition. The relay may also be interlocked with the generator or line breaker so that it is operative only when the machine is on the line.

The main function of an undercurrent relay in the field circuit is to detect loss of field because of open circuits in the field or a failure in some part of the excitation system. It does not provide positive protection against inadequate excitation for a given generator output. Furthermore, it would not always be fast enough, if time delay is intentionally provided, to disconnect a machine with inadequate excitation before synchronism is lost.

Some companies have rigid operating instructions regarding the minimum excitation current permitted for a given generator output. This quantity has generally been under the manual supervision of an operator, but it could be supervised automatically by balancing the kilowatt load against the d-c field strength. Automatic voltage regulators can be compensated to assure adequate excitation, or for generators they could be interlocked with the prime mover throttle so that the generator could not operate with a low field at full throttle. They could also be provided with a device to limit regulator action to a predetermined minimum field current and to indicate when this excitation existed.

When a generator loses field, it draws excitation from the system. Reactive kilovolt-ampere flow into the generator can therefore be used as an indication of inadequate excitation. A more positive scheme is to use reactive current, since the reactive kilovolt-amperes may be held down because of a severe drop in voltage. There are several installations[2] of this type. Reactive current is measured by means of a polyphase reactive kilovolt-ampere relay, with a voltage regulator to maintain a constant voltage on the relay. The reactive-current relay is interlocked with an undervoltage relay in the bus to prevent tripping unless the bus voltage is low enough to make operation of the generator unsafe. The combination is sensitive to the effect of a serious reduction in excitation, regardless of how it is caused, and removes a machine from service quickly enough to minimize the effects of field failure on the rest of the system.

After Loss of Synchronism

A relay operating on current alone, voltage alone, or directional current alone cannot be depended upon to discriminate between an out-of-step condition and a system disturbance. An overcurrent relay with pick-up above maximum load current and time delay greater than maximum fault clearing time may not close its contacts during an out-of-step condition, particularly if the excitation of the machine is low. If the relay pick-up and time delay are set low enough to operate for an out-of-step condition, the relay is quite likely to operate during the period of hunting following a system disturbance. A directional overcurrent relay would have the same weakness. Likewise, a voltage relay with its drop-out set low enough to ride through a fault condition might not operate during out-of-step unless the machine impedance were high so that the terminal voltage would dip to a low value.

A more reliable scheme of protection makes use of a notching element actuated by out-of-step impulses. The notching relay trips the machine or sounds an alarm after a given number of pole slippages, usually three to five pairs. The notching element may be either a mechanical device or a counting chain of auxiliary relays arranged so that each link will notch up in succession as successive out-of-step impulses are given to it.

The notching prerequisite makes it possible to use more sensitive impulse-detecting relays without the possibility of tripping at the time of a line fault. A single impulse will be given to the notching element at the time of a fault. It is therefore necessary to reset the device after each system short circuit. Otherwise, repeated short circuits would eventually cause tripping. This resetting operation can best be performed automatically by means of a timing relay which is set in motion by the first notch and arranged to reset the notching element if the notching sequence is not completed within a specified time.

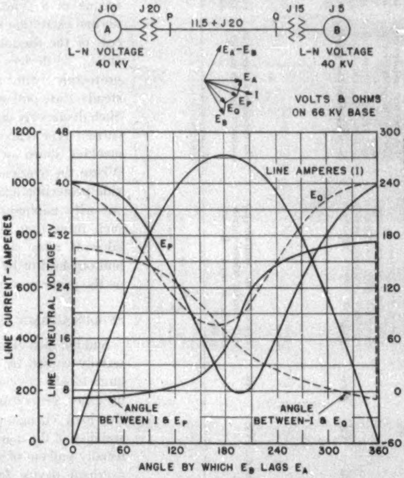


Figure 1. Behavior of the current and voltage at each end of a typical interconnection during a slip cycle

The total number of notches in the sequence should be as small as possible to minimize stress on the machine and disturbance to the system. However, it must be large enough to insure against operation from other system disturbances, as for example the impulses caused by the successive reclosing of a line breaker. A notching sequence constituting a pole slippage of three to five pairs of poles will usually provide sufficient margin.

An overcurrent or an undervoltage relay may be used to actuate the notching element. However, it may be difficult to set an overcurrent relay to discriminate between a hunting condition and an out-of-step condition. Also, the voltage dip at the machine terminals may not be enough to drop out a voltage relay positively. A more reliable scheme makes use of a duodirectional watt relay and an overcurrent relay, with their contacts connected in series as shown schematically in Figure 3. The alternate closing of the forward and reverse contacts of the watt relay with the overcurrent relay contacts closed will notch up the notching element.

The watt relay prevents the overcurrent relay, which may be set to less than full-load current, from notching up because of changes in load current. The object of the overcurrent relay is to prevent the watt relay from notching up during hunting. If the machine hunts during light load, the watt relay will follow the oscillations, but the current will normally not be high enough to operate the overcurrent relay. If the machine is carrying a heavy load the current element might pick up, but the power surges during hunting will normally not reverse in direction, and the watt relay would not close its reverse contacts. The combination thus provides reliable out-of-step protection except when the machine pulls out because of complete loss of field. A cylindrical-rotor machine without field excitation may operate as an induction

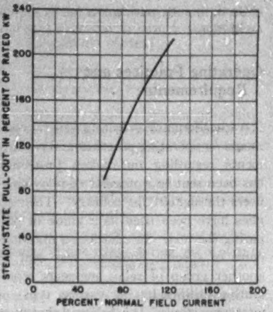


Figure 2. Effect of field excitation on steady-state pull-out of typical generator and system

generator at one or two per cent slip without reversal of power. The best protection against out-of-step operation from this cause is a field-failure relay.

Where there are several generators bussed together in a station, the out-of-step relays may have to be supplemented by a discriminating relay to indicate which machine is actually out of step. This might be a directional impedance relay used to indicate the direction of the electrical center, which would normally fall within any one machine that pulled out of step with others in a station. The difference in reactive current flow in the various machines might also be used as a discriminating factor. It is not known whether there are any such applications.

It is common practice to provide relays that will remove the field excitation from synchronous motors of moderate size when they pull out of step and reapply excitation at the proper time for resynchronizing. The power factor of the current flow into the motor has been used as an indication of loss of synchronism. In addition to power-factor relays, there are several relays designed especially to remove the field excitation on pull-out and reapply excitation for resynchronizing. One of these is the "slip-cycle impedance" relay which operates on the variations in impedance of the armature winding of the motor that occur during each slip cycle of out-of-synchronism operation. Another type of relay used for this purpose is the "field-frequency" relay which responds to magnitude and frequency of voltage generated in the field circuit. Also available is a "synchromatic" relay which responds to speed as a function of armature current and to time. There are many of these types of relays in successful operation.

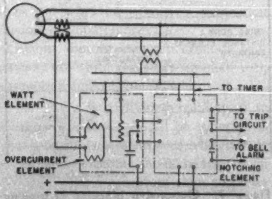


Figure 3. Out-of-step notching relay for protection of synchronous machines

Protection of Transmission Lines

Before Loss of Synchronism

Loss of synchronism between generating stations or between power systems is generally caused by some transient disturbance, such as a transmission line fault or a sudden load change. The ability of the interconnection to maintain synchronism during such a disturbance depends mainly on the speed of fault clearing and the angle between stations previous to the fault. The speed of fault clearing is fixed by the fault protective devices. However, the angular shift through the interconnection depends upon the load being interchanged and the impedance of the interconnection, both of which may vary. Either one or both of these quantities could serve therefore as a warning against possible loss of synchronism in case of a disturbance.

A watt relay can be used to sound an alarm or make a load correction when the load interchange reaches the allowable limit for a given impedance. However, load magnitude is not a reliable indication where the impedance is liable to change.

There is in service one group of relay installations intended to measure the angle between systems. The interconnections consist of both high- and low-voltage ties between systems. Stability cannot be maintained through the low-voltage tie in the event the high-voltage tie opens. A voltage relay is energized from a differential circuit supplied with voltage from each system. When the systems are over 60 degrees out-of-phase the relay operates to warn that something must be done immediately to relieve the condition.

Remote control by carrier-current telemetering offers possibilities of automatically giving an alarm or adjusting load interchange to conform to system conditions. There is one installation[3] where the receiver system phase angle is compared with the sending system phase angle by means of carrier current over a distance of 460 miles. The interchange is automatically controlled by this comparison of phase angle.

After Loss of Synchronism

When interconnected generating stations or power systems pull out of step, it is generally desirable to open the interconnection at such a point that system operation is least disturbed. However, all the fault protective relays in the interconnections are subjected to out-of-step current and voltage pulsations, and this may cause circuit breakers to be tripped at one or more undesirable locations. The usual function of out-of-step relays as applied to transmission lines is therefore to block undesirable tripping. They may also be used as out-of-step tripping relays to open rapidly the tie breakers at preselected locations during out-of-step conditions.

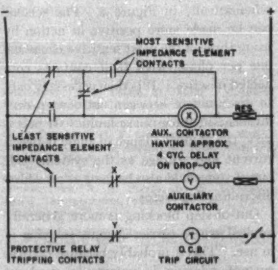

-
+
MOST SENSITIVE IMPEDANCE ELEMENT CONTACTS
X
RES.
LEAST SENSITIVE IMPEDANCE ELEMENT CONTACTS
AUX. CONTACTOR HAVING APPROX. 4 CYC. DELAY ON DROP-OUT
X
Y
AUXILIARY CONTACTOR
Y
T
PROTECTIVE RELAY TRIPPING CONTACTS
O.C.B. TRIP CIRCUIT

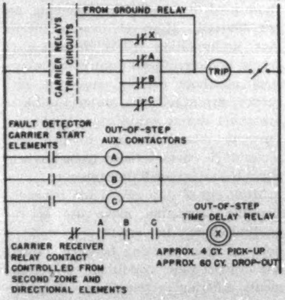
Figure 4. Out-of-step relay for blocking the trip circuits of fault-protection relays during out-of-step conditions

Relays operating on residual current or voltage for ground-fault protection are not affected during out-of-step, since the pulsations are balanced between phases. Also, parallel-line balanced-current relays and current differential relays, such as those used in a-c pilot-wire relaying, are not affected. However, overcurrent relays, undervoltage relays, distance-type relays, and directional comparison relays, such as those usually employed with carrier-current relaying, are susceptible to operation during out-of-step conditions. Distance relays can be provided with phase-angle characteristics to be less sensitive to system oscillations than to faults.

Most out-of-step relays for transmission-line protection make use of the gradual change in current or voltage or both in discriminating between a fault and out-of-step condition. Two elements differing in sensitivity enough to give a few cycles of time delay between pick-up as the two systems pull apart are used. Overcurrent elements can be used if the interconnected generating capacity is essentially constant, so that the out-of-step current can be safely predicted. Impedance elements are better suited, however, because the ratio of voltage to current during out-of-step conditions varies over a greater range and is more nearly independent of generating capacity. The impedance elements may be the same as those used for fault protection, or they may be separate from the fault-protective relays. If a fault occurs within the protected zone, both elements operate substantially together and the out-of-step feature is made inoperative. However, if there is a relatively slow decrease in the measured impedance, such as that characterized by a system oscillation alone, one element operates before the other, and an auxiliary relay is operated either to trip or to block tripping as desired.

A typical out-of-step relay is shown schematically in Figure 4. The scheme can be made more positive in action by using three of the most sensitive elements, one per phase, with their contacts connected in series. It is then necessary only to discriminate between out-of-step conditions and three-phase faults. If necessary, the gradual change in angle between current and voltage as the systems pull out of step could also be used as an added discriminating factor.

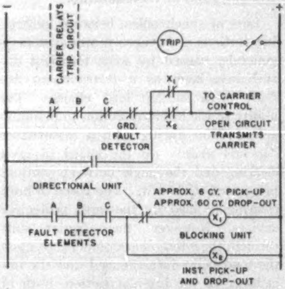
Out-of-step blocking is more generally applied where carrier-current relaying is in use. This is probably because carrier-current relaying is commonly used on systems subject to instability and because out-of-step blocking can be readily adapted to carrier relaying. The blocking may be accomplished either by opening the trip circuit at each relay location during the out-of-step tripping impulse, or by transmitting a carrier signal through the slip cycle when an out-of-step condition is indicated.



FROM GROUND RELAY
CARRIER RELAYS
TRIP CIRCUITS
X
A
B
C
TRIP
FAULT DETECTOR CARRIER START ELEMENTS
OUT-OF-STEP AUX. CONTACTORS
A
B
C
OUT-OF-STEP TIME DELAY RELAY
A
B
C
X
CARRIER RECEIVER RELAY CONTACT CONTROLLED FROM SECOND ZONE AND DIRECTIONAL ELEMENTS
APPROX. 4 CY. PICK-UP
APPROX. 60 CY. DROP-OUT

Figure 5. Out-of-step blocking combined with carrier-current relaying. Trip circuit is blocked during out-of-step period

A typical scheme of blocking the trip circuit is shown in Figure 5. The same distance-type elements are used for carrier control, for backup protection with the carrier out of service, and for indicating an out-of-step condition. Auxiliary contactors are used to block the tripping circuit during the period of a slip cycle if a three-phase-fault indication in the backup zone persists for approximately four cycles before the local carrier receiver relay closes its contacts indicating an internal fault. As indicated in Figure 1, impedance-type fault-detector elements would operate before the slip cycle advanced far enough to indicate an internal fault. This scheme can be made operative with the carrier out of service and the relays operating as distance type.



CARRIER RELAYS
TRIP CIRCUIT
TRIP
X_1
A
B
C
TO CARRIER CONTROL
GRD. FAULT DETECTOR
X_2
OPEN CIRCUIT TRANSMITS CARRIER
DIRECTIONAL UNIT
APPROX. 6 CY. PICK-UP
APPROX. 60 CY. DROP-OUT
A
B
C
X_1
FAULT DETECTOR ELEMENTS
BLOCKING UNIT
X_2
INST. PICK-UP AND DROP-OUT

Figure 6. Another method of combining out-of-step blocking with carrier-current relaying. Carrier signal is transmitted to block tripping during out-of-step period

A typical scheme of blocking by transmission of carrier signal is shown in Figure 6. An auxiliary blocking unit causes a carrier signal to be transmitted during the out-of-step period when a three-phase fault indication in the nontripping direction persists for some six cycles at either terminal.

There are some installations where the elements of distance relays used for fault protection without carrier are also used for out-of-step blocking or tripping. This is usually done as shown in Figure 6. By means of a control switch the out-of-step relay can be set to either block tripping or to trip on the first out-of-step swing.

In some carrier-current installations high-speed tripping is sacrificed in an attempt to prevent tripping during out-of-step conditions. Other schemes in use transmit blocking carrier for a short time after an external fault has been cleared, in anticipation of a heavy system oscillation.

Operating Practices and Requirements

A questionnaire soliciting general comments on operating practices and requirements regarding out-of-step protection has been sent to a number of relay engineers throughout the country. Their replies have been used as a guide in preparing the text. In addition, a questionnaire of specific questions was sent to another group of relay engineers representing utilities having various types of interconnections. It is believed that their replies give a fair cross section of actual practices and requirements. The questionnaire read as follows:

1. Have any cases of synchronous-machine pull-out occurred on the system you represent?

2. What precautions, if any, are employed to recognize a condition such as sudden overload or loss of excitation that might cause a synchronous machine to pull out of step? Please explain the principle of the scheme of protection used and its operating record.

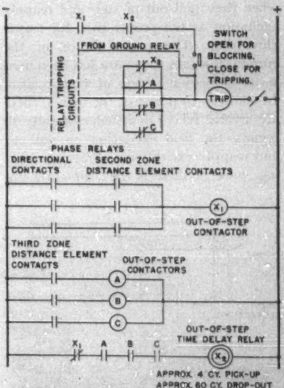
3. Which of the following do you consider the most desirable in case of the loss of excitation on a major generator:

(*a*). Sound an alarm.

(*b*). Trip the machine off the line.

(*c*). Automatically transfer to an emergency source of excitation.

4. What scheme of protection, if any, is used to detect that a machine has pulled out of step? What is its operating record?



X_1
X_2
SWITCH OPEN FOR BLOCKING. CLOSE FOR TRIPPING.
FROM GROUND RELAY
RELAY TRIPPING CIRCUITS
X_2
A
B
C
TRIP
PHASE RELAYS
DIRECTIONAL CONTACTS
SECOND ZONE DISTANCE ELEMENT CONTACTS
X_1
OUT-OF-STEP CONTACTOR
THIRD ZONE DISTANCE ELEMENT CONTACTS
OUT-OF-STEP CONTACTORS
A
B
C
OUT-OF-STEP TIME DELAY RELAY
X_1
A
B
C
X_2
APPROX. 4 CY. PICK-UP
APPROX. 60 CY. DROP-OUT

Figure 7. Out-of-step blocking or tripping combined with fault-protection relays without carrier current

5. Which of the following do you consider the most desirable in case a synchronous machine pulls out of step?

(*a*). Sound an alarm.

(*b*). Trip the machine off the line.

6. Have any cases of system instability, that is, instability between systems or between generating stations, been experienced?

7. Have there been any cases where out-of-step or severe system swings have caused line relays to operate?

8. What scheme, if any, is used to block tripping of line relays during out-of-step or to separate the system at a selected point? Is the scheme used with or without carrier current? What is its operating record?

9. Which of the following schemes of operation do you consider most desirable?

(*a*). Provide no out-of-step blocking and let the line relays trip where they may.

(*b*). Provide blocking of all relays that are liable to trip during out-of-step with the thought that stability will be regained.

(*c*). Provide blocking of all relays that are liable to trip during out-of-step except those at one selected location where out-of-step tripping is least objectionable.

10. Do you consider it desirable to keep the out-of-step blocking devices entirely separate from the line relays or to incorporate them in the line-relaying scheme if possible?

11. Miscellaneous comments.

A summary of the replies to the questionnaire is given in Table I.

General Conclusions

1. Practically all operating companies have had synchronous machines pull out of step because of loss of excitation or some other disturbance.

2. It is not general practice to provide generators with means of detecting disturbances that might cause the machine to pull out of step. Some have reverse-current or undercurrent relays in the field circuit, and at least one company protects against inadequate excitation by measuring the reactive current taken by the generators. Quite a few provide partial protection by means of overcurrent relays, although it is recognized that this does not provide positive protection against out-of-step operation. Large synchronous motors and synchronous condensers are quite generally supplied with protection against loss of field.

3. There is a divided opinion as to whether an alarm should be sounded in case a major generator loses excitation or whether the machine should be tripped off the line. Most operators seem to prefer tripping.

4. It is not general practice to provide special devices for detecting when generators pull out of step. Large synchronous motors and synchronous condensers are often provided with some degree of protection by overload relays. Some are provided with out-of-step relays.

5. There is a divided opinion as to whether an alarm should be sounded when a synchronous machine pulls out of step or whether it should be tripped off the line. The majority seem to favor tripping. There is a feeling that if a generator pulls out of step because of loss of excitation it will not regain synchronism without excessive disturbance if excitation is restored. It is generally felt that a machine will be seriously damaged if it is not tripped immediately. However, there have been cases where turbine generators have pulled out of step because of loss or reduction of excitation, have run for a minute or two as induction generators, and then pulled back into step when excitation was restored. Inspection showed no apparent damage.

6. Practically all utilities, except those consisting of steam stations connected rigidly together electrically, have experienced system instability.

7. Most utilities have experienced undesirable operation of fault-protective relays as a result of system instability.

8. Quite a number of utilities attempt either to block line relays from tripping because of out-of-step conditions or to set the relays so that tripping will occur at a preselected point. Out-of-step blocking in conjunction with carrier relaying is the method most commonly used.

9. Most utilities would prefer to provide blocking of all relays that are liable to trip during out-of-step except those at one selected location where out-of-step tripping is least objectionable. Some use out-of-step relays for tripping where fault-protection relays cannot be depended on to trip during out-of-step operation.

10. There is a divided opinion as to whether out-of-step devices should be kept entirely separate from the line relays or whether they should be incorporated in the line-relaying scheme if possible. There may be applications where the settings of the elements for out-of-step protection should be different from those for fault protection. In some cases there is an advantage in having the out-of-step devices separate from the line relays.

11. Most operators agree that, where possible, consideration should be given to strengthening interconnecting ties between systems and to decreasing fault clearing time in an effort to prevent instability. However, this cannot always be economically justified. Also, the war has made it necessary to explore every possibility to attain maximum utilization of existing equipment. Out-of-step blocking and selective tripping provide one effective way of doing this.

12. There are out-of-step devices and methods available that will meet most requirements if applied properly. However, operating experience with some of them is limited. There is an apparent need for further investigation of the action of out-of-step relays during certain conditions. Among these are:

(*a*). A prolonged fault during out-of-step.

(*b*). The closing of a circuit breaker on a system out of step.

(*c*). Out-of-step operation through a long high-voltage line where charging current is to be reckoned with.

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Distance Relay Protection for Subtransmission Lines Made Economical

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Synopsis: Less important transmission lines are usually protected with directional overcurrent relays. Much better protection and consequently improved service would be provided by modern high-speed distance relays but at several times the cost. What is required is a distance relay arrangement considerably less expensive than the relays now used on high-voltage transmission lines but with as nearly as possible the same characteristics. Such an arrangement is now available. The paper describes successful tests in the laboratory and on an actual system in this country with both natural and staged faults.

The equipment consists of a single high-speed reactance relay which is enabled to protect all three phases by being automatically switched to the correct potential and current by a phase selector relay.

LESS important transmission lines are usually protected with directional overcurrent relays or induction disk-type impedance relays. On most of such subtransmission circuits there are stations where, in order to obtain selectivity, the relays have to be set for perhaps two seconds, and occasionally selectivity between two relays cannot be obtained at all because under one condition one relay has more current and under another condition the other has more. Better selectivity can be obtained with distance relays, but they are more expensive than overcurrent relays.

The modern high-speed induction-cylinder-type reactance relay is extremely accurate and easy to set. It is more expensive than either the overcurrent relay or the impedance relay of the induction-disk type, hence its use has generally been confined to high-voltage transmission lines. What is required for subtransmission lines is a relay with substantially the characteristics of the high-speed reactance relay at the price of the directional overcurrent relay. Although this is not yet literally possible, a very good compromise can be obtained by using one reactance relay, instead of the usual three, and a selector relay to connect it to the proper currents and potentials.

While, in general, stability is not the problem on subtransmission circuits that it is at the higher voltages, there are cases where it is desirable to obtain fast clearing times, such as on circuits between a large generating station and a load area; also, where a line may be burned down unless promptly cleared. In the arrangement to be described the selector relay operates in four cycles and the distance relay in one cycle for nearby faults making a total of 5 cycles on a 60 cycle system.

It is seldom that more than one of the distance relays in the three phases operates on a fault because only one of the line-to-line potentials is affected in line-to-line faults. In balanced three-phase faults all three relays may operate, but only one is necessary to effect tripping. Consequently, for less important transmission lines and those where one cycle relay operation is not required, it is permissible to use a single distance relay and to connect it to the faulted phases by a selector relay.

Compared with directional overcurrent relays for phase faults the new scheme is easier to apply and gives complete selectivity combined with high-speed tripping at only a moderate increase in cost. Compared with conventional distance relays the new scheme effects a saving in critical materials and labor, since one distance relay and an auxiliary relay replaces three distance relays. This in turn reduces investment, panel space, and maintenance.

Historical

In 1934 a paper[3] was presented before the Institute describing different methods of controlling distance relays with selector relays so that one set of distance relays could take care of both phase and ground faults or one relay could protect three phases against either phase or ground faults. Later 72 induction-disk-type reactance relays were installed, normally connected for phase faults, and with provision for switching to wye potentials, and so forth, for ground faults. A report of satisfactory operation was received, and twenty-four more were installed on the same system.

Selector relay schemes were widely used in Europe, but they did not become popular in this country. One reason for this may have been that the distance measurement with the selector schemes then available was not as accurate as with the full set of relays, particularly on ground faults; another may have been that the minimum time was about half a second. It is the purpose of this paper to describe a modern arrangement of distance and selector relays in which the same selectivity and accuracy is obtained as with a full set of distance relays and the minimum time is 0.08 second.

In one scheme a single distance relay protects against phase faults. In the other a single distance relay protects against ground faults. Both have been thoroughly tested in the laboratory, on the artificial transmission line, in life tests, and by staged faults on actual systems. Both are in service on lines in this country.

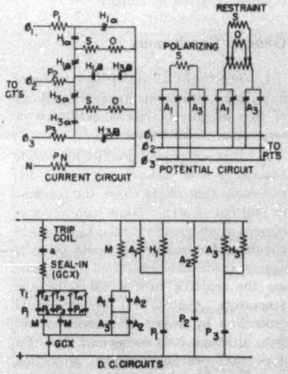

Figure 1. Schematic diagram for phase faults

$P_1P_2P_3P_N$—Overcurrent fault detectors in the three phases and residual circuit

$A_1A_2A_3$—Auxiliary relays controlled by $P_1P_2P_3$

H_1H_3—Current transfer relays. α and β refer to their two sets of transfer contacts

S and O—Starting and ohm units of the reactance relay

M—Relay for delaying tripping 0.025 second

$T_1T_2T_3T_N$—Targets indicating circuit involved in fault

Paper 43-92, recommended by the AIEE committees on power transmission and distribution for presentation at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943. Manuscript submitted April 8, 1943; made available for printing May 15, 1943.

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The kindness of the Duquesne Light Company and of the Central New York Power Corporation in arranging for the test was greatly appreciated. The engineering and operating staffs of the two companies are to be commended for their efficient planning and execution of the tests.

HIGH-SPEED REACTANCE RELAY

The relay provides three zones of operation, giving the well-known stepped time-distance characteristic which has been described in previous papers. Faults up to 90 per cent of the distance to the next station are cleared instantaneously; faults in the neighborhood of the next station are cleared after a delay sufficient to allow the proper breaker at that station to open; more remote faults are cleared in a still longer time. These three zones are adjustable in distance by means of an internal tapped transformer; and all but the shortest range, which operates in about one cycle, are adjustable in time.

Upon the occurrence of a fault in the tripping direction the relay operation is initiated by a directional impedance unit called the starting unit. The zone is decided by the ohm unit, which measures the reactance of the line between the relay and the fault, and the time is controlled by a clockwork timer, self-wound by a d-c solenoid.

The starting and ohm units are of the induction-cylinder type which is well known for its efficiency and a steady torque. The ohm unit is accurate within plus or minus two per cent over a wide range of current and power factor. The use of reactance as a basis of distance measurement minimizes error caused by arc resistance which is particularly important in ground faults.

For phase faults the reactance relay is supplied with the potential between the faulted conductors and the vectorial difference of the current flowing in them. With this excitation the relay will measure the same distance for three-phase, phase-to-phase, and double-ground faults. For ground faults the reactance relay has the phase-to-neutral potential of the faulted conductor and the current in the conductor plus a predetermined portion of the residual current. With the proper amount of residual current the relay will measure substantially the same distance regardless of changes in system connections.

Protection Against Phase Faults

A high-speed reactance relay is controlled by a selector relay. Figure 2 shows the external appearance of these relays.

The operating characteristics of the reactance relay are the same as those of the standard *GCX* relay except that shorter lines can be protected because the potential circuit is not continuously energized and hence can be short-time rated.

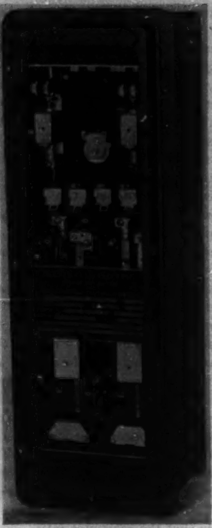


Figure 2a (left). Selector relay for phase faults

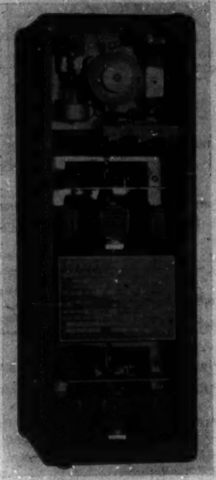


Figure 2b (right). Type GCX high-speed reactance relay

The selector relay contains two calibrated instantaneous overcurrent units (P_1 and P_3 in Figure 1) in phases 1 and 3, which detect the fault and determine the phases involved. These overcurrent units control auxiliary relays A_1 and A_3 respectively which apply the line-to-line potential between the faulted phases to the restraining coils and the quadrature potential to the polarizing coil of the reactance relay. They also energize the proper current transfer relay, H_1 or H_3, and set up the correct tripping circuits for target indication. There are two smaller overcurrent units, P_2 and P_N, in phase 2 and the residual circuit, which are for target indication. There are two current transfer units H_1 and H_3 which connect the current coils of the reactance relay to the current transformers of the faulted phases. The selector relay also contains a time-delay unit *M* which delays tripping slightly so as to ensure proper selection by the reactance relay unaffected by an external fault changing its character. There are four targets marked phase 1, phase 2, phase 3, and ground.

Table I

Type of Fault	E Restraint (Ohm Unit and Starting Unit)	I	E Polarizing (Starting Unit)	X Measured*
$\phi_1-\phi_2$ or $\phi_1-\phi_2-G$	E_{12}	I_1-I_2	E_{23}	$2\,X_p$
$\phi_2-\phi_3$ or $\phi_2-\phi_3-G$	E_{23}	I_2-I_3	E_{31}	$2\,X_p$
$\phi_3-\phi_1$ or $\phi_3-\phi_1-G$	E_{13}	I_1-I_3	E_{21}	$2\,X_p$
$\phi_1-\phi_2-\phi_3$	E_{13}	I_1-I_3	E_{21}	$2X_p$

* X_p is the positive sequence reactance.

The currents and potentials supplied to the reactance relay are shown in Table I.

The overcurrent units are of standard design and the current transfer units have high-pressure overlapping contacts which ensure that the current-transformer circuit will never be opened.

Ground-Fault Protection

The relays are similar to those used for phase faults, but wye potentials are used, and residual current is supplied to the second current coil instead of minus the current from the next lagging phase. Residual current is also supplied from any parallel lines in order to compensate for mutual coupling.

The ground-reactance relay differs from the phase-reactance relay only in the addition of a capacitor to give the starting unit maximum torque with residual current 45 degrees lagging the residual potential. Figure 3 shows how the connections of the ground-selector relay differ from those of the phase-selector relay which was shown in Figure 1.

Type of Fault	E Restraint (Ohm Unit and Starting Unit)	I	X Measured
ϕ_1-G	E_1	I_1+KI_{res}	$2\,X_p$
ϕ_2-G	E_2	I_2+KI_{res}	$2\,X_p$
ϕ_3-G	E_3	I_3+KI_{res}	$2\,X_p$

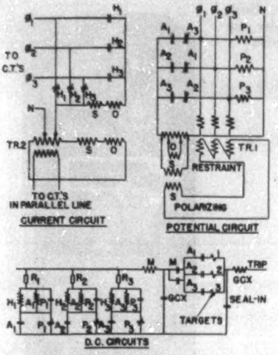


Figure 3. Schematic diagram for ground faults

$P_1P_2P_3$—Undervoltage fault detectors in the three phases

$A_1A_2A_3$—Auxiliary relays controlled by $P_1P_2P_3$

$H_1H_2H_3$—Current transfer relays

$R_1R_2R_3$—Resistors

TR 1—Wye-broken delta auxiliary potential transformer

TR 2—Residual auxiliary current transformer

Three undervoltage units energized with wye potential are used for detecting ground faults on systems grounded through impedance. Figure 5 shows how a single-phase ground fault on an impedance-grounded system displaces the neutral so that the faulted phase gets low voltage whereas the other two get above-normal voltage, so that the selection of the faulty phase is very definite. On solidly grounded systems either undervoltage or overcurrent fault detectors can be used, depending upon the distance from a grounding point.

The undervoltage units are set to drop out at 90 per cent of normal potential. The reactance relay is normally de-energized; when a fault occurs the undervoltage unit *P* in the faulted phase drops out energizing an auxiliary relay *A* and a current transfer relay *H*. The relay *A* seals around the contacts of *P* and applies the potential of that phase to the reactance relay as shown in Figure 3 while the transfer unit *H* connects the relay-current coils to the current transformer of the same phase, so that the relay is now ready to measure the distance to the fault. When the fault has been cleared and line voltage restored to normal, the *P* units pick up and short-circuit the coils of the *H* and *A* units so that they reset.

The reactance relay does not trip directly but through an auxiliary relay *M* with a slight time delay; this is in order to give the reactance relay a chance to reset if an external fault should change its character. For example, if an external single-phase fault should blow into a second phase, the reactance relay may close its contacts, because the additional current between the conductors may make the line reactance appear to be lower than the ohm unit setting. This condition will be very brief because the operation of the second current-transfer relay will by-pass the interphase current; the time-delay relay requires that the reactance-relay contacts remain closed for one cycle before it will trip the breaker; hence the reactance-relay contacts will reset before the auxiliary relay can pick up and trip. During this double-ground fault condition the selector relay supplied both current windings with residual current and the potential winding with the leading of the two wye potentials involved in the fault. This ensures underreaching on double-ground faults so that the relay acts only as a back-up for phase relays.

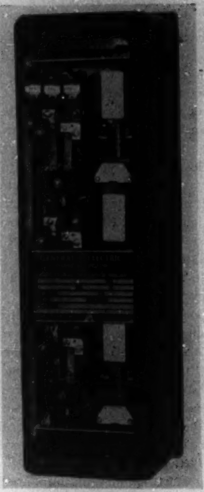


Figure 4. Selector relay for ground faults

The current coils of the reactance relay are all double-wound. One winding is supplied with residual current and the other with phase current controlled by the phase selector relay. The restraining potential coils of the ohm unit and starting unit have phase-to-neutral potential controlled by the selector relay. The polarizing or directional potential coil of the starting unit has residual potential which is supplied by auxiliary potential transformers connected wye-broken delta. In order to obtain correct measurement the potential should be supplied by three potential transformers in wye-wye grounded on the high side.

Table II

Test Number	Fault Location	Type	Fault	Location	Phase Targets	Zone Targets
1	Ilion	Solid	1–2–*G*	5% beyond zone 1 setting	1–2–*G*	2
2	Ilion	Solid	1–2–*G*	5% inside zone 1 setting	1–2–*G*	1
3	Ilion	Solid	2–3–*G*	5% inside zone 1 setting	2–3–*G*	1
4	Ilion	Solid	3–1–*G*	5% inside zone 1 setting	3–1–*G*	1
5	Ilion	Solid	3–1–*G*	5% beyond zone 1 setting	3–1–*G*	2
6	Ilion	Solid	1–*G*	5% inside zone 1 setting	—	—
7	Ilion	Solid	3–*G*	5% inside zone 1 setting	—	—
8	Ilion	Solid	1–2–3–*G*	5% inside zone 1 setting	1–2–3	1
9	Ilion	Solid	1–2–3–*G*	5% inside zone 1 setting (current reversed in relay)	—	—
10	Washington Street	Solid	1–2–3–*G*	On outgoing side of bus (current reversed in relay)	—	—
11	Washington Street	Solid	1–2–3–*G*	On outgoing side of bus	—	—

Targets

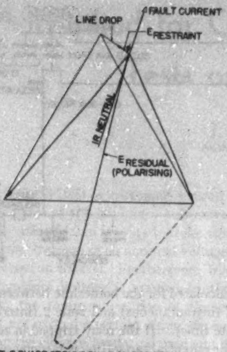
The usual targets are provided in the reactance relay to indicate the zone or time of operation. In the selector relay for phase protection four targets are provided for the three phases and ground.

The selector relay for ground faults has three targets. A ground target is not necessary, because the relay does not work unless the fault involves ground.

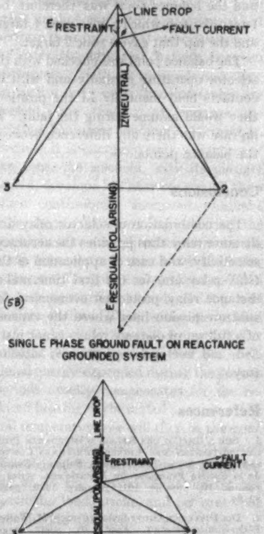
Tests

The tests were of three kinds: laboratory tests, including life tests to check the effectiveness of the "switching"; tests on the artificial transmission line to check the sequence of operation and the accuracy of distance measurement; and, finally, natural and staged tests and operating experience on an actual transmission line.

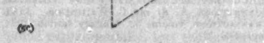
The point of greatest interest for most engineers is the idea of switching the current circuit. In the standard reactance relay, the potential and d-c circuits have been switched from zone 1 to zone 2 con-



SINGLE PHASE GROUND FAULT ON RESISTANCE GROUNDED SYSTEM

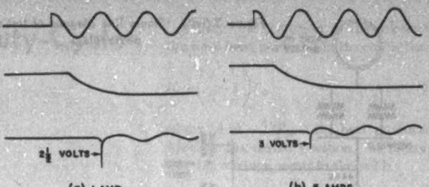


SINGLE PHASE GROUND FAULT ON REACTANCE GROUNDED SYSTEM



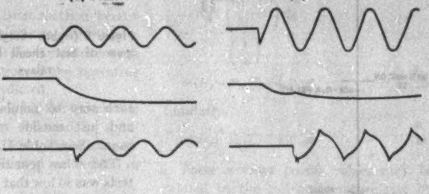
SINGLE PHASE GROUND FAULT ON SOLIDLY GROUNDED SYSTEM

Figure 5. Vector diagram of ground fault



(a) 1 AMP

(b) 5 AMPS



(C) 40 AMPS

(d) 100 AMPS

Figure 6. Oscillograms of current transfer relay operation

The top trace is the current in the winding of the current transformer. The middle trace is the current in the coil of the current transfer relay. The bottom trace is the voltage across the current windings of the reactance relay and the contacts of the current transfer relay

nections for the last eight years without trouble, and it is actually easier to switch current than potential because the current transformer will provide a high enough potential to break down an insulating film on the contacts. With contacts of adequate size, overlap, and pressure, switching current is no less reliable than switching potential, and the contacts require less maintenance.

(a). Laboratory Tests

Oscillograms (Figure 6) taken at 1, 5, 40, and 100 amperes showed smooth transition of current. The voltage across the contacts was negligible at the higher currents; at five amperes there was a peak of about three volts magnitude which lasted only about 0.00002 second. After a continuous life test of 5,000 operations at 100 amperes, the contacts were still in good shape; oscillograms showed some peaks up to 11 volts, but of less than one millisecond duration. A life test of 500 operations with zero overlap marred the contacts but did not interfere with their operation. A further life test with $^1/_{32}$-inch negative overlap, that is, break before make, caused pitting of the contacts, but no failure, although there was a large amount of energy in the arc because of 100 amperes flowing through $^1/_{32}$ inch of air. This was obviously worse than any possible condition of deteriorated adjustment, so it was felt that the design was very satisfactory.

In all these life tests, the circuit was arranged to close 100 amperes on the *a* contacts for three seconds and then to transfer back to the *b* contacts at three amperes for 15 seconds. These conditions are very much more severe than any service duty because of the heat that is accumulated in the contacts.

(b). Artificial Transmission-Line Tests

In these tests, the relay was applied to lines of different lengths, and the tap settings of the reactance relay for the borderline between zone 1 and zone 2 operation were checked for different conditions of generator and load to see if any variations in balance point occurred which could be blamed on the selector relay. In addition, tests were made in which the type of fault was changed (for instance from phase 1–ground to phase 1–phase 2–ground) to determine if, during the transition, any incorrect tripping would result from momentarily having the wrong potential or current on the distance measuring elements. Using all possible fault combinations it was impossible to cause incorrect operation.

(c). System Tests on Phase Relays

The reactance and selector relays for phase faults were installed last year on the Washington Street–Ilion line at the Washington Street substation on the 44-kv system of the Central New York Power Corporation. This circuit has a calculated resistance of 7.37 ohms and a calculated reactance of 10.75 ohms, which corresponds to 1.61 ohms secondary reactance. The relay measures $2X_p$ which is 3.22 ohms; zone 1 (instantaneous) was set to reach ten per cent short of Ilion, 2.86 ohms, and zone 2, 3.45 ohms. The time settings were one-half second and two seconds for zone 2 and zone 3.

There have been two faults on this circuit during electrical storms. The first case of trouble was a one-wire-to-ground fault approximately 90 per cent of the distance from Washington Street to Ilion. The relays did not operate on this disturbance, which was correct.

When the second fault occurred the relays operated and showed 1–2–3–*G* and zone 1 targets, indicating that a fault had probably started two-wire-to-ground and then involved the third phase. The

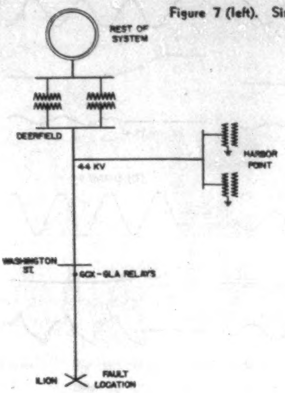


Figure 7 (left). Single line diagram of test circuit for phase relays

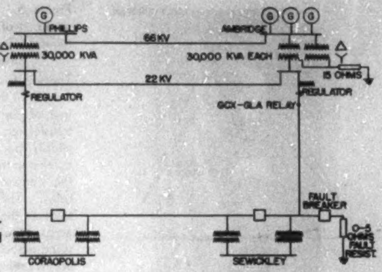


Figure 8 (right). Single line diagram of test circuit for ground relays

fault was approximately 25 per cent of the distance from Washington Street to Ilion. A simultaneous three-phase-to-ground fault would not show a *G* target, because the fault current would be practically balanced in the three phases and would give negligible residual current.

Several cases of trouble have occurred on other circuits in the next bus section or in the bus section back of the relay where fault current was reversed in the relay and the relay scheme functioned correctly; that is, did not operate.

For the purpose of the tests a portion of the 44-kv system was reconnected temporarily as in Figure 7. The short-circuit phase current was supplied by two 15,000-kva, 110,000 wye to 44,000 delta, transformer banks connected to the 110,000-volt system at the Deerfield substation in Utica. The calculated three-phase short circuit is 1,178 megavolt-amperes on the Deerfield 110-kv bus. Two grounding transformers, located at Harbor Point substation in Utica, supplied the ground current.

Solid faults were placed on the Washington Street–Ilion line between the line disconnect and the oil circuit-breaker at Ilion with the circuit de-energized. The faults were initiated on the system from the Washington Street 44,000-volt bus by closing the circuit breaker at the Washington end. The 44,000/110-volt potential transformers connected to this bus supplied potential to the relays and a *PM*-13 oscillograph.

Instead of moving the fault location the relay setting was changed five per cent each way to simulate faults just inside and just outside of the instantaneous zone. See Table II.

The relay potential in the last two tests was so low that indeterminate action was expected, but actually, in test 11 the relay moved slowly toward the tripping contacts and would probably have tripped if the back-up relay had not tripped first.

(d). System Tests on Ground Relays

The relays were tested on the 22-kv system of the Duquesne Light Company near Pittsburgh. The principal purpose of this was to determine the effect of load current and ground resistance on the accuracy of ground-distance relays on a system grounded through a resistance of roughly one ohm per kilovolt. The results of the tests will be published later when complete and edited. The present description of the tests will be limited to reference to the operation of the selector relay.

The tests were made at the Ambridge station on the line to Sewickly where the faults were applied as shown in Figure 8. Sewickly was also fed from Phillips in the other direction via Coraopolis. Between each source of power and the fault was a voltage regulator so that the phase and magnitude of the current transfer could be accurately controlled over 360 degrees range. The resistance of the faults at Sewickly was varied between zero and five ohms, and the balance points were found for various conditions to determine the effect of load in combination with fault resistance.

Oscillograms were taken of the phase current, the residual current, the phase potential, the residual potential, the current and potential supplied to the reactance relay, and the tap current. For each condition, the setting of the relay was calculated for the borderline between zone 1 (instantaneous) and zone 2 (intermediate time). If the relay tripped in zone 1, the potential tap setting was increased one or two taps until it showed a zone 2 target, and the balance point was therefore between the tap which gave a zone 1 target and the tap that gave a zone 2 target.

The balance point was checked with the selector operating normally and with its contacts held manually in the positions they would assume during the fault. In no case was there any difference between the balance points.

Conclusions

The combination of selector relay and distance relay thus provides the accuracy, selectivity, and ease of application of the *GCX* relay and for the first time makes distance relay protection economical for subtransmission lines where the expense of a full set of distance relays is not justified, and five-cycle operation is satisfactory.

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A Method for Correlating Duty-Cycle Tests on Solenoids

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A SOLENOID is customarily rated in terms of the minimum pull exerted at any point within its rated stroke when a certain fraction of its nominal voltage is impressed on the coil. Furthermore, when full nominal voltage is applied continuously, the temperature rise of the coil must not exceed a certain value, depending on the class of insulation used and the maximum ambient temperature.

In many applications, however, solenoids are not energized continuously but perform their function at more or less regular intervals with rest periods between operations. With a-c solenoids operating on such a duty cycle, two influences are present, one tending to decrease the heating, the other tending to increase it. Because it is energized only a fraction of the time, the solenoid, with its plunger seated, develops less heat per second than when continuously energized. On the other hand, the power input during the brief time when the plunger is in motion is many times greater than when the latter is seated. The total energy thus expended at each operation depends on the load and the stroke. Thus, with a given load and stroke, it is possible to operate the solenoid at such intervals that the additional energy expended during the pickup periods exactly compensates for the reduced heating in the seated position. The coil temperature rise will then be the same as in the continuously energized case, and the solenoid may be considered as operating at its full duty-cycle rating for the particular load, stroke, and per cent time energized in question.

The relations between load, stroke, per cent time energized, operations per unit time, and temperature rise are best determined experimentally, but the procedure is tedious if the operation frequency must be varied by trial on each test until the allowable temperature rise is reached. Moreover, the results are then applicable only in cases where that particular temperature rise limit is prescribed. With the test and correlation method hereinafter described, a minimum of tests provide data from which the temperature rise under any combination of the operating variables may be predicted.

The analysis underlying the correlation method is given first, followed by a description of the test and correlation procedure. The symbols used are defined in the appendix.

Analysis

The static pull curve of a typical a-c solenoid is shown in Figure 1. If the plunger starts its closing motion at a gap s, the kinetic energy possessed by the moving parts of the solenoid and its load at gap x will be

$$E_x = \int^{s} (f-L)dx = \frac{Wv^2}{2g} \tag{1}$$

whence $v = -\sqrt{\dfrac{2gE_x}{W}}$ (2)

Since

$$dt = \frac{dx}{v}, \qquad T = \int_{s}^{0} \frac{dx}{v} = \sqrt{\frac{W}{2g}} \int_{0}^{s} \frac{dx}{\sqrt{E_x}} \tag{3}$$

which may be written as

$$T = \sqrt{W}F_1(L, s, x) \tag{4}$$

The heat input to the coil during the plunger travel is

$$H_c = \int_{0}^{T} P_c dt \tag{5}$$

Figure 2 shows a typical relation between P_c and x, taken from static tests. Actually, when the plunger moves rapidly, P_c may also be a function of those factors which affect the speed, namely, L, W, s, x. Then, using equation 4 in 5

$$H_c(L,W,s) = \sqrt{W} \int^{0} P_c(L,W,s,x)dF_1 \times (L,x,s) \tag{6}$$

where the integration limits are on the variable x. Hence

$$H_c(L, W, s) = \sqrt{W}F_2(L, W, s) \tag{7}$$

Similarly

$$H_t(L, W, s) = \sqrt{W}F_3(L, W, s) \tag{8}$$

During one operation of the solenoid, the total heat produced in the coil is then

$$H_c + \left(\frac{A}{N} - T\right)P_{cs} \tag{9}$$

where $(A/N-T)P_{cs}$ is the heat produced while in the seated position. It follows that the average power in the coil is

$$\overline{P}_c = \frac{H_c + \left(\dfrac{A}{N} - T\right)P_{cs}}{\left(\dfrac{1}{N}\right)} = (H_c - P_{cs}T)N + P_{cs}A \tag{10}$$

Similarly,

$$\overline{P}_t = (H_t - P_{ts}T)N + P_{ts}A \tag{11}$$

These average power values may be related to the coil temperature rise by considering the equivalent thermal circuit of the solenoid shown in Figure 3. If the thermal resistances are assumed independent of temperature or temperature difference, it is evident that the temperature at 0, representing the average condition in the coil, will be a linear function of $\overline{P}_c$ and $\overline{P}_t$ of the form

$$\theta = F_4(R_{ca}, R_{ct}, R_{ta})\overline{P}_c + F_5(R_{ca}, R_{ct}, R_{ta})\overline{P}_t \tag{12}$$

Actually, the various R's are not strictly constant even during a single operation of the solenoid. When the plunger is extended, the inner surfaces of the coil and the plunger itself are more effectively cooled than when the latter is seated. If it can be assumed that the heat transfer through each of the resistances is improved by a factor $(1+K_1S)$ whenever the plunger is extended, the average resistances for a complete operation cycle are:

$$R_{ca} = \frac{R_{ca}'}{1+K_1S(1-A)}; \quad R_{ct} = \frac{R_{ct}'}{1+K_1S(1-A)};$$
$$R_{ta} = \frac{R_{ta}'}{1+K_1S(1-A)} \tag{13}$$

Hence equation 12 becomes

$$[1+K_1S(1-A)]\theta = F_4(R_{ca}', R_{ct}', R_{ta}')\overline{P}_c + F_5(R_{ca}', R_{ct}', R_{ta}')\overline{P}_t = k_c\overline{P}_c + k_t\overline{P}_t \tag{14}$$

Substituting equations 10 and 11 into 14 there results

$$[1+K_1S(1-A)]\theta = [k_c(H_c - P_{cs}T) + k_t(H_t - P_{ts}T)]N + [k_cP_{cs} + k_tP_{ts}]A$$

or

$$\theta = \frac{F_6(L, W, s)N + K_2A}{1+K_1S(1-A)} \tag{15}$$

K_1, F_6, and K_2 may be determined as described in "Determination of Constants." Having done this, θ may be

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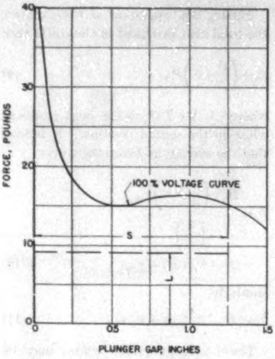

Figure 1. Pull curve of a typical a-c solenoid

evaluated from equation 15 for any combination of L, W, S, A, and N.

Determination of Constants

(A). K_2

The solenoid is energized continuously at full voltage until steady temperature rise is attained. According to equation 15, the coil rise θ is then

$$\theta = K_2A = K_2 \quad (16)$$

since $A = 1.0$ and $N = 0$

(B). F_6 AND K_1

Equation 15 may be written in the form

$$[1+K_1S(1-A)]\theta - K_2A = F_6(L, W, s)N \quad (17)$$

which can be of the straight-line form

$$y = mx$$

if

$$y = [1+K_1S(1-A)]\theta - K_2A$$
$$x = N$$
$$m = F_6(L, W, s) \quad (18)$$

Thus a plot of $[1+K_1S(1-A)]\theta - K_2A$ versus N for experimental data taken at a fixed load and stroke should be a straight line of slope F_6 passing through origin. Such a plot is shown in Figure 4 with lines corresponding to three different values of F_6.

In obtaining the data for any one line the solenoid is operated at full voltage with a certain load and stroke. For each of several values of A, tests are run at different cycling speeds until steady temperatures are reached. Then with K_2 definitely known from the continuous heat run, and K_1 tentatively assumed equal to zero, y may be worked out and plotted against N.

If zero is the correct value of K_1, the data for different values of A will all have the same slope; if $K_1 = 0$ is too low, the points for the lesser values of A will describe a line of slightly lower slope than the points for the higher values. At this juncture, various values of K_1 should be tried (usually less than 0.25) until divergence in the data is eliminated. Once determined, this same value of K_1 should cause data taken at other strokes and loads to correlate equally satisfactorily.

Tests made at convenient fractions of full load are plotted in the manner of Figure 4. Thus F_6 becomes known, as a function of stroke, for each of several loads. Figure 5 shows the final arrangement of the data for an all-weight load.

Adaptation to Mixed Weight and Spring Loads

With mixed weight and spring loads, the F_6 values will be less than those of Figure 4. To determine them accurately, it would be necessary to run a complete set of tests for each of several spring-weight combinations, but a good approximation can be deduced from the tests with all-weight loads as follows: Equation 4 shows the closing time to be proportional to $\sqrt{W}$ for a given total load and stroke. This, in turn, makes H_e and H_t vary substantially as $\sqrt{W}$. Following this through equations 10 and 11 to 15, it will be seen that F_6 is also proportional to $\sqrt{W}$. Thus for a mixed load, the F_6 for an all-weight load of the same magnitude should be multiplied by $\sqrt{W/L}$ to obtain a proper value for use in equation 15.

Advantages of the Method

It has been shown that K_2 is determined from a single preliminary heat run and

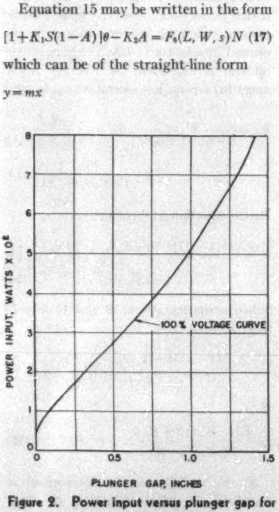

Figure 2. Power input versus plunger gap for a typical a-c solenoid

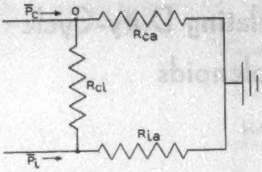

Figure 3. Thermal circuit of an a-c solenoid

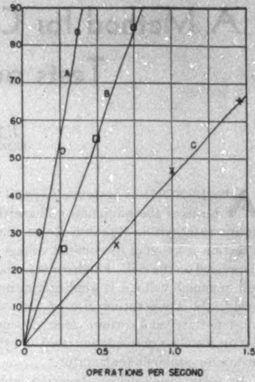

Figure 4. Data plot for determining the load-stroke function, F_6, of an a-c solenoid

A—100 per cent stroke
B—70 per cent stroke
C—40 per cent stroke
100 per cent rated load

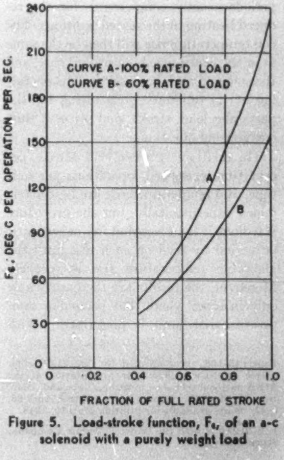

Figure 5. Load-stroke function, F_6, of an a-c solenoid with a purely weight load

that a proper K_1 may be found from a few duty-cycle runs taken at various values of A and N while holding the load and stroke constant. Thereafter, one duty-cycle heat run will suffice to determine F_s for any one combination of L, W, and S, since a single experimental point and the origin can locate a line in Figure 4. However, several points are desirable for each line in Figure 4 to minimize experimental error. Even so, the number of tests required to completely cover all possible combinations of L, W, S, A, and N is greatly reduced by the use of this correlation method. Furthermore, experimental errors are readily detected, and the extrapolation of the data beyond the scope of the investigation is made more certain.

Applications Other Than A-C Solenoids

D-c solenoids in which a relatively large current is allowed to flow for a brief time during the pickup period are often used to increase the work output for a given size. As the plunger is seated, a cutout switch or relay operates to insert resistance and reduce the current to a suitable continuous value. Since, as with a-c solenoids, it is difficult to compute the energy input during the closing period, the correlation method of this paper is useful in organizing the duty-cycle-heating data.

In general, the method may be applied to any electrical device subjected to regular heating cycles so long as the cycling period is substantially less than the thermal time constant of the device. It is particularly useful when there is a complex transient heating effect in each cycle.

Appendix

Symbols

x = instantaneous plunger gap, inches
s = total plunger stroke, inches
S = fraction of full rated stroke
f = instantaneous magnetic force parallel to plunger travel, pounds
L = total load opposing magnetic force, pounds
W = equivalent weight of moving parts, pounds*
v = velocity of plunger, inches per second
g = acceleration of gravity, inches per second squared
E = kinetic energy, inch-pounds
t = time from start of plunger motion, seconds
T = total plunger motion time, seconds
P = power input, watts
H = energy input during closing period, watt-seconds
A = fraction of time solenoid is energized
N = operations per second
R = heat flow resistance, degrees C per watt
θ = coil temperature rise above ambient, degrees C

K, k denote constants.

F denotes a function, the independent variables of which are given in the parentheses following the symbol.

Bars above quantities indicate average values.

Prime superscripts on R's refer to thermal resistances existing when the plunger is seated.

SUBSCRIPTS

x refers to instantaneous position of plunger.
s refers to sealed position of plunger.
c refers to coil.
i refers to iron.
ca refers to coil to ambient air.
ci refers to coil to iron.
ia refers to iron to ambient air.

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* An element of mass $\Delta M'$ in an associated mechanism may be represented by an equivalent mass ΔM attached directly to the plunger if

$$\Delta M = \Delta M' \left(\frac{v'}{v}\right)^2$$

where v' is the velocity of $\Delta M'$ when the plunger velocity is v.

A Simple Method for the Determination of Bushing-Current-Transformer Characteristics

S. D. MORETON
ASSOCIATE AIEE

Synopsis: Now that excitation characteristics are being made available to the application engineer, it has become increasingly important that a simple accurate calculation method be used to determine the errors. "Inphase addition" has all the advantages of simplicity, and, under careful analysis, it is apparent that over the normal ranges involved, the more complicated procedure of vector addition is not warranted. This simple method has been extended to cover complete curve drawing from only one calculation by a graphical process.

IN the past, characteristics of bushing current transformers have been presented in the form of curves of true ratio versus primary current for a variety of different burdens and power factors. Such curves were obtained by tests on the actual transformers and were the only data available to the application engineer. The art of calculating current-transformer characteristics from excitation curve data has been known for some time;[1,2] only recently, however, has popular attention been focused upon it as a method[3,4] for determining the transformer errors of a particular application. With this in mind, the American Standards Association standards have been written to foster a calculation method,[5] although no specific method was decided upon.

It has been realized that such a radical change in application procedure might well result in difficulties unless the method was reasonably easy to use, and it has, therefore, been suggested that the errors introduced by the relatively simple method of inphase addition be investigated more fully. The conclusion was reached that this method was the practical answer. These errors are best shown graphically, as in Figure 1, which gives a comparison of calculated results for three of the ratios of a 600/5 multiratio bushing current transformer with a two-ohm burden at 90 per cent power factor and 50 per cent power factor, as well as inphase addition. It is immediately apparent that the errors introduced by disregarding the angular difference between the secondary current and the exciting current are small for the normal range of relay burden power factors.

Conclusion

When all the variable factors are taken into consideration, as discussed under "Accuracy Factors," it is evident that the errors introduced by inphase addition are a relatively small part of the total pos-

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The author wishes to acknowledge the assistance of his associates for their constructive criticism, in particular, W. F. Skeats for his comments on the mathematical proof of the graphical method and his suggestions for a simplified presentation of the primary current.

sible variation, and thus, from the standpoint of the application engineer, inphase addition is justifiable for the normal range of burden power factors. This is particularly evident by a comparison of Figures 1 and 8.

Calculating Theory

Basically, the relationship between secondary ampere turns, exciting ampere turns, and primary ampere turns is a vectorial one, where the calculations are based on the equivalent circuit of Figure 2. The most time-consuming part of the calculations is the vector addition, whether it is performed mathematically, graphically, or by vector charts. By considering this vector relationship as a scalar relationship much of the work is avoided.

Calculating Procedure for Determining Ratio Correction Factor

The calculation procedure for inphase addition is based on the equivalent circuit of Figure 3, with steps and formulas as follows:

1. Compute the necessary secondary voltage to force the desired secondary current through the total burden, as: $E=IZ$.
2. Find the secondary exciting current required to produce this secondary voltage from the excitation curve, see Figure 4.
3. Add this secondary exciting current arithmetically to the secondary current and multiply by the number of secondary turns to obtain the primary current, as: $I_p=N(I_e+I_s)$.

The ratio correction factor (RCF) may be defined as the ratio of primary ampere turns to secondary ampere turns. In a bushing current transformer, the primary winding has one turn, therefore:

$$RCF=\frac{I_p}{NI_s} \qquad (4)$$

$$RCF=\frac{N(I_e+I_s)}{NI_s}=1+I_e/I_s \qquad (5)$$

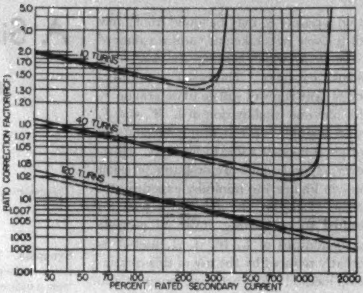
and this may be found without passing through step 3.

Calculating Procedure for Determining Phase Angle β

In those applications that involve the comparison of two currents or a current and a voltage, it might be desirable to determine the phase-angle error of the current transformer. It may be found by an extension of the method of inphase addition and by the use of the phase-angle chart, Figure 5. Excitation characteristics are required that give not only the total exciting current but also the lagging excitation phase angle between the exciting current and the secondary voltage. Figure 6 is typical of this type data and is presented on a per-turn basis.

Figure 1. Calculated ratio characteristics; two ohm burden

——— Inphase addition
— · — 90 per cent power factor
— — — 50 per cent power factor



The chart is entered at a per-unit exciting current, I_e/I_s, of equation 5, which could be rewritten as:

$$RCF-1=I_e/I_s \qquad (6)$$

The phase-angle error is read off directly, using the curve that corresponds to the "difference angle," equal to the burden phase angle minus the excitation phase angle $(\theta_s-\alpha_e)$. The phase-angle error is positive when the difference angle is positive.

Accuracy Factors

Factors which influence the accuracy of the calculated results are variations of excitation characteristics of the current transformer from the typical curve values and variations in the magnitude of the burden.

The excitation requirements of electrical iron are not only a function of the size and shape of the core but also a function of the amount and kind of impurities, the type of rolling process used, the annealing temperature and subsequent cooling, as well as the previous magnetic history. Some of these factors are very difficult to control, with the result that the final characteristic, as published by the manufacturer in the form of a typical excitation curve, is, of necessity, only an average curve. All of the factors lumped together and controlled with the usual manufacturing tolerances can well result in a variation as great as plus or minus 50 per cent in the total exciting current at any one particular flux density for various lots of iron. By more rigid control and selective choice of the iron by sampling processes, this can be reduced to plus or minus 25 per cent; see Figure 7 for a typical band curve. Even this variation would seem to be intolerable, were it not for the fact that this represents a variation of the ratio error of the transformer and not of the true ratio. In the majority of applications, where the maximum desirable RCF is less than 1.1, and where the transformer is operating below the knee of the excitation curve, the maximum possible variation will be plus or minus 25 per cent of this ten per cent error, or plus or minus 2.5 per cent in the RCF; see Figure 8 for a typical band curve.

The use of a calculating method for the determination of bushing-current-transformer characteristics requires a knowledge of the magnitude of the burden which, in most instances, consists of relays that have iron cores, and thus have

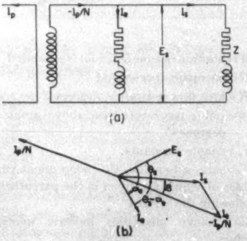


Figure 2

(a). Exact equivalent circuit
(b). Vector diagram of (a)

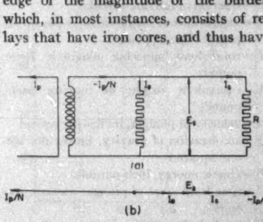


Figure 3

(a). Approximate equivalent circuit
(b). Scalar diagram of (a)

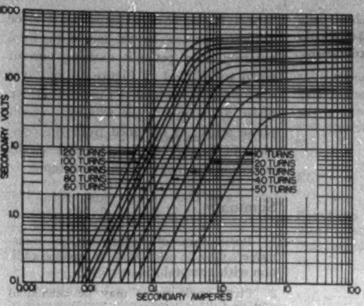


Figure 4 (left). Secondary excitation characteristic, 600/5 multiratio bushing current transformer

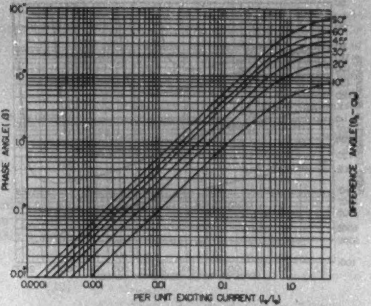


Figure 5 (right). Phase-angle error chart

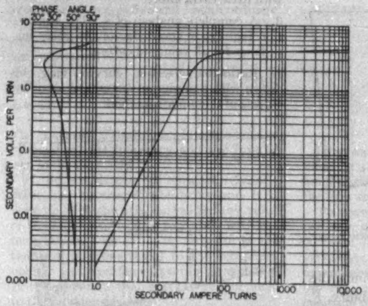


Figure 6 (left). Excitation characteristic

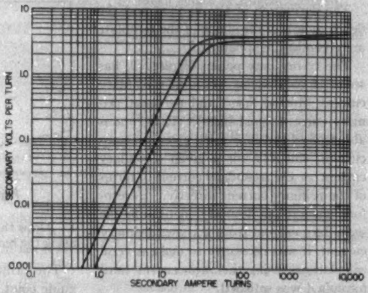


Figure 7 (right). Band-excitation characteristic

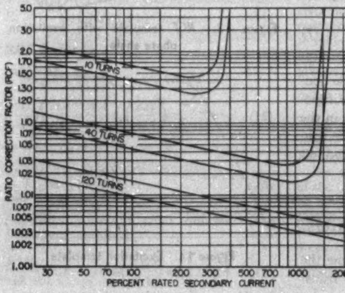


Figure 8 (left). Band-ratio characteristic on three ratios of a 600/5 multiratio transformer

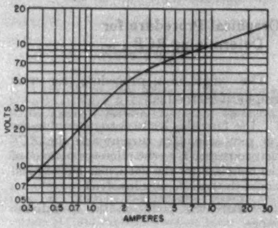


Figure 9 (right). Burden characteristic of time overcurrent relay on 1.5 ampere tap

inherently nonlinear impedances. Fortunately, the majority of relays have magnetic circuits which contain series air gaps so that the main part of the exciting current is required to establish the air-gap flux, and thus, large variations in exciting current of the iron core present only small variations in burden between units. Therefore, relays have relatively constant burden characteristics up to the point of iron saturation. Beyond this point, the reactance component of the burden is inversely proportional, approximately, to the current so that the current transformer will hold up to a higher value of primary current if burden saturation occurs before current transformer saturation. Typical burden values of relays are published by the manufacturer and are usually measured at the pickup current of the relay. If they are of the type that depends upon saturation to obtain its characteristics, such as a time overcurrent relay, the application engineer would require burden characteristics, such as shown in Figure 9, to make a thorough analysis. Thus, it seems inevitable that as the art of calculation becomes more widespread, the application engineer will require more complete information; see Figure 10 for a calculated *RCF* curve, based on composite characteristics of the overcurrent relay of Figure 9 and the transformer shown in Figure 4.

Graphical Method

When the ratio errors are desired for a range of primary current values for a multiratio transformer, the labor at calculation becomes tremendous, even with the simplification of inphase addition. To reduce this work, a graphical method has been developed, which eliminates the point-by-point method of curve drawing and permits a complete characteristic to be drawn from one calculation by the use of a master template, Figure 11.

Note that the shape of this template is a function of the type of iron and the size of the log-log co-ordinate paper only.

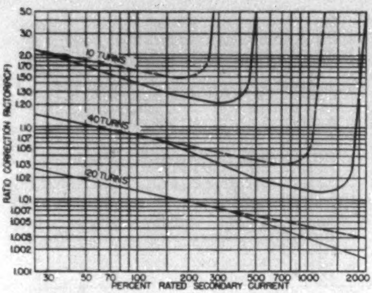


Figure 10. Calculated ratio characteristic, 600/5 multiratio bushing current transformer

– – – – 2.5 ohm burden
——— time overcurrent relay burden on 1.5 ampere tap (2.5 ohms at pickup)

The derivation of the method and the development of the shape of the template are given in the appendix.

The co-ordinate paper, Figure 12, has scales of *RCF* versus per cent secondary current (dotted lines) and per cent primary current (solid lines). Thus, one is able to find the *RCF* for a given primary current, the *RCF* for a given secondary current, or the secondary current in terms of the primary current directly. Heretofore, when it was desired to determine the secondary current from a known primary current, it was necessary to make several calculations, using the ratio curves until formula 4 was satisfied.

Graphical Procedure for Determining RCF

The calculation procedure for this graphical method is:

1. Use formulas 7 and 8 to determine the per cent secondary current and the *RCF* that corresponds to the reference point *P* of the template for the particular burden and tap of the transformer involved.
2. Locate this point on the co-ordinate paper.
3. Set the template with its reference point on the corresponding point of the co-ordinate paper and line it up horizontally.

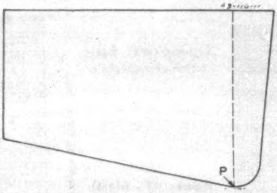


Figure 11. *RCF* template

4. Draw the complete characteristic curve, using the template as a guide.

The formulas required to determine this guide point of maximum permeability, *P*, are:

$$\%I_s = C(N/Z) \qquad (7)$$

$$RCF = 1 + D/(N\%I_s) \qquad (8)$$

where *C* and *D* are constants of the particular transformer involved and are functions of the shape and type of iron in the core, as well as the frequency. The constants *C* and *D* may be determined directly from the excitation curve.

Graphical Procedure for Determining Phase-Angle Errors

Where it is desirable to determine the phase-angle error of the current transformer, it may be obtained as a part of the graphical process by the use of Figure 5 entering at a per-unit exciting current equal to $RCF-1$ of Figure 12 and at a difference angle equal to the total burden phase angle minus the exciting current phase angle, as obtained from the template of Figure 13 when located on the co-ordinate paper according to step 3.

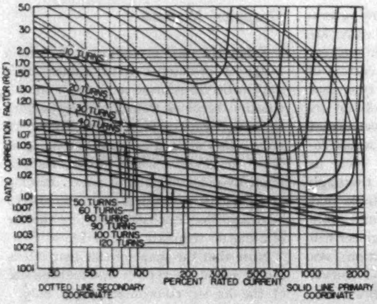


Figure 12. Calculated ratio characteristic, 600/5 multiratio bushing current transformer

Summary

1. Ratio errors of bushing current transformers may be calculated by inphase addition with acceptable accuracy.
2. Phase-angle errors may be calculated with little extra effort.
3. A complete analysis of a relay application requires a knowledge of the relay impedance for all values of current.
4. Ratio-characteristic curves may be drawn with a minimum amount of labor by the use of a template method.

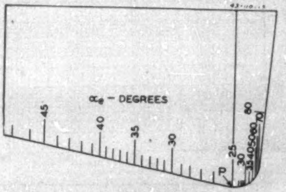


Figure 13. *RCF* template with excitation phase angle

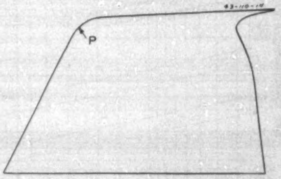


Figure 14. Excitation template

5. The use of special co-ordinate paper presents an ideal way to give ratio characteristics.

Appendix

Graphical Theory

Excitation Curve

Fundamentally the theory is based on the fact that the a-c excitation characteristics of electrical iron may be expressed mathematically as:

$$NI_e/l = f_1(E/NA, f) = f_1\left(\frac{I_sZ}{NA}, f\right) \qquad (9)$$

or

$$NI_e/l = f_2\left(\frac{E}{NA}\right) = f_2\left(\frac{I_sZ}{NA}\right) \qquad (10)$$

for any one particular frequency. This equation is in the form of:

$$P_y = f(Q_x) \tag{11}$$

and hence, if plotted on log-log paper with the exciting current as abscissa and the secondary voltage as ordinate, it represents a family of curves all of which have the same shape. Consequently one template, Figure 14, whose shape is determined solely by the magnetic properties of the iron and the co-ordinate paper can be used as a guide for drawing all curves of the family if one reference point, P, is marked on the template and is specified for each curve. It is convenient to select this reference point at an excitation corresponding to maximum permeability of the iron, which is the point on the excitation characteristic that is tangent to a 45-degree line and has an ordinate value of J and an abscissa value of K. The co-ordinates of the reference point on the log-log paper are:

$$E = JNA \tag{12}$$

$$I_e = Kl/N \tag{13}$$

The constants J and K are functions of the primary-current frequency and the type of iron in the core only.

If the template is set with its reference point on the co-ordinate point and it is lined up with the axis, curves for any value of secondary turns may be drawn with the template as a guide. The ordinate of the template is proportional to the secondary voltage and from an inspection of equation 10 it is apparent that it is also proportional to the flux density since

$$E/NA = 4.44 B_{\text{max.}} f \times 10^{-8} \tag{14}$$

RCF Curves

Formula 10 may be expressed as:

$$\left(\frac{NI_e}{l}\right)\frac{NA}{ZI_s} = \frac{NA}{ZI_s}\, f_2\left(\frac{ZI_s}{NA}\right) \tag{15}$$

or

$$\frac{N^2A}{lZ}\,\frac{I_e}{I_s} = f_3\left(\frac{ZI_s}{NA}\right) \tag{16}$$

Substituting for I_e/I_s from equation 6 the following is obtained:

$$\frac{N^2A}{lZ}\,(RCF-1) = f_3\left(\frac{ZI_s}{NA}\right) = f_3\left(\frac{E}{NA}\right) \tag{17}$$

Equation 17 is also of the form of equation 11 and consequently represents a family of curves, when drawn on log-log paper, having scales of $(RCF-1)$ versus I_s, all of which can be drawn using a single template as a guide by setting the reference point, P, of the template, Figure 11, on the co-ordinates given by equations 12 and 13. It will be found more convenient to use equation 12 combined with equation 1 as:

$$I_s = \frac{JNA}{Z} \tag{18}$$

and equation 13 combined with equation 6 as:

$$RCF-1 = \frac{I_e}{I_s} = \frac{Kl}{NI_s} \tag{19}$$

Equations 18 and 19 can be converted to read in terms of per cent secondary current, $\%I_s$ (based on five amperes as 100 per cent) as:

$$\%I_s = \frac{20JNA}{Z} = C\,\frac{N}{Z} \tag{20}$$

$$RCF-1 = \frac{20Kl}{N\%I_s} = D/N\%I_s \tag{21}$$

where

$$C = 20JA \tag{22}$$

$$D = 20Kl \tag{23}$$

Here again it should be noted that the shape of the template of equation 17 is a function solely of the magnetic properties of the iron and the co-ordinate paper.

To convert this curve of $(RCF-1)$ versus per cent secondary current, the dotted co-ordinate lines of Figure 12, to $(RCF-1)$ versus per cent primary current it has been found most convenient to operate on the per cent secondary current scale itself by equation 6, where I_e/I_s represents the ratio error in per-unit values. Thus every co-ordinate value of $(RCF-1)$ and per cent secondary current has a corresponding co-ordinate value of $(RCF-1)$ and per cent primary current according to the equation:

$$\%I_p = 100(RCF-1) + \%I_s \tag{24}$$

which gives the resulting per cent primary current scale of solid co-ordinate lines in Figure 12.

For convenience in reading the numbers of the $(RCF-1)$ or ordinate scale they may be written as $(RCF-1)+1$ or RCF without changing either the shape of the template or the shape of the co-ordinates since equation 4 can be expressed as:

$$RCF = \%I_p/\%I_s \tag{25}$$

which is equivalent to equation 24.

PHASE-ANGLE CURVES

The abscissa of the RCF template is proportional to secondary voltage by equation 17, it is therefore proportional to the flux density by equation 14, and thus it is also proportional to the excitation phase angle of the iron, see Figure 6. Therefore the RCF template may be marked off in degrees of phase angle of the exciting current as in Figure 13, where the markings on the template are a function of the type of iron and the frequency only. The phase-angle error can be determined most easily by the use of a simplification of the vector charts following the procedure outlined earlier.

Glossary

E—Induced secondary voltage.
N—Number of secondary turns.
A—Cross-sectional area of magnetic circuit.
l—Mean length of magnetic circuit.
I_s—Secondary current.
$\%I_s$—Secondary current in per cent based on five amperes ($\%I_s = 20I_s$).
Z—Total secondary burden.
I_e—Excitation current based on secondary.
I_p—Primary current.
$\%I_p$—Primary current in per cent based on tap and five amperes in secondary circuit.
θ_s—Phase angle of total secondary burden Z.
α_e—Phase angle of exciting current.
β—Phase angle error of current transformer.
f—Frequency of supply in cycles per second.
$B_{\text{max.}}$—Crest flux density in the core.

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Report on Application of Lightning Protective Devices in Wartime

AIEE LIGHTNING ARRESTER SUBCOMMITTEE

Preface: The present war emergency requires that the maximum use be made of existing equipment and systems and that a minimum of critical material be used for new equipment.

This publication and other guides and reports in this series have been prepared for the information of users during the war emergency. Upon termination of the war emergency they will be reconsidered by the standards committee and the committees which prepared them, and will be approved, revised for normal use, or rescinded.

This procedure is being followed in preference to the preparation of special emergency standards which might involve redesigning and drastic changes in manufacturing practices. These guides will accomplish the maximum conservation of critical materials, since they provide for the maximum use of existing equipment and systems, as well as new equipment, without changing the fundamental basis on which the present standards have been prepared.

I. Purpose and Scope

THE object of this report is to show how critical materials can be conserved in the application and use of lightning arresters to meet the wartime requirements of our country. This objective may be accomplished in two ways:

1. By making greatest use of protective devices. This refers to rebuilding of old arresters and applying new arresters close to their maximum capability, thereby reducing the content of critical materials in the arresters themselves.
2. By proper use and maintenance of protective devices, failures from lightning of important machines and equipment which would require large amounts of critical materials and man-hours to replace can be minimized, thereby maintaining a high degree of service to important war loads.

It is not to be implied that the suggestions contained in this report are the only means worth-while in attempting to save critical materials, nor that they are recommended as expedient in all cases. It should be clearly understood that local conditions, service requirements, and many other such factors may have to be considered before reaching a decision as to whether or not it is advisable to use one or more of the proposed schemes.

The importance of a check up and study on existing systems of overvoltage protection against lightning to obtain better protection cannot be overemphasized. This type of study applies to both new and existing installations.

II. Suggestions for Saving Critical Material

The suggestions presented and discussed hereinafter are backed in most cases by successful operating experience. The experience record, however, does not pretend to be all inclusive as it has not been expedient to survey thoroughly the entire field throughout the country to obtain and analyze detailed records. Where diverse views of committee members have existed, this situation is pointed out.

1. Applying arresters on basis of voltage rating.
2. Rebuilding and revamping old arresters.
3. Short connections between arresters and equipment.
4. Line-type arresters in place of station-type.
5. Protection of equipment not already protected.
6. Protective devices other than arresters.
7. Shielding of stations.
8. Tests and maintenance of arresters.

The eight suggestions are discussed in order.

1. Applying Arresters on Basis of Voltage Rating

It is generally recognized that a lightning arrester has a maximum normal frequency (60-cycle) voltage rating just as any other piece of equipment like a transformer, breaker, or disconnecting switch. Although tolerances of overvoltage for these latter types of equipment are specified in standards, they are undoubtedly often exceeded in service. The arrester differs from other types of apparatus in one respect, namely, that its maximum voltage rating should not be exceeded under any operating condition; otherwise, the arrester may be severely damaged. The use of an arrester on a system where the line-to-ground voltage exceeds the arrester rating, even for a few cycles, may well be viewed with concern. If arrester failure occurs because of abnormal system overvoltage, it is very likely that the arrester has been misapplied.

In general, arresters listed in the manufacturers' catalogs for nongrounded neutral systems have a maximum rating of approximately five per cent above line-to-line system voltage and those applied to effectively grounded neutral systems have a factor of safety of approximately 40 per cent above system leg voltage (80 per cent of the line-to-line voltage).

In cases where the ungrounded neutral arrester has been used on a grounded neutral system, a saving may be effected by using the grounded neutral arrester, provided the system neutral is grounded effectively. Also, there are certain cases where an accurate calculation of the system overvoltage on grounded neutral systems has resulted in application of arresters having a lower rating than the general manufacturer's guide for the selection of arresters indicates.

For example, on one particular system rated 132-kv grounded neutral, an arrester having a maximum line-to-ground rating of 109 kv has been used successfully. In this particular case the use of a lower arrester rating has resulted in the use of power equipment with lower insulation (115-kv class insulation instead of 138-kv class which would normally have been required). In this connection, it should be realized that the lower the rating of arrester used, the greater the degree of lightning protection to the equipment, but on the other hand, there is less factor of safety in the arrester.

2. Rebuilding and Revamping Old Arresters

Another factor to be considered is the savings of materials possible by rebuilding or revamping old type arresters. Several companies have reported that they have been doing work along these lines for some time. In the case of oxide-film arresters which were extensively used in the past, it is general practice in a number of companies to reduce the number of

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This report was prepared by the AIEE lightning arrester subcommittee of the committee on protective devices for the purpose of making essential information immediately available to war industries, thus furthering the conservation of valuable material for the war emergency. It is educational and in no way mandatory. It is not intended as a "Standard," and has not been formally approved by the standards committee or the board of directors.

cells in existing arresters which, in effect, means reducing the arrester rating to match more nearly anticipated normal frequency overvoltage conditions of the circuit where they are used. The principle involved has been discussed. Likewise, the changing of four-leg oxide-film arresters to the three-leg type has been an accepted practice for some time with outstanding success from the viewpoint of added protection and absence of arrester failure. Of course, in such cases the 60-cycle maximum rating of the remaining three legs must be checked against the system overvoltage under fault conditions. In some cases where the three-leg 60-cycle rating was too low, part of the fourth leg was left in service to supply the deficiency.

Successful rebuilding of mica-spaced and open-gap auto-valve arresters has also been reported. The rebuilding of old arresters supplies added protection to equipment where they are installed by lowering the protected level and also may make available some of the material removed for other arrester locations or spare parts. The rebuilding of even obsolete arresters may in some cases be warranted, if new arresters are not available. The added protection of rebuilding obsolete arresters may in some cases be justified, although the protected level may be higher than would ordinarily be desired.

3. Short Connections Between Arresters and Equipment

The desirability of having short connections between the arrester and the equipment it is to protect has been discussed extensively in technical literature in the recent past. It probably does not require further amplification here. It is well known by theory and test that while the arrester may hold a given voltage at its own terminals, this voltage may be quite different at the terminals of protected equipment if long leads intervene to the lightning arrester. Even in distances of 25 to 50 feet, on steep front surges, it is generally recognized that the voltage at the equipment may be increased appreciably by virtue of the lead length.

The importance of considering this factor is evident for two reasons: first, less protection is supplied to equipment already in service where lead length is considerable. This is, of course, equally true of new installations of protective equipment. Second, there may be some saved or salvaged copper by proper consideration of this lead-length feature.

Another saving which can be effected in copper conductor is that of using substantial structural work in stations such as columns, trusses, and so forth, for part of the lightning discharge path to ground. In many cases in the past, common practice has been to extend separate grounding leads from the ground end of the arrester to ground, even though the main structural part of the station was electrically tied into the grounding system. This extra refinement, it is felt, under wartime emergency conditions, is not necessary and probably results in very little, if any, increased protection by adding the ground lead from the arrester to the equipment where a suitable and substantial steel structure or network already exists.

4. Line-Type Arresters in Place of Station-Type

In the installation of new equipment, it often is possible to maintain sufficient margin between the distribution-type-arrester characteristic and the insulation strength of present-day equipment. Where old equipment only is involved, it is doubtful whether the substitution is worth while, and in fact, it may often be found that the protective level supplied by even modern station arresters is unduly high so that the use of line-type arresters cannot even be considered.

Several successful applications of line-type arresters on new equipment have been made with satisfactory operating experience. It should be pointed out, however, that many of the applications coming under this classification result more often in economies than in a material reduction in the use of critical material or in saving of manpower.

5. Protection of Equipment Not Already Protected

Secondary savings considerably greater than cost of applying lightning arrester protection are usually possible in cases where equipment is not now protected. It is not the intention of the committee to recommend that all equipment not now protected should be immediately supplied with arrester protection, but there are undoubtedly existing installations and new ones being made where the expenditure of a small amount of critical material in lightning arresters may produce large secondary savings by preventing failures of important equipment. This feature is pointed out as one worth considering and studying in individual applications. The decision reached will be influenced by many factors such as exposure of equipment to lightning, its importance in service, the possibility of replacement, cost and time of replacement, and similar factors.

6. Protective Devices Other Than Arresters

While the lightning arrester has been mentioned frequently in this report, the general features of application of overvoltage protective devices considered here apply in most cases to other protective equipment such as rod gaps, shielded gaps, De-ion gaps, and similar devices. Where to use one in preference to another is entirely outside the scope of this report to recommend. However, it is suggested that careful consideration be given in the application of any protective devices to insure that a suitable margin between the voltage level held and the strength of the equipment be maintained. It should be pointed out, however, that all of these devices mentioned have been reported by some users as giving successful and satisfactory service within their scope of application.

7. Shielding of Stations

Where a large amount of equipment is placed in one location such as at major stations, the general practice in recent years has been to supply adequate shielding from lightning by the use of elevated ground wires or ground networks, and even in some cases, by individual lightning rods shielding equipment. While such protection requires the use of some critical material, it may well be considered from the point of view of spending a relatively small amount of critical material for shielding to prevent damage to equipment having a much larger amount, the replacement of which would probably necessitate an expenditure of critical material and manpower.

8. Tests and Maintenance of Arresters

In so far as the arrester itself is concerned, it is suggested that special consideration be given to testing arresters wherever possible and feasible, to insure that they are in the best of condition to perform their expected functions. Several years ago recommendations were made by the AIEE lightning arrester subcommittee on methods of testing some types of arresters. Since that time, a large number of arresters have been tested, and other methods of test or refinements have been used. It cannot be too strongly urged that a special attempt should be made to keep the arresters and their grounding connections in the best possible condition.

Summary

The features in the preceding paragraphs have been presented and discussed with the thought in mind that the information may be helpful to those who have the lightning or overvoltage protection problem to deal with. It is not intended that all of these suggestions be applied without careful study and thought; in fact, unless such a detailed study is made of systems now in use or proposed, the full benefits of the applications suggested will most likely not be obtained. It is hoped that this paper will stimulate constructive thought on the part of those who have the responsibility of designing and operating protective systems already installed or to be installed in the future.

It should be kept in mind that in applying some of these *suggestions*, the failure rate on the protective devices themselves may increase, but it is believed that if the conditions of each situation are considered and action taken on an engineering basis, the net over-all effect will be of considerable benefit to the war effort.

Interim Report on Emergency Overloads on Overhead Conductors

AIEE WORKING GROUP ON TOWERS, POLES, AND CONDUCTORS

Preface: The present war emergency requires that the maximum use be made of existing equipment and systems and that a minimum of critical material be used for new equipment.

This publication, and other guides and reports in this series, have been prepared for the information of users during the war emergency. Upon termination of the war emergency they will be reconsidered by the standards committee and the committees which prepared them, and will be approved, revised for normal use, or rescinded.

This procedure is being followed in preference to the preparation of special emergency standards which might involve redesigning and drastic changes in manufacturing practices. These guides will accomplish the maximum conservation of critical materials, since they provide for the maximum use of existing equipment and systems, as well as new equipment without changing the fundamental basis on which the present standards have been prepared.

PRESENT restrictions in the use of materials and sharp increases in loads may make it necessary to load existing lines to the thermal capacity of the conductors. The purpose of this report is to call attention to the limiting factors and the information available from which to evaluate the limits.

The problem divides itself into two parts, the temperature at which the conductor may be operated without impairing its strength or destroying its covering, and the temperature the conductor will attain for a given current density under any given conditions. Neither of these can be calculated with any degree of precision, and the probable error must be duly considered in establishing emergency ratings for any conductors.

I. Permissible Temperatures

A. Loss of Strength Caused By Annealing

The annealing point of the softer metals hardened by cold working, is not very definite. Variations are introduced by impurities and by the amount and rate of cold working. Time is also an important factor, and it is not definitely known if effects of intermittent heating are directly additive or not.

Aluminum. G. W. Stickley gives the following figures for annealing of hard drawn aluminum. "In tests of wire heated one hour at 100 degrees centigrade the strength was decreased only two per cent, and after heating at this temperature for as long as four years the decrease was only 15 per cent. In similar tests of wire heated one hour at 150 degrees centigrade the strength was decreased only about five per cent. It should be noted that although overloads might have a small effect in decreasing the strength of aluminum conductors, they would have still less effect upon the strength of steel reinforced aluminum cable. Tests of the high-strength steel wire used as the core of ACSR have shown that heating at temperatures as high as 260 degrees centigrade for two months actually increases the strength of the wire slightly."

Howell and Paul[1] state that commercially pure aluminum in the hard drawn temper, designated 2*S-H*, is not materially softened by prolonged exposure to temperatures in the region of 200 degrees Fahrenheit.

Copper. Many data on annealing of copper have been published, but the values given vary over an extreme range. If the conductor is kept at 171 degrees centigrade, Anaconda claims ten per cent softening in one hour, while General Cable Company finds that it requires 6.7 hours to produce ten per cent softening at 175 degrees centigrade. H. R. Stewart states results from actual tests as follows: A number two copper wire held at 215 degrees centigrade and a number 1/0 held at 160 degrees centigrade for four hours each lost about five per cent of its strength. Kidder and Woodward[3] find that copper may be held at 180 degrees centigrade for eight hours on six consecutive days (total 44 hours) with 16 per cent loss in strength, and when heated to 200 degrees centigrade for the same length of time it lost 35 per cent. From this they conclude that the danger zone lies about 175 degrees centigrade for short duration.

A very complete investigation is presented by Myron Zucker.[4] It shows that the annealing temperature is affected by the amount of cold working and the purity of the copper. The harder the copper, the lower the annealing temperature. A small percentage of silver will raise the annealing temperature considerably, while oxygen will lower it. He summarized his findings in curve form, giving a spread of from 40 to 1,200 hours at 100 degrees centigrade for copper in the electrolytic range. He also states that an X-ray diffraction test can be used to determine the annealing characteristics of copper without destroying the sample. The beginning of recrystallization indicates a loss of five per cent of the tensile strength of the wire.

In most cases, annealing tests on copper

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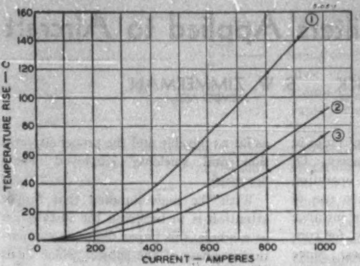

Figure 1. **Temperature rise of 795,000-circular mil steel-reinforced aluminum cable, new**

Tests made at 60 cycles indoors in still air on unused cable from stock. Cable not weathered but no longer bright

1. Cable in Ohio Brass number 77,898 suspension clamp (without armor rods)
2. Cable with armor rods in Ohio Brass number 77,613 suspension clamp
3. Bare conductor, with neither armor rods nor suspension clamp

have been designed to determine the length of time required for complete annealing at different temperatures. The difficulty in exterpolating these data to a time–temperature combination which would give a small percentage of annealing may be responsible for the wide discrepancy in values.

Before the question of annealing can be definitely answered, information on the effect of the following items must be known:

Various impurities commonly found in commercial metals.

The cold working required to make the metal into wires or cables.

The stresses to which the conductor normally is subjected.

Intermittent heating and possible recovery between heating periods.

Copperweld. The strength of a copperweld conductor is predominated by the steel content, and it is not likely to be loaded to a point where its mechanical strength is impaired. Rolf Selquist suggested that the current-carrying capacity of copperweld be limited to 70 per cent of that of a copper conductor of the same cross section.

B. Protection of Wire Coverings

Kidder and Woodward[3] state that with a copper temperature of 175 degrees centigrade the outside surface of the covering will be 130 degrees centigrade, which is well below the impregnating temperature, and 80 degrees centigrade, or just below the standard acceptance specification with a copper temperature of 100 degrees centigrade. Myron Zucker[4] stated that recent tests on *URC* covering indicate that it can be safely operated for ten hours at 100 degrees centigrade. The compound used in older types of weatherproofing gets soft and tacky at temperatures as low as 60 degrees centigrade and the *URC* covering begins to soften at 85 degrees centigrade. It is reasonable to assume that such temperatures are going to accelerate the deterioration of the covering. In many cases it may well be that this would be of little importance as compared to the additional capacity gained.

C. Preservation of Clearances

Operating conductors at elevated temperatures will mean considerable increases in sag. Where maintenance of clearance is important, these increases should be carefully checked. Even small amounts of annealing may result in permanent stretch of the conductors after a heavy ice load, and it may become necessary to resag the line.

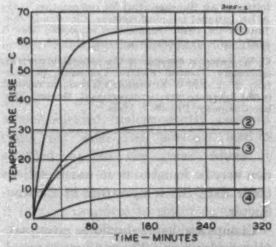

Figure 2. **Rate of temperature rise of 795,000-circular mil steel-reinforced aluminum cable, new**

Tests made at 60 cycles at 550 amperes, indoors in still air on unused cable from stock. Cable not weathered but no longer bright

1. Cable in Ohio Brass number 77,898 suspension clamp (without armor rods)
2. Cable with armor rods, in Ohio Brass number 77,613 suspension clamp
3. Bare conductor, with neither armor rods nor suspension clamp
4. Cable with armor rods, but without suspension clamp

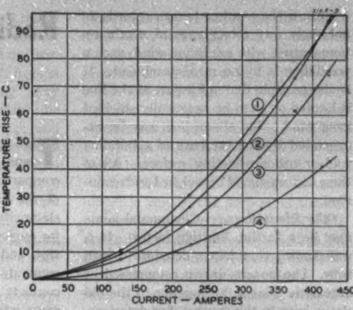

Figure 3. **Temperature rise of badly oxidized seven-strand 2/0 copper cable**

Tests made at 60 cycles indoors in still air. Conductor badly oxidized from many years of service

1. Cable in Ohio Brass number 70,757 suspension clamp and number 70,759 keeper (horns removed)
2. Thermocouple between core strand and outside layer
3. Thermocouple on surface of cable
4. Memco number 64 twisted sleeve

II. Temperatures That the Conductor May Attain

A. Temperature Rise Versus Current

Several references covering the heating of conductors from electric currents are available in the engineering literature.[3,5–9] The formulas given by Schurig and Frick seem to give consistent results. Kidder and Woodward[3] give a detailed description of their method of its application.

B. Weather Conditions

The temperature rise of the conductor must be co-ordinated with the ambient temperature, wind conditions, rainfall, and so forth. The frequency and duration of high temperatures in combination with low wind velocities may be found from local weather records. These data may be plotted in various combinations to give a picture of the probability of high temperature with little or no wind coinciding with maximum load. One reason for overloading a circuit may be the tripping of a parallel circuit during a lightning storm, making it desirable to make a study of weather conditions immediately following lightning storms.

Even if the weather bureau records do not show any coincidence of maximum temperature with minimum wind, such a possibility is by no means excluded. It is also possible that some part of the line is located so as to be practically shielded from wind. Sun absorption may be exaggerated by the presence of a building wall or other reflecting surface. All of these factors should be weighed and evaluated.

Other Limitations. Clamps and joints may be a further limiting factor. It is well known that joints deteriorate with time. The modern, drawn, compressed, or automatic joint is far superior to the old sleeve joint in this respect, but deterioration in some degree is bound to take place.

Most suspension and dead end clamps in use today form a more or less closed magnetic circuit around the conductor, and losses at 60 cycles are appreciable. This was brought out by W. H. Burleson. A test on a 500,000-circular mil copper conductor gave a temperature rise of the clamp 80 per cent higher than that of the conductor proper at a current of 800 amperes. The results on another test on a 795,000-circular mil ACSR, with 550 amperes, shown in Figure 1, indicate a considerable cooling effect of the armor rods, and that the temperature of the clamp (without armor rods) reached two and one-half times that of the conductor. Figure 2 shows that while the rise is rapid during the first hour, it requires almost four hours to reach a stable condition. In a weathered conductor, Figure 3, the center or core strand may reach a temperature 15 degrees centigrade in excess of that of the surface strand. This steep gradient, on the order of 5 degrees centigrade to 7 degrees centigrade, has also been found by Zeerleder and Bourgeois.[2]

Conclusions

If permanency and safety of the line are to be considered, emergency ratings must be extremely conservative when based on our present knowledge. A sample test of the conductor in question cannot be considered a reliable answer as to the annealing characteristics of the conductors along the entire line. The lot may have been purchased from a single manufacturer, on a single order, and yet may have been made from two or more different lots of metal.

This does not necessarily mean that it would be unsafe to increase the rating of a line over previous standards. In many cases a substantial increase is possible, even if the various factors are chosen on a conservative basis, and the final limitation may be found to lie in considerations other than the temperature of the conductor.

Clamps and joints should be given careful consideration. The maximum annealing effect is apt to be found where the stresses in the conductors are at a maximum.

Surface measurement of conductor temperatures may be misleading by ten per cent, or more.

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Radio-Noise Filters Applied to Aircraft

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THE importance of the use of radio in aerial navigation is well known. Of comparable importance to the operation of the aircraft, however, is the use of electricity in other forms, as for instance for engine ignition, for lighting, for control, and a variety of purposes. Since commutators and other forms of arcing contacts used in this equipment may cause interference with radio under some conditions, careful planning is necessary to get the fullest use from both power and radio equipment, and not overburden the aircraft with attachments solely to make the radio work.

In order to obtain installations which are reasonably free from radio interference of this kind, due consideration must be given to all of the component parts. For example, attention should be given to the shielding of radio equipment so that it will not be subject to direct pickup of radio noise phenomena. Furthermore, unnecessarily close proximity of power and radio circuits to each other, which gives close coupling for radio noise, should be avoided. In regard to the power equipment, disturbances which give rise to interference should be avoided as far as possible and the proper suppression and shielding employed where necessary.

With the understanding that proper attention is to be given the installation as a whole, this paper discusses the particular phase of the subject which deals with control of radio interference as a complex voltage on the power circuit called radio-influence voltage. Reducing or suppressing this voltage reduces the noise in the radio set resulting from this cause. The paper deals particularly with two suppression devices, capacitors, and radio-noise filters. Filters have been especially useful in many situations on aircraft equipment. They provide an effective and practical means of suppression for these applications.

Available Filters

Manufacturers of filters have made available certain sizes of filters intended to cover a large majority of the requirements for aircraft. Filters are available in 25-, 50-, 100-, and 200-ampere ratings applicable to d-c circuits up to 50 volts. These filters are contained in a metal case which is provided with suitable mounting brackets.

These filters consist of a series line inductance with a capacitance at each end. Two terminals are arranged for connection of the filters in series with one side of the power circuit in which the noise volt-

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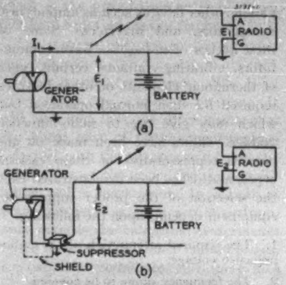

Figure 1. Elementary case of power circuit and radio receiving system

(a.) Without suppressor
(b.) With suppressor

ages are to be suppressed. The other side of the power circuit is grounded, and the filter case is also grounded. When tested in a circuit with aircraft equipment, noise-reduction ratios in the order of 200 to 1 to 5,000 to 1 (46 to 74 decibels) are obtained over the frequency range of 0.20 to 20 megacycles. Also on a circuit standardized for rating performance, noise reductions of 1,000 to 1 to 10,000 to 1 (60 to 80 decibels) are obtained over the same frequency range. Weights of these filters range between 1.5 and 3.0 pounds depending on current rating. Over all outside dimensions including mounting flanges range in length, breadth, and height from 4 by $3^3/_4$ by $2^1/_2$ to $5^1/_2$ by $4^1/_4$ by 3 inches, respectively. The filters will retain their suppression characteristics over a temperature range of 40 degrees centigrade below zero to 90 degrees centigrade above zero. Vibration tests in all possible positions of filter operation and at frequencies and amplitudes met with in service insure against mechanical failures.

Factors Entering into the Problem of Interference Control

The following discussion is intended to bring out by means of a simple example a number of points which should be understood in order to select, install, and test properly suppression devices, and some of the terms commonly used.

An elementary case of a power circuit and a radio receiving system is shown in the upper diagram *a* of Figure 1. The supply generator has a commutator which produces small but extremely rapid changes in the current and voltage. This causes a disturbance voltage and current, indicated at E_1 and I_1. In this diagram only the disturbance component is represented, the power voltage and current being omitted for clearness. The disturbance voltage and current result from a complex phenomenon involving very rapid changes and can, therefore, be treated as radio-frequency voltage and current, not of a single frequency but a spectrum covering a wide range of frequencies. The presence of this voltage and current E_1 and I_1 in the power circuit causes induction in the paralleling radio circuit, similar to the induction which takes place when a radio-frequency voltage is applied to the power circuit. The induced disturbance voltage which appears at the antenna and ground terminals of the radio set is indicated by e_1 and is called radio-noise voltage. The disturbance voltage on the power circuit which produces radio-noise voltage on an antenna, already described and indicated at E_1, is the radio-influence voltage (*RIV*). Both of these quantities can be measured in microvolts at various frequencies by means of a frequency-selective instrument called the radio-noise meter.[1]

There are several ways in which the possibility of interference with radio because of power equipment can be minimized, some of which are as follows. Referring to the elementary case of Figure 1*a*, it is evident that physical separation between the power circuit and the radio circuit would reduce the radio-noise voltage. However, space limitations in an aircraft make it impractical to control interference by this means alone. It is also evident that complete shielding of the radio system would eliminate or reduce the radio-noise voltage. An example of complete shielding would be a well-shielded, independently powered radio set with the antenna lead-in shielded up to the point where it emerges from the fuselage of a metal aircraft. A similar result would be obtained if it were practical to shield completely the power system by enclosing the generator, battery, and other apparatus in metallic containers and running connecting wires in continuous metal conduit. The shielding of power circuits carrying 100 amperes or more and extending to all parts of the aircraft has the disadvantage of weight and inconvenience in servicing the power system. However, complete shielding is commonly applied to some types of apparatus and circuits as, for instance, engine ignition.

Referring again to Figure 1*a*, it is seen that reducing or suppressing the radio-influence voltage E_1 reduces the radio-noise voltage e_1. This can be accomplished either by reducing the disturbance itself or by localizing, that is, confining the disturbance in the apparatus which produces it. A radio-noise suppressor is a device which does this. It is connected to a piece of apparatus such as a generator as indicated in the lower diagram *b* of Figure 1. It does not obstruct the flow of normal power but does prevent the flow of the unwanted disturbance. In terms of radio-influence voltage the purpose of the suppressor is to reduce the voltage E_1 of Figure 1*a* to a smaller value E_2 shown in Figure 1*b*. This results in reducing the radio-noise voltage e_1 to a smaller value e_2. The ratio E_1/E_2 called attenuation ratio is a measure of the effectiveness of the suppressor. It is often expressed in decibels (db) according to the relation

$$\text{db} = 20 \log_{10} \frac{E_1}{E_2}$$

This is called attenuation or insertion loss. The performance of a representative suppression device, in this case a filter, in a circuit such as Figures 1*a* and 1*b*, is shown by Figure 2 wherein the upper curve (E_1) shows the *RIV* without the filter and the lower curve (E_2) shows the *RIV* with the filter, plotted against frequency. The ratio of these two quantities, also plotted against frequency, is shown on Figure 3. Two vertical scales are given, one showing numerical ratio, and the other insertion loss in decibels.

Suppressor Arrangements

Suppressors have been applied to power circuits in many cases for such purposes as smoothing out ripples present in a d-c voltage or reducing distortion of an a-c voltage as well as reducing radio noise.

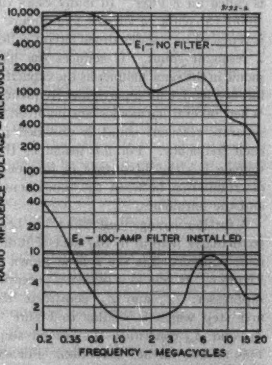

Figure 2. Radio-influence characteristics of airplane generator circuit

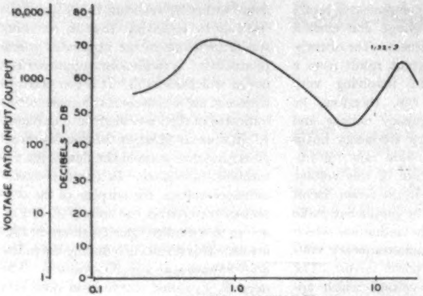

Figure 3. Attenuation characteristics of 100-ampere filter from generator test data

While many different suppressor arrangements might be used, experience indicates that certain simple structures can be used to best advantage. Figure 4 shows a simple arrangement for smoothing out the ripple present in the voltage of a certain d-c machine. It consists of a shunt capacitor across the terminals. The capacitor does not pass direct current and hence does not interfere with the normal operation of the machine. In the diagram E_0 represents the ripple voltage, Z_0 represents the impedance of the machine for this ripple, and Z_c represents the corresponding impedance of the capacitor. It will be seen that Z_0 and Z_c act as a potential divider for the ripple voltage E_0. For example, if Z_c is one tenth of Z_0, only one tenth of E_0 appears on the outgoing circuit. In other words, this arrangement gives 10 to 1 reduction (20 decibels).

Sometimes it is advantageous to add impedance to the machine as at Z in Figure 5, especially if the machine itself has very little impedance. This arrangement is called an *L* filter. In many cases the shunt capacitor of Figure 4 or Figure 5 is replaced by a series tuned circuit called a resonant shunt. It reduces the harmonic in the same manner as the capacitor but is selective for a particular frequency. When several harmonics are involved, a separate resonant shunt is provided for each frequency. For a-c machines this scheme has the advantage of frequency discrimination between the fundamental and the harmonics.

A further arrangement which has been particularly helpful in the field of radio-noise suppression is the π filter shown in Figure 6. It acts as a two-step potential suppressor. Z_0 and Z_{c1} constitute the first step which is similar to Figure 4. Z_{L2} and Z_{c2} constitute the second step. This affords further reduction. For example, if each step gives 10 to 1 reduction or 20 decibels, the over-all reduction will be 100 to 1 or 40 decibels. A practical aircraft filter of this type is shown in Figure 7.

The *L* filter and the π filter are similar in structure to certain simple filters known as low-pass filters employed in communication practice.

Use of Suppressors in Aircraft

The selection of a radio-noise suppressor to fit a given situation and the proper way to install it so that it will function properly involves a number of considerations which are discussed in the sections which follow. These considerations are brought out with reference to a type of aircraft power-supply system that has been studied extensively, but the conclusions are not necessarily restricted to this type of system. A common situation is that of a power supply consisting of one or more generators driven from aircraft engines and operated at about 28 volts d-c, a two-wire circuit, one side of which (usually the negative side) is solidly grounded to the body of the aircraft, stand-by storage batteries, motors, and various other devices such as amplidynes, dynamotors, and inverters. Some of these pieces of apparatus have commutators, vibrating contacts, certain types of thermionic elements or other features required for their normal operation but which may give rise to radio-influence voltage. Tests have been made on apparatus representative of these various types, and it has been demonstrated that the selection of the proper suppression equipment depends upon the following:

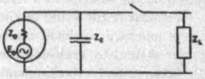

Figure 4. Capacitor as suppressor

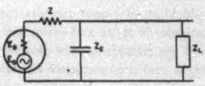

Figure 5. L filter as suppressor

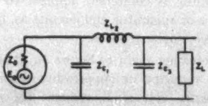

Figure 6. π filter as suppressor

1. The required attenuation of the radio-influence voltage.
2. The frequency range to be covered.
3. The impedance characteristics of the apparatus and circuit.
4. The power currents which the suppressor device must be able to carry.

Complete quantitative data on the first three items are not usually at hand, and most situations have to be treated on the basis of representative tests and experience with the use of suppression devices. With the aid of such knowledge, the kind of suppression equipment to employ can be decided upon. In some cases a capacitor as in Figure 4 can be used to best advantage. In other cases a filter arrangement such as Figure 5 or Figure 6 gives the best results. The general field of application for capacitors and for filters will now be considered.

Capacitors as Suppressors

There are many cases of devices having small current ratings such as five amperes or less on which capacitors may be used effectively, for example when the required attenuation ratio is between 10 to 1 and 100 to 1, and the frequency range is not large, such as the range of standard broadcast frequencies. A capacitor has the property of very low impedance at high frequency. In the case of an ideal capacitor the higher the frequency, the lower the impedance becomes. It must be remembered, however, that every resistor, inductor, and capacitor possesses to some degree all three of the properties—resistance, inductance, and capacitance. In the case of the capacitor some means of connection to the apparatus circuit is necessary, and these connections may have appreciable length and, therefore, appreciable inductance. To determine the effect of these properties a capacitor was tested in a circuit similar to Figure 4 with resistances used to represent the circuit impedances Z_0 and Z_L. Voltage was measured with capacitor off and capacitor on. The ratio of voltage E_1 with suppressor off as in Figure 1*a* to E_2 with suppressors on as in Figure 1*b*, which is the attenuation ratio is shown in Figure 8 plotted against frequency.

The straight line, dotted at the upper end, represents the calculated attenuation for a pure capacitance. The solid curve shows the actual attenuation. It will be noted that the attenuation over the lower part of the frequency range coincides with the pure capacitor, but the attenuation at the upper end is quite different and is characterized by a peak at point *A*. At this point actual performance exceeds the calculated performance based on pure capacitance mainly caused by capacitor resonance, but there is a rapid falling off of attenuation ratio at frequencies above this peak, instead of an increasing attenuation ratio as there would be with an ideal capacitor. The frequency at which the peak occurs in the case of the capacitor tested, which was of a special design for high frequency, was between five and ten megacycles. The characteristic of another capacitor of the same microfarad value but not specially designed for high frequency is shown by the broken line for comparison. The shape of the latter curve indicates that the capacitor has somewhat more inductance and more resistance at high frequencies, which limit its effective range to about two megacycles.

Referring to the "high-frequency" capacitor, the maximum effectiveness, and the frequency at which it occurs, at *A*, depend upon all three of the quantities—resistance, capacitance, and inductance. In this frequency region increasing the capacitance does not necessarily increase the effectiveness and may result in reduced effectiveness depending on the other factors. Sometimes advantage is taken of the "peak" effectiveness by choosing suitable values of capacitance and length of leads to control the inductance so that the peak occurs at a desired frequency. The usefulness of this scheme is somewhat limited, and it is generally preferable to make the capacitor leads as short as possible.

The curve of Figure 8 indicates in a general way the field of application for capacitors as suppressors. At the lower end of the curve which in this example corresponds to about 0.2 megacycle, the capacitor has less effectiveness than at the higher frequencies. While the effectiveness at this frequency can be improved by making the capacitance value (microfarads) larger, this usually results in considerable size and weight. A further increase in attenuation can be obtained by means of a filter such as Figures 6 and 7 with less bulk and weight. At the upper end of the frequency range, that is, above five or ten megacycles, the effectiveness of the capacitor is limited by the inductance of leads and other factors besides capacitance. In and above this frequency range it is usually necessary to use a filter giving an attenuation curve such as Figure 3. In the frequency range from about one to five megacycles there are many cases where the capacitor as a suppressor is the best solution. The most important considerations in the use of capacitors as suppressors are the characteristics of these capacitors in the desired radio-frequency band and the proper arrangement of leads and connections.

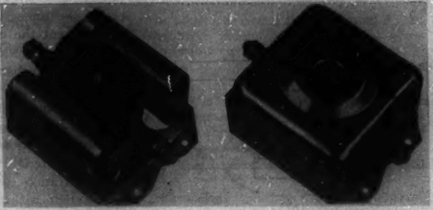

Figure 7. Radio-noise filter showing compact arrangement required

Whether capacitors are used instead of filters depends on

(*a*). The economies.

(*b*). The allowable weight and size.

(*c*). The required performance.

Filters As Suppressors

When apparatus rated one kw or more has to be suppressed over a wide range of frequencies such as 0.2 to 20 megacycles and suppression of 40 decibels or more is required, a capacitor is not generally sufficient for reasons already shown, and some form of filter is necessary. While in some instances filters have been specially designed and assembled as part of a piece of equipment, the general situation calls for the availability of filters as separate items to be selected and applied as the need arises. Manufacturers have made available certain sizes of filters intended to cover a large majority of these requirements. To meet as nearly as possible the general requirements outlined, these filters are designed to give attenuation characteristics based on measurements on a variety of apparatus and experience of aircraft operators over the frequency range of radio equipment used in aircraft. Impedance characteristics of aircraft equipment and circuits have been taken into account in a general way by the use of a laboratory test setup to be described later.

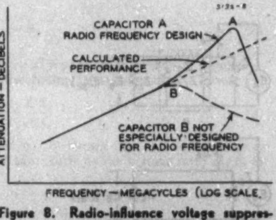

Figure 8. Radio-influence voltage suppression characteristics of capacitors

To illustrate the application of these filters the case of a 2.5-kw 28-volt d-c generator will be considered. The full-load current of this machine is 90 amperes approximately. The filter rated 100 amperes would, therefore, be selected and installed as illustrated in Figure 1*b*. Filter performance is illustrated by Figure 2 already referred to in this paper, when the filter is properly installed.

In order to obtain the full degree of effectiveness which the filter is capable of giving, careful attention to certain installation details is necessary. While a generator is referred to as an example the discussion is intended to apply to a variety of apparatus. The filter should preferably be close to the generator. The intervening circuit must be shielded, the enclosure generally consisting of a conduit which forms a continuous shield with the generator and filter cases. The filter base must be well grounded, for instance mounted directly on the grounded metal structure. The grounded side of the circuit should be well grounded at a point immediately adjacent to the filter.

The importance of this and other factors to the proper functioning of such a filter can be seen by reference to Figure 9. Here three important factors are emphasized, all of which are usually present to a greater or less degree simultaneously. In these diagrams the filter is intended to isolate the radio-influence voltage appearing at the left from the circuit at the right but is not fully effective because of faulty coupling.

Capacitive coupling shown at *a* in Figure 9 is one of the conditions that may interfere with predicted good performance. It results from the presence of ex-

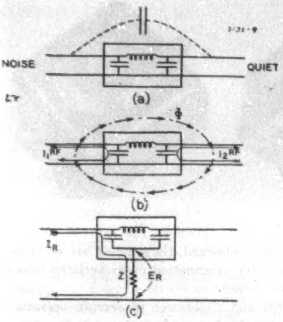

Figure 9. Types of faulty coupling in filters

traneous capacitance between input and output circuits as indicated by *C*. To high frequencies this provides a by-pass and allows extraneous voltages to appear in all or certain parts of the quiet circuit in spite of the presence of the filter.

Inductive coupling shown at *b* in Figure 9 is another condition to be avoided. If the impedance to radio-frequency currents flowing at the left is small, as it should be for a well-designed filter intended for suppression, radio-frequency currents flowing through this circuit may set up electromagnetic flux which if allowed to cut the conductors of a quiet circuit will introduce therein a radio-frequency disturbance as illustrated. In many cases magnetic shielding can be used to reduce this coupling. Electromagnetic flux at commercial frequencies must be shielded with heavy sections of magnetic material. At radio frequencies flux penetration can be prevented to a great extent with thin conducting surfaces. Inductive coupling occurs only when there are loops existing in input and output circuits.

Figure 10. Insertion-loss tester for radio-noise filters

Conductive coupling is a condition such as shown at *c* in Figure 9. Radio frequency currents in the circuit at the left flow through a common ground lead having an impedance *Z*. As a result voltage E_R appears across this impedance and is directly introduced in the circuit which is intended to be quiet. This type of induction may occur unless particular care is taken to secure a good ground connection. The case of the filter should be solidly connected to the electrical ground plane. The impedance of only a few inches of ground wire is extremely detrimental to filtering action.

To obtain a quiet circuit all sources of noise must be given attention. One filter can sometimes be arranged to suppress more than one source. For instance, if the generator in the aforementioned example has a voltage regulator which causes radio noise and all connections to this regulator are made between the generator and the filter, the filter suppresses noise from both sources. All other connecting wires such as field leads must also be connected on the generator side of the filter.

In general the important things to be given attention in the installation of a filter are location, shielding of the unfiltered parts of the circuit, and ground connections.

Measurements

Tests are needed to check the performance of filters under actual operating conditions and to obtain data on which further designs and improvements can be based. The effectiveness of filters is determined by making two measurements of radio-influence voltage preferably on an actual installation in an aircraft. These measurements are made

(*a*). Without filter in the circuit.

(*b*). With filter in the circuit.

Figure 11. Insertion-loss tester for radio-noise filters

Rear view showing amplifier, oscillator, and power supply

Measurements on the input and output sides of the filter do not indicate the effectiveness of the filter because the filter itself changes the circuit constants. The necessary equipment for these tests consists of a radio-noise meter[1] and a reliable signal generator for calibration.

To get measurements within the accuracy[2] of the noise meter certain precau-

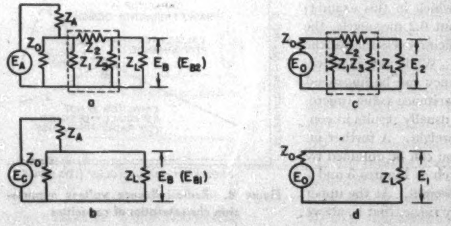

Figure 12. Laboratory circuit for rating filter performance and corresponding apparatus circuit

(*a*). Laboratory circuit, filter in

(*b*). Laboratory circuit, filter out

(*c*). Apparatus circuit, filter in

(*d*). Apparatus circuit, filter out

tions are necessary, particularly in tests made on an aircraft in flight. Among these are

(*a*). Maintenance of proper battery voltage.

(*b*). Proper alignment and adjustment of the noise meter and frequent calibration checks against the signal generator.

(*c*). Placing the noise meter close to the test point which in measurement with the filter should be right at the filter terminal on the "quiet" side.

(*d*). Avoiding test locations where the noise level is high.

(*e*). Providing the noise meter with cushions to eliminate mechanical vibration.

(*f*). Providing a temperature controlled chamber when low temperatures are encountered.

In addition to tests on the actual equipment it has been found useful to make tests on filters at radio frequencies in a laboratory circuit. This setup approximates in a general way the impedance characteristics of a piece of aircraft equipment and the asssociated circuit. The filter performance curves so obtained are not interpreted as those to be expected on apparatus but rather as those obtained in a standardized test circuit. However, the general experience has been that the filters showing the best performance on this network also showed the best performance in actual apparatus. This test has proved useful in the development of filter designs and in checking the product of the factory. Figures 10 and 11 show a filter tester based on this method used in a factory.

A diagram of the laboratory circuit in somewhat common use is shown in Figure 12 together with the corresponding apparatus circuit. Filter performance in this circuit is obtained by measurements without the filter in the circuit and with the filter in the circuit. The analysis given in the appendix shows that the laboratory circuit indicates the same performance as an actual apparatus circuit with a certain set of impedance values.

The measurements just described on a laboratory circuit or on the actual power circuit do not take into account the radio equipment and circuits. A complete study of filter performance should include measurements on the radio equipment, but consideration of the measurement equipment and technique for this purpose is beyond the scope of this paper.

Appendix. Laboratory Circuit for Filter-Performance Tests

The attenuation test commonly made on filters in the laboratory with sine wave voltages at radio frequencies utilizes a resistance network as shown in diagrams *a* and *b* of Figure 12. The voltage source or signal generator is represented by E_A. The circuit impedances Z_o and Z_L of a corresponding apparatus circuit shown by diagram *d* are represented by the same symbols on diagrams *a* and *b*. In the laboratory test these impedances consist of 20-ohm resistors. In addition a 300-ohm resistor Z_A is connected in series with the signal generator to adapt the circuit to the internal impedance characteristics of available signal generators. This 300-ohm resistor does not affect the test result because it cancels out in the "filter out" to "filter in" ratio as shown by the formulas in this appendix. In the diagram *a*, Z_1, Z_2, and Z_3 represent a π-filter (see Figure 6).

The test may be made in two ways. The first method is to hold the signal generator voltage constant and obtain two values of output voltage E_B, E_{B1} with filter out as shown in *b* of Figure 12 and E_{B2} with filter in as shown in *a*. The ratio $(E_{B1})/(E_{B2})$ is equal to the filter attenuation ratio, as will be demonstrated. The second method will be described later.

The following approximations which hold for practically all test setups have been made for the purpose of simplifying the analysis:

The paralleling effect of Z_L on Z_3 is neglected.

The paralleling effect of Z_o (to the left) and of other impedances (Z_2 and so forth to the right) on Z_1 is neglected.

The impedance looking into Z_2 from the left, diagrams *a* and *c*, is assumed to be the same as Z_2 itself, which neglects the added effect of Z_3 and Z_L.

The impedance looking into the circuit from the signal generator in *a* and *b* is assumed to be equal to Z_A which neglects the added effect of Z_o, Z_1, Z_L, and so forth. The largest error occurs in *b* where the actual value is 310 ohms and the approximate value is 300 ohms, the value of Z_A.

The impedance looking into the circuit from the source in diagram *c* is assumed to be equal to Z_o which neglects the added effect of Z_1 and other elements to the right.

In diagram *a* the value of E_B is approximately

$$E_{B2}=E_A\frac{Z_1}{Z_A}\times\frac{Z_3}{Z_2} \qquad (1)$$

When the filter is removed, as in *b*, and the signal generator voltage kept the same $(E_C=E_A)$ E_B becomes approximately

$$E_{B1}=E_A\frac{Z'}{Z_A} \qquad (2)$$

where Z' represents Z_o and Z_L in parallel.

Combining these equations we have

$$\frac{E_{B1}}{E_{B2}}=\frac{Z'Z_2}{Z_1Z_3} \qquad (3)$$

It will be shown that this equation gives the same ratio as the voltage ratio E_1/E_2 in the apparatus circuit diagram *c* and *d*. In the former (filter in) the voltage E_2 is given approximately by the relation

$$E_2=E_o\frac{Z_1Z_3}{Z_oZ_2} \qquad (4)$$

In diagram *d* (filter out) the voltage E_1 is given by the relation

$$E_1=E_o\frac{Z_L}{Z_o+Z_L} \qquad (5)$$

Combining equations 4 and 5 we obtain

$$\frac{E_1}{E_2}=\frac{Z_oZ_L}{Z_o+Z_L}\times\frac{Z_2}{Z_1Z_3} \qquad (6)$$

Since the term $(Z_oZ_L)/(Z_o+Z_L)$ gives the value of Z_o and Z_L taken in parallel which is designated by Z' in equation 3, the right-hand members of equations 3 and 6 are the same, and therefore

$$\frac{E_{B1}}{E_{B2}}=\frac{E_1}{E_2} \qquad (7)$$

Equation 7 shows that within the approximations stated the laboratory test shows the effectiveness which the filter being tested would have in an apparatus circuit having the same value of Z_o and Z_L.

It is generally more convenient to use the second method which is to reduce the signal generator voltage in diagram *b*, filter removed, to give the same value of output voltage E_B as in *a*, filter in. The effectiveness is then given by E_A/E_C which is the same ratio as $(E_{B1})/(E_{B2})$. This is demonstrated as follows:

$$\text{Equation 1 becomes } E_B=E_A\frac{Z_1Z_3}{Z_AZ_2} \qquad (1a)$$

$$\text{Equation 2 becomes } E_B=E_C\frac{Z'}{Z_A} \qquad (2a)$$

Combining these equations we obtain

$$\frac{E_A}{E_C}=\frac{Z'Z_2}{Z_1Z_3} \qquad (3a)$$

Referring to equations 3 and 7, it is evident that

$$\frac{E_A}{E_C}=\frac{E_1}{E_2} \qquad (8)$$

This relation enables the filter effectiveness to be determined from the signal generator voltage calibration in which case the voltmeter on the output (E_B) is used merely as an indicator.

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Manual Switches for Aircraft

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Synopsis: In the design and application of electrical control equipment the problem of obtaining complete field data and requirements is always present in some degree.

The problem is acute in branches of the electrical industry supplying aircraft control. Three factors contribute to the difficulty:

1. The tremendous engineering effort centered in the aircraft industry causes rapid technical changes that soon make accumulated data obsolete.
2. Technical data on utilization devices are not always available to either the aircraft or electrical manufacturer. The mushroom growth and technical advance of the aircraft industry have, in some cases, outdistanced laboratory equipment and personnel capacity so the data an electrical-control manufacturer would like to have are simply not available.
3. The secrecy surrounding many aircraft developments blocks the passing of information from the aircraft manufacturer to the electrical manufacturer.

The electrical-control manufacturer can meet this problem, in part, by making available complete performance data on switching equipment offered for sale. Properly chosen, these data enable the aircraft engineer to match control means with his electrical utilization device requirements.

In the preparation of such data the following considerations are of primary importance to both the user and producer of switches.

The functions of an aircraft switch may be classed broadly as follows:

1. To establish a circuit.
2. To maintain a circuit.
3. To interrupt a circuit.

Performance of these functions is importantly affected by the following characteristics of the load:

(*a*). Inrushes.
(*b*). Inductance.
(*c*). Current.
(*d*). Voltage.

IN establishing a circuit, the increasing order of difficulty shows:

(*a*). Inductive loads.
(*b*). Resistive loads.
(*c*). Lamp loads.

When a set of switch contacts closes, the circuit is seldom established without some disturbance (see Figure 1). The current magnitude at the time of this disturbance becomes a major factor in switch performance. Contact erosion results, caused by the arcing which normally accompanies such disturbance, and in extreme cases freezing or welding occurs with the possibility that the switch may immediately become inoperative through inability to open the freeze. In most switches the contact bounce that creates these disturbances ends within ten milliseconds, and the current magnitude for that period of time is of primary importance in connection with this first function of a switch, establishing the circuit.

(*a*). Inductive loads build up slowly, and the current for a time after the switch contacts close is less than steady-state value. For this reason damage to contacts is minimized.

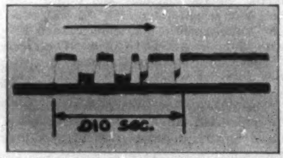

Figure 1. Switch contact bounce

(*b*). Resistive loads cause more damage in establishing the circuit than inductive loads do because of the substantially vertical front current increase. This means that any bouncing of contacts (with accompanying arcing) occurs at practically full steady-state circuit load instead of the fraction of steady-state current indicated in the preceding inductive-load discussion.

(*c*). Inrush loads are most severe in circuit establishment. The common forms experienced are lamp and motor inrushes. The former—because of their characteristic vertical front current increase, and because of the relatively slow decay of the inrush—are the most severe of any circuit-closing condition. Any switch bouncing is almost sure to occur at currents well above steady-state values. Motor inrushes are not comparably severe because the inductive component tips and rounds off the wave front in a manner so bouncing usually occurs before maximum inrush current is reached. Condensers have a steep wave front like resistance, but the circuit ordinarily has other limiting resistance.

The second function of the switch, maintaining the circuit, is mainly affected by the circuit characteristics of current and voltage. The passage of current across the internal resistance of a switch results in a wattage loss that must be dissipated without causing harmful temperature rises in either conducting or insulating members. Experience indicates that, if the continuous duty rating of a switch be taken as 100, the 15-minute, 5-minute, 1-minute, and 5-second ratings may be taken as 110, 150, 200, and 400, respectively, for the average switch used in aircraft circuits. Losses must also be limited to the extent necessary that excess voltage drop will not obtain. The circuit voltage (which may vary from supply voltage) strains the switch insulation, which must, under all operating conditions, be capable of restricting current flow to switch parts that are normally current-carrying members. Conducting paths to ground or opposite polarity result from voltage breakdowns, and such paths may carbonize and progressively develop more wattage until complete failure occurs. The stripping of conductor insulation at the switch requires the provision of adequate circuit insulation from terminal to terminal through the switch.

The third function of the switch, interrupting the circuit, is affected by circuit current, voltage, and inductance. Figure 2 shows a switch opening a resistance load of $30A$ at $30V$ and the same switch opening a highly inductive load of $25A$ at $30V$. The arcing time for the inductive but slightly smaller load is more than 12 times that for the resistive load. The major watt-seconds loss is developed near the start of circuit opening even though a high-voltage kick develops just before the current reaches zero. This last kick, though occurring at so low current value that little wattage is involved is of importance because of the strain it places on circuit insulation and because it contributes, in some instances, to arc restriking or flashovers to ground.

In order that switch functioning under the previously described circuit conditions may be checked by laboratory tests it is necessary that voltage and current

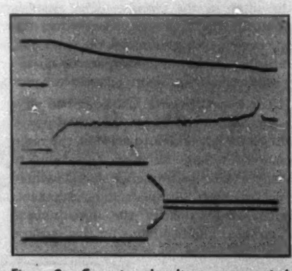

Figure 2. Current and voltage across switch contacts, 25 amperes inductive (above) and 30 amperes resistance (below) loads on 30-volt supply

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sources, along with elements to establish circuit components for inrush and inductive effects, be provided. A series of performance requirements, substantially formalized in specification *ANS*-20 is applied to test each switch intended for aircraft, establishing the following data:

1. Current-making capacity (10,000 operations).

(*a*). Lamp load (10× hot filament current).

2. Current-breaking capacity (10,000 operations).

(*a*). Resistance load.

(*b*). Inductive load (using standard test inductors with L/R ration of 0.26).

3. Emergency break (50 operations inductive load).

4. Current-carrying capacity.

1*a*. The inrush circuit provided for determining current-making capacity uses a normally closed contactor which opens soon after (about 40 milliseconds) the switch under test closes (see Figure 3) and remains open until the switch under test opens. As shown in Figure 4, the switch under test opens zero load. This may be made a finite load, however, by shunting the break load across this normally closed contactor. The advantage of this circuit lies in the fact that the line of current increase is almost vertical, and a flat top holds the inrush at maximum value for approximately 40 milliseconds so that any switch closing disturbance occurs at peak current. The lockout-contactor type of circuit is a more severe circuit than usually found in practice, but it provides a test that may be duplicated in any laboratory and determines a switch rating that may be used safely with any kind of inrush load.

2*a*. The resistance-load test requires no special comment.

2*b*. The inductive circuit for this type of testing was co-operatively developed by the Army Air Corps and the Navy Bureau of Aeronautics, again with the aim of providing a circuit which could be duplicated in any laboratory and which would, at one time, test switches under conditions of sufficient severity to guarantee satisfactory performance in any of the so-called "highly inductive" aircraft circuits. These circuits include brake coils, solenoids, relay and contactor coils, and so forth.

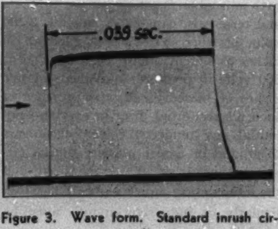
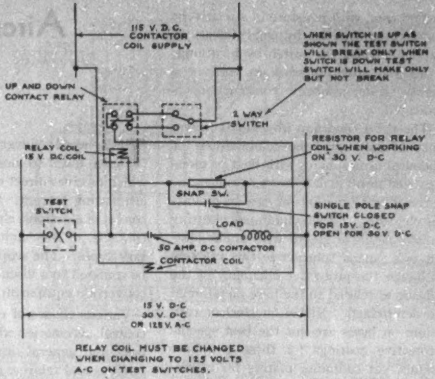


Figure 3. Wave form. Standard inrush circuit

Figure 4. Line diagram of standard inrush circuit


115 V. D.C. CONTACTOR COIL SUPPLY
WHEN SWITCH IS UP AS SHOWN THE TEST SWITCH WILL BREAK ONLY WHEN SWITCH IS DOWN TEST SWITCH WILL MAKE ONLY BUT NOT BREAK
UP AND DOWN CONTACT RELAY
2 WAY SWITCH
RELAY COIL 15 V. DC. COIL
RESISTOR FOR RELAY COIL WHEN WORKING ON 30 V. D-C
SNAP SW.
SINGLE POLE SNAP SWITCH CLOSED FOR 15V. D-C OPEN FOR 30 V. D-C
TEST SWITCH
LOAD
50 AMP. D-C CONTACTOR
CONTACTOR COIL
15 V. D-C
30 V. D-C
OR 125 V. A-C
RELAY COIL MUST BE CHANGED WHEN CHANGING TO 125 VOLTS A-C ON TEST SWITCHES.

So duplication could be readily attained, the following inductor elements are defined:

1. Iron circuit.

(*a*). Lamination dimensions.

(*b*). Lamination material.

(*c*). Magnetic saturation.

(*d*). Air gaps.

2. Copper circuit.

(*a*). Coil dimensions.

(*b*). Ballast resisters.

The stored energy of the magnetic circuit and the rate at which this stored energy is released were made the subject of a study based on tests conducted on a number of solenoids, relay coils, and so forth, now in use. Voltage surge, current decay, and arcing time were the main measures of equivalence and were determined with the circuit controlled by the same switch type in all cases. The standard test inductor shown in Figure 5 resulted.

The functioning of a switch is basically dependent on the characteristics of the electrical circuit to be controlled, but the physical characteristics of the surrounding atmosphere and supporting structure are, in many cases, of equal importance.

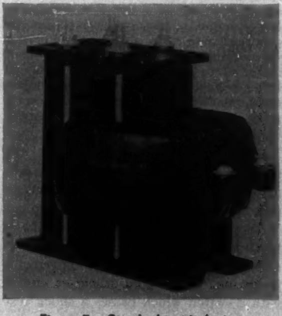


Figure 5. Standard test inductor

Physical conditions which at times assume such importance are:

1. Ambient temperature.
2. Humidity.
3. Chemical atmosphere or sprays.
4. Accelerations.
5. Altitude or pressure.

The ambient temperature, in the low range of 50 and 60 degrees Fahrenheit below zero may interfere with the mechanical action of a switch by congealment of lubrication, while the high range of 150–175 degrees Fahrenheit accentuates the problem of temperature control in current-carrying elements and insulation.

The second physical condition substantially affecting switch performance is the presence of humidity or moisture, either through exposure of the switch to direct wetting action by rain, splashing, and so forth, or by breathing air into the switch interior under conditions of high relative humidity with variable temperature. In the first case the switch is best protected by the use of water-resistant gasketing and joint constructions. In the

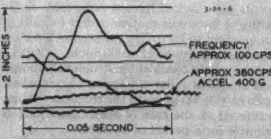


Figure 6. Panel subjected to shock test

With eight-pound shutter on bakelite panel at center second case, with moisture in a relatively tight switch interior by condensation of vapors entering the switch by breathing, either the provision of drain holes or a highly water-resistant construction is indicated.

The third condition involves the presence of salt spray or combinations of chemical atmospheres with heat or moisture that result in deterioration at metallic surfaces and junctions of case or mounting elements. The elimination of abutting unlike metals or metals with unlike plating is a partial solution to this problem although the protective adequacy of the plating as related to the base metal must be determined. Nickel on steel or cadmium on brass are not the best specific protective coatings for these two base metals, yet cadmium plating for assemblies of steel and brass parts is a good plating because the more readily corroded material (steel) is protected and unlike metallic abutments are not present. Choice of protective metallic coating must also recognize the by-products resulting from protective action. For instance, zinc, in the presence of moisture, forms zinc hydroxide and carbonate, bulky salts of high electrical resistance, that would cause failure in closely fitting assemblies or at the electrical contacts of a switch. Cadmium, in a tight enclosure and at high temperature, in presence of some insulating varnishes and oils, such as tung and chinawood oils and air dry phenolic varnishes, may "bloom," forming a bulky salt of high electrical resistance.

The fourth condition affecting switch performance results from mechanical acceleration which may occur in the following forms:

(*a*). Linear.
(*b*). Vibrational.
(*c*). Shock.

(*a*). In the first form a constant linear acceleration and its effect on switches can be readily computed and measured by laboratory tests. A rotating table may be used to generate this acceleration in accordance with the formula:

$$\text{Acceleration} = Kx(\text{RPM})^2 \times R$$

(*b*). In the second form of acceleration free vibrations that may be induced by vibration in the plane frame and structural elements are checked by test on a table oscillating in harmonic motion over a frequency range of 10 to 60 cycles per second with an over-all amplitude of 0.060 inch. Devices under test are checked for structural failures and electrical failures (closing circuit when open or vice versa) with the frequency range traversed at the rate of about once in 3-5 minutes for a period of one hour.

(*c*). In the third form of acceleration, shock resulting from landing, shell burst, and so forth, transmits itself to the switch through plane structure and switch mounting means. The motion, velocity, and accelerations involved are a combination of effects discussed in *a* and *b*, although the energy imput to the switch is ordinarily greater than in either *a* or *b*. See Figure 6 showing the time—motion trace of a panel subjected to a high-shock test. Manual switches have been tested in a pendulum shock machine. Consideration is being given the use of a drop machine suggested by the Bureau of Standards.

In these three acceleration tests, the measure of electrical failure is a self-maintaining electrical indicator set to pick up in four milliseconds and drop out in four milliseconds. A device like a bomb rack solenoid requiring eight milliseconds to initiate armature movement, would not be affected when in circuit with a switch which passes a shock test with four millisecond indicating means.

The fifth physical condition affecting switch performance is altitude or pressure. Air-gap dielectric strength becomes a matter of definite concern. The sparking potential, a function of gap and pressure, becomes a major factor, and switches with normal forms of air gaps at contacts and terminals, capable of holding 2,500 volts pressure across these gaps at 29 inches of mercury, have by test failed at 800 volts in pressure equivalents of five inches of mercury. In the presence of such a substantial drop in sparking potential any tendency toward flashovers to ground or across polarity during circuit opening is severely emphasized, and the interrupting capacity of switches, particularly on inductive circuits, must be carefully determined.

Aircraft Inverter Construction

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THE word "inverter" is a generic term used to designate any device which receives direct current and delivers alternating current. Such a device may consist of electronic elements, or vibrating contacts, or mechanical choppers, or rotary types. The scope of this paper will be confined to a discussion of rotary-type conversion equipment.

Various classes of rotary inverters and control accessories will be listed with a brief and general description of characteristics and relative merits, viewed from the standpoint of the requirements of military aircraft. A statement of what the principal requirements are is first in order.

Operating Requirements

An aircraft inverter must operate on the d-c supply of the plane, which usually is obtained from generators driven from the main engines. These generators may be of 50-, 100-, or 200-ampere rating; and in a four-engine bomber there are usually four 200-ampere generators connected in parallel while in normal flight. The d-c bus voltage is maintained fairly constant by means of carbon-pile regulators controlling the generator fields. A battery usually floats on the bus.

The inverter is connected to the d-c bus through a solenoid-operated switch. For purposes of inverter design, the voltage delivered to it is considered to be 27.5 volts, with maximum variation of plus or minus 2.5 volts. In routine flight, carbon-pile voltage regulators restrict the d-c voltage variation to less than one volt, except for momentary transients.

With an input voltage at any point from 25 to 30 volts, the inverter should be capable of starting and carrying full rated load continuously. An operating life of at least 1,000 hours without major overhaul is expected, with starting as frequent as once per hour.

Output ratings may be anywhere from 6 to 3,000 volt-amperes, with an ever-present possibility of increased loads in the future requiring larger sizes. Effort has continuously been made and will continue to be made to standardize on rated loads or sizes. In view of the fluidity of the load requirements and resulting necessity of modifying specifications on actual conversion units procured from time to time, a formal tabulation of ratings will not be listed. Formalized specifications are available, one of which is the United States Army Specification 94-32270. These formal specifications are amended from time to time as need requires.

In order that this discussion may be self-contained however, three sizes of

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inverters currently used in large quantities will be mentioned, the ratings of which are as follows:

	Output at 400 Cycles		
Rating	26 Volts 40 Per Cent Power Factor	115 Volts 90 Per Cent Power Factor	Class*
250 va.	60 va. . 1 phase	190 va. . 1 phase	*C*
750 va.	250 va. . 1 phase	500 va. . 1 phase	*D*
1,000 va.	250 va. . 1 phase	750 va. . 3 phase	*F*

* See tabulation under "Classification of Basic Types."

The 26-volt output is primarily intended for electrical-instrument supply but may be used for other single-phase loads such as fluorescent lighting. The voltage requirements are not exacting on this circuit, and automatic regulation has not been considered necessary.

The 115-volt circuit may be used to supply fluorescent lights, automatic radio compass, motor loads, supercharger regulators, automatic pilot, and bombsight. In the case of some of these loads, the voltage requirements are not exacting, and the percentage variation experienced on the d-c bus may be tolerable. In such case, an inverter which may be regarded as a transformer (if of sufficiently low internal impedance) may be a simple and satisfactory mechanism.

However, as more precision equipment is developed and put into use, and with transient voltage fluctuations occurring because of high momentary current to gun-turret motors, need for automatic control of the 115-volt 400-cycle source becomes apparent.

As a broad statement, the voltage on that circuit should not exceed 120 nor be less than 110 at any load from no load to full load, and at any d-c input voltage from 25 to 30. However, modulations or transients or poor wave form may be objectionable in some instances. Since no practical source will deliver a pure sine wave at exactly 115 volts 400 cycles in service, and since the greater precision demanded the more complicated the equipment becomes, the construction and acceptance of the most practical machine in any particular case necessarily becomes a matter requiring engineering cooperation. In other words, there are so many factors involved that the procurement engineers cannot say "Here are our specifications. Any machine meeting them is acceptable, and contrariwise." Likewise, the manufacturers' engineers cannot say "Here is the best possible equipment for the job; that's all there is."

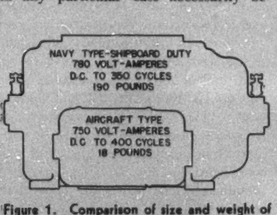

Figure 1. Comparison of size and weight of units built for aircraft and shipboard duty

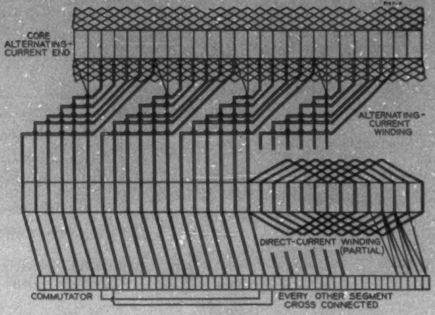
CORE
ALTERNATING-CURRENT END
ALTERNATING-CURRENT WINDING
DIRECT-CURRENT WINDING (PARTIAL)
COMMUTATOR
EVERY OTHER SEGMENT CROSS CONNECTED

Figure 2. Cascade inverter — schematic diagram of armature connections

Specified weights for various inverter ratings will not be listed, as they are changing from time to time. Refinements in design make lighter weight machines available. However, a contrary tendency toward heavier weights also inevitably results from requirements for more accurate control of output voltage and frequency. As an illustration of weight trend, one inverter of the inductor-dynamotor type was in use in 1939, weighing 18 pounds and capable of delivering 250 volt-amperes. A cascade-type inverter was designed in 1940 having the same weight and delivering 750 volt-amperes. A comparison of the latter with a motor-generator set for use on board ship is given by Figure 1, where the two are shown with outlines drawn to the same scale.

Later developments, however, required addition of radio-interference filter and control accessories, adding a few pounds.

The importance of light weight in aircraft equipment needs no argument. One pound of fixed weight requires another pound of structure and fuel to carry it.

Likewise, the importance of efficiency may be emphasized by the following approximate data:

An aircraft engine consumes about 0.5 pound of fuel per horsepower-hour. The generators are roughly 75 per cent efficient. Hence, 1,000 watts of inverter input requires the consumption of about one pound of fuel per hour. Every 100 watts saved is worth a pound of fuel on a ten-hour flight. That pound of fuel might take the airplane another half mile. It might be said as a generality that 100 watts of additional inverter losses would necessitate a certain additional weight of generator capacity. In a practical case, this capacity may be already available.

All things considered, perhaps a weight saving of one pound in an inverter is worth while if the losses are not increased as a result by more than 100 watts. This figure is by no means definite, however, and has not been set up as a design criterion.

It is no simple problem, nor can any definite general answer be given, as to what point it is best to stop at in weight reduction on account of loss in efficiency or possible sacrifice in reliability. Likewise, extremely light weight may mean high internal impedance and hence high-voltage regulation. High speed makes light weight possible but reduces bearing life and decreases reliability.

These are all matters which should have the benefit of sound engineering judgment. The most sound judgment is

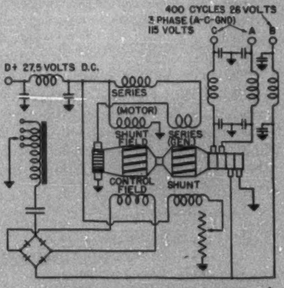

Figure 3. Schematic diagram of 1,000-volt-ampere dynamotor-generator type

based on experience. It is quite common in aircraft work, however, for experience to bring to light considerations which either were not anticipated or were erroneously regarded as of negligible importance.

Classification of Basic Types

A rotary inverter essentially consists of a d-c motor driving an alternator. The two elements may be combined in a single magnetic circuit, the most intimate combination being an inverted rotary converter. The latter is analogous in some degree to an autotransformer, where primary and secondary conductors are common. A simple rotary converter cannot be used to fulfill the requirements in question, however, on account of the voltage ratio involved and the fact that grounded circuits are used on both primary and secondary.

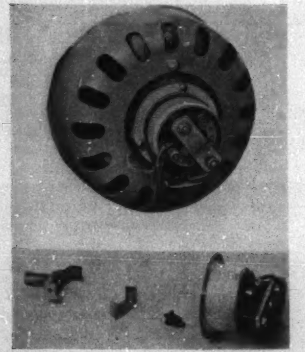

Figure 4. Centrifugally operated contact-making speed governor, cover removed

Parts shown in lower view include two rotating pieces, bakelite button for transmitting thrust, and stationary contact unit with adjusting screw

The following list covers classes of equipment in current production, types which have been built experimentally, and others which conceivably could be used. The list does not include every theoretical possibility of variation and combination.

A. Motor-generator set

1. M-G set with rotating field generator
2. M-G set with rotating armature generator
3. M-G set with inductor generator
4. M-G set with permanent magnetic generator

B. Dynamotor

C. Inductor-dynamotor

D. Cascade inverter

E. Inverted rotary converter with transformer

1. Separate transformer
2. Rotating transformer

F. Dynamotor-generator

G. Dynamotor with booster

H. Cascade inverter with booster

Types $A4$, B, C, D, and E are not adaptable to independent output voltage control, except by a separate series boost or buck arrangement in primary or secondary circuits. The behavior of B, C, D, and E types may be compared with that of a transformer, where output voltage is proportional to input voltage less internal impedance drop.

Considering the other classes where automatic control of output voltage may be obtained by field regulation, these may in turn be divided into two groups—those with slip rings and those without.

$A1$, $A2$, F, and G have slip rings.

$A3$ and H do not have slip rings.

Of these latter two groups, those without slip rings will weigh perhaps 25 to 35 per cent more than those with slip rings, all design factors being on an equivalent basis.

Another theoretical possibility which would fall in the last group is an inductor dynamotor with inductor-type booster generator. The practical advantages of such a combination is questionable.

The motor-generator set with inductor generator, class $A3$, has the advantages of ruggedness and reliability, since it has no conductors on the generator rotor and no slip rings. For 400-cycle service where light weight and hence high speed is required, the generator will probably be the twin-stator type with single stationary field coil in the shape of an annular ring lying between the two stators. The generator rotor will have three salient poles or six effective poles on each end, for 400 cycles at 8,000 rpm. The direction of the flux is centrifugal in one stator and centripetal in the other, and crosses each air gap in three rotating bands of high and essentially constant density at the rotor pole faces.

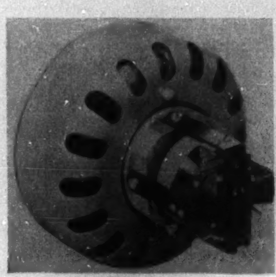

Figure 5. Centrifugally operated carbon-pile speed governor

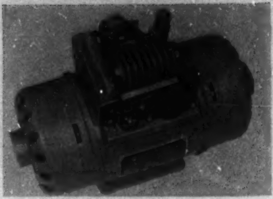

Figure 6. Physical arrangement of resonant-circuit feed-back speed-control equipment including capacitor, reactor, and rectifier shown in Figure 3

The inductor dynamotor, class C, has a single armature with salient teeth or poles. The a-c winding lies in slots in the pole faces, the field poles otherwise being like those of an ordinary two pole d-c motor. This mechanism is essentially a merging of the motor-generator set with inductor generator into a single magnetic circuit. This combination does not produce a net gain necessarily, since control of output voltage (except in steps by a tap switch) is lost, and a speed of over 4,000 rpm for 400 cycles would result in difficulties. For some purposes however, such a machine has been extensively used with satisfactory results.

The cascade inverter consists of two parts in one frame and is physically similar to a two bearing motor-generator set. The d-c end acts as an inverted rotary converter built for 200 cycles, multiphase output. This output is delivered to the rotor of the a-c end, which is slotted and wound similar to the d-c armature, except that the 200-cycle phase rotation is reversed, as shown by Figure 2. Thus, the a-c end operates as an a-c excited generator, with combined electrically and mechanically rotating field. When built for a speed of 6,000 rpm for 400-cycle output, half of the energy is delivered from the d-c end electrically and half mechanically. There are four conventional stationary salient field poles on the d-c end, and a four-pole stator on the a-c end. The only control of output voltage possible is obtained by a tap switch connected to the output stator.

An inverted rotary converter with rotating transformer, class $E2$, has been built, the advantages conceived being that the (circular three-legged) transformer would be well cooled; and six-phase low voltage could be carried directly to the transformer primary winding. This particular machine was intended for three-phase output only and hence had three slip rings.

The dynamotor-generator, class F, was designed to deliver both three-phase and single-phase output, and it was desired not to have the single-phase load unbalance the three-phase voltages. Accordingly, the motor was designed for operation at 8,000 rpm with six poles, and a dynamotor winding superimposed to deliver single-phase 400 cycles through slip rings. Figure 3 is the schematic diagram. The generator is a three-phase rotating-armature type. Hence this machine is a combination of class $A2$ and class B.

A dynamotor with booster, class G, has been built for three-phase output only. A dynamotor may be somewhat lighter than a motor-generator on account of having a single magnetic circuit, utilizing the same flux for both motor and generator conductors. A small generator adequate to supply the differential energy required to maintain constant output voltage is provided, acting as a booster. The greater part of the energy conversion takes place in the dynamotor. This was built in two and three kilovolt-ampere sizes and is probably the lightest type of inverter of the slip ring class for this particular duty.

A cascade inverter with booster, class H, has been built, which is a class D (cascade) inverter with tertiary element in the 200-cycle circuit between the d-c and a-c armature cores. This tertiary element is a small 200-cycle generator with four salient field poles and with each armature coil connected in series with the 24-wire 12-phase 200-cycle output from the d-c armature. The series boost effect at 200 cycles was added at an advanced phase angle so that with a lagging 400-cycle load the power factor of the 200-cycle load on the d-c armature might be brought near unity. The objective was to increase the efficiency since an inverted rotary converter operates most efficiently on a resistance load. However, the net over-all gain of this boosting at an advance angle is dubious, since the tertiary element must then have more capacity than would be the case for in-phase boosting.

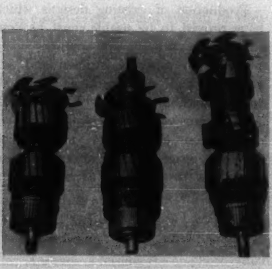

Figure 7 (left). Armature for cascade inverter

(Center). Armature for dynamotor-generator type. Twenty-six-volt a-c winding can be seen in the same slots with d-c motor armature winding

(Right). Rotating element including motor armature and rotor for inductor generator

Figure 8. Inverter made by the Leland Electric Company

An inverter with inductor-type generator is being considered, with one stator having a three-phase winding and the other supplying the single-phase load.

At least four of the classes listed are in current use in large quantities.

As is frequently the case, there would be no particular problem involved if one type of machine had all of the advantages (minimum number of brushes, light weight, reliability, and independent sources of single- and three-phase output). Many practical considerations enter, and it may be that, even for a single well-defined set of load requirements, it will be difficult if not impossible to arrive at a universally accepted decision as to the best machine for the job.

The problem of obtaining satisfactory brush operation at high altitudes is now generally recognized in the electrical industry as a serious one. The approach to this, in so far as inverter design is concerned, has been to study the problem intensively in itself but simultaneously to develop inverter designs without generator slip rings. As indicated previously, a sacrifice in weight must be accepted in payment for the increased reliability of an inverter with no generator brushes.

Construction Details

Inverters for aircraft duty are made in two-bearing open-type frames. Specifications require openings to be small. Internal fans, either propeller or centrifugal type, are used. Sealed or labyrinth-type shielded bearings are generally used, with no provision for relubrication. A speed of 8,000 rpm is commonly used, but cascade types running at 6,000 rpm and inductor types at 4,000 are also widely used. The inverters must be capable of operating in any position, be built to withstand shock and vibration, and be capable of operating in any outdoor temperature and pressure encountered on the surface of the earth and up to 40,000 feet above it.

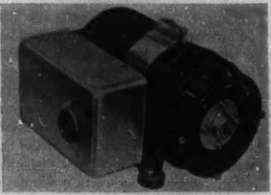

Figure 9. Inverter of Eclipse aviation division, Bendix Aviation Corporation

Hollow shafts are used in many of the inverters for weight reduction. End bells are made of magnesium or aluminum. Armature and stator laminations have holes or slots where all metal not definitely needed for carrying flux or for mechanical purposes is removed.

Armatures are, of course, dynamically balanced. In the case of some of these machines where the armatures are long, the shaft not too heavy, speed high and air gap small, special procedure may be required to insure that a static unbalance near the middle is not compensated by two weights added on the opposite side but near the bearings, resulting in a tendency for the shaft to spring.

With good air circulation easily obtainable at the high speeds used, and the losses kept relatively low to obtain good efficiency, it has not generally seemed necessary to provide for higher temperatures than commonly dealt with in other electrical apparatus.

Filters for suppression of radio interference are provided integral with the inverters.

Electrical connections are made with

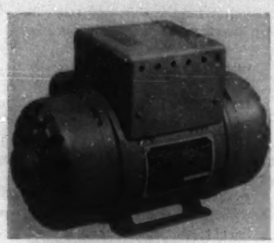

Figure 10. Holtzer-Cabot Inverter

standardized *A-N* aeronautical-type fittings.

Frequency Control

Figure 4 shows a centrifugally operated contact-making speed governor which has no slip rings or brushes. The rotating element consists of a yoke carrying a spring which is deflected by the centrifugal force resulting from two weights. The axial motion of this spring is transmitted to contacts which are a part of the stationary unit by means of a "bakelite button" which engages with a dimple in the spring at the center of rotation. The points are of molybdenum-silver. The usual capacitor and resistor are provided across the contacts. The familiar Lee-type governor is also used.

A carbon-pile speed governor which is actuated by centrifugal force has been developed, the operation of which is superior to that of the contact-making governor in some respects. This is shown by Figure 5.

A resonant circuit feed-back speed-control circuit, Figures 3 and 6, is used on one inverter. This has a series circuit tuned for a peak at slightly over 400 cycles. In series with the capacitor and reactor in this circuit is a dry-disk rectifier. The d-c terminals of this in turn supply a control field on the motor. Consequently, as the output frequency approaches 400 cycles, the current in this control field rapidly increases, and the speed stabilizes. The rectifier delivers to the control field direct current plus an 800-cycle component. In order to maintain substantially constant flux, each field pole is provided with a heavy copper shading coil. Taps are provided on the reactor to compensate for manufacturing variables in the inverter and the capacitor.

Electronic controls have been developed for both speed and voltage. It would be quite possible to obtain extremely accurate frequency control by this means, if required, by using a tuning fork as the reference. Here again however, the greater the precision required the more susceptible to trouble the apparatus becomes—as well as being more bulky.

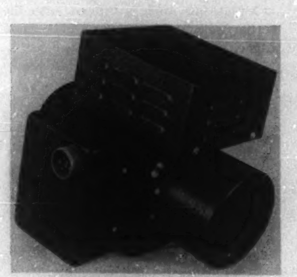

Figure 11. Inverter manufactured by General Electric Company

Voltage Controls

It is believed that no good purpose will be served by elaborating on theoretical possibilities for automatic voltage control. A long discussion could be presented, starting with the Tirrill-type regulator and leading up to electronic-type precision controls. These devices would include multifinger contact-type regulators, saturated reactors, and various others. The carbon-pile regulator, however, has come into very general use both for controlling the main d-c generators and for regulating the 400-cycle inverter output as well. Present indications are that it has no inherent serious disadvantages and that it produces satisfactory results. In applying a carbon pile to an inverter, over-all temperature compensation must be obtained, including the rectifier in the circuit supplying the actuating solenoid from the 400-cycle circuit being regulated.

Radio-Interference Filter

Specifications require the radio-frequency component of voltage in any input or output conductor of the inverter to be less than 50 microvolts at any frequency from 0.2 to 20 megacycles. For the purpose, it is common practice to use a π-circuit filter in each conductor. The reactance should not be so large as to cause excessive voltage drop in the 400-cycle conductors, and here air-core chokes are commonly used. Textbook rules for the most efficient design of choke result in minimum space and weight. Capacitors should be no larger than necessary, both because of space and weight considerations and the effect of the leading current on the no-load voltage where voltage regulators are not used. Great care must be used in designing a filter as to arrangement of components, length of leads, and method of grounding.

Conclusions

While continuous effort has been made to standardize ratings and performance requirements, this has been difficult. Load requirements change rapidly, and the way must be left open for refinements of inverter design.

A motor with automatic speed control driving a generator with automatic voltage control appears to be the most desirable general type for most purposes.

Production of existing designs which have proved to be usable must be continuously carried on; and simultaneously new designs must be studied and advantages and disadvantages weighed. Interchangeability must be considered. Also, designs cannot be changed too frequently on account of maintenance problems, spare parts for aircraft components now being carried at many points around the world.

Fundamental Principles of Amplidyne Applications

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Synopsis: The Amplidyne generator has been widely applied and is recognized as an important device in the electrical industry. Since it is used primarily as a power amplifier in regulating systems, the requirements of an amplifier for this use are analyzed, and the characteristics of the Amplidyne generator are compared in this paper with these requirements.

Owing to the large number of distinct windings of the Amplidyne, it is desirable to follow a consistent practice in terminal markings and polarities. Proposals for this purpose are outlined in the paper, and typical values of amplification for different output ratings are given.

Service experience and an application typifying the use of the Amplidyne are discussed, so that the reader may become familiar with the device and its uses.

SINCE its introduction into the electrical industry, the Amplidyne generator has been found useful in a large variety of applications. The chief use has been in closed-cycle controllers or regulating systems as a power amplifier. In order to see why the Amplidyne is particularly adaptable and useful in regulating systems, it is desirable to review and analyze the characteristics of the regulating system to determine the requirements of an amplifier for use therein.

The Regulating System

A closed-cycle control system or regulating system is one in which the controlling agency is actuated by some function of the final output in such a manner as to minimize any deviation of the output from an ideal value.

There are three fundamental elements of such a system, namely:

1. A standard of ideal performance against which the output to be regulated can be compared.
2. An amplifier to amplify any deviation of output from the standard.
3. A means of feeding the amplifier output into the system in such a manner as to minimize the deviation.

Many factors need to be considered in devising a regulating system, and these have been outlined in a previous article.[1] It is not the purpose here to elaborate on these factors any more than necessary to establish a basis for analysis of the amplifier requirements in the system and to show how such an analysis acts as a guide in evaluating various proposed amplifiers for this use.

The first element of the regulating system, namely, the standard of performance, may be either a primary or a secondary standard and must be of like physical kind to the quantity to be compared with it. It is very often necessary to transform the quantity to be regulated into the same form as the standard for comparison. It can be readily understood that standards and transformations can most readily and economically be obtained at low power levels.

The third element of the system, the means of feeding the amplifier output into the system, may, and in most cases does, require a very considerable amount of power.

It is obvious that a high degree of power amplification is an important requirement for an amplifier to be used in a regulating system.

Stability is another primary requirement. It is beyond the scope of this article to establish mathematically the requirements for stability of the regulating system. Instability can be explained by the existence of a series of time delays in the system. In general, the greater the number of time delays in series and the higher the amplification the greater will be the tendency to oscillate. Also, in a system with three or more simple exponential time delays in series, the more nearly the time delays equal each other in magnitude the greater will be the tendency to oscillate. Since in the usual industrial power apparatus the time delay of the final controlled element is appreciable, it is generally important to reduce to a minimum the time delays in the system prior to the final one. From these considerations, the second desirable characteristic of the amplifier is as short a time delay as possible between input and output.

One thing that has not been generally recognized is that the closed regulating circuit affects the response time of the system as well as minimizing the deviation between the standard and the system output. In this respect, a regulating system is very similar to an inverse feedback amplifier.

Analysis of Simple Regulating System

Figure 1 illustrates a very simple regulating system with one time delay. For purposes of analysis, assume that up to a time $t=0$ the standard voltage has been 0 and is instantly made equal to E_S at time $t=0$. Furthermore, assume the amplifier and generator amplifications to be linear over the range of voltage considered.

Let

R_1 = generator field resistance
L_1 = generator field inductance
K_1 = generator armature voltage per field ampere
M_1 = amplifier amplification

$$T_1=\frac{L_1}{R_1}\text{ generator field time constant}$$

$$E_1=-M(E_R-E_S)=(R_1+L_1p)i_1$$

$$E_R=Ki_1$$

$$-ME_R+ME_S=(R_1+L_1p)\frac{E_R}{K_1}$$

$$\frac{E_R}{K_1}R_1(1+T_1p)+ME_R=ME_S$$

$$E_R=\frac{K_1ME_S}{R_1\left(\frac{MK_1}{R_1}+1+T_1p\right)}$$

Let

$$\alpha=\frac{K_1M}{R_1}=\text{over-all system amplification}$$

Then

$$E_R=\frac{\alpha E_S}{\alpha+1}\left(1-\epsilon^{-\frac{(\alpha+1)t}{T_1}}\right)$$

If we had merely applied a voltage E_1 to the field of the generator sufficient to bring the output voltage without the regulating system up to a value equal to E_R, the equation for E_R would have been

$$E_R=\frac{E_1{}^1K_1}{R_1}\left(1-\epsilon^{-\frac{1}{T_1}t}\right)$$

Thus, we see that the use of the regulating system not only minimizes deviation from the value of the standard but has reduced the time delay of voltage build-up or effectively decreased the time constant in the ratio of $\alpha+1$ to one.

This analysis assumes linearity of amplification, and it is to be noted that if the voltage output of the amplifier is limited,

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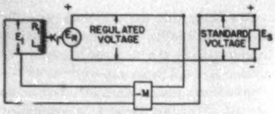


Figure 1. Simple regulating system

the response will be determined by the maximum voltage available at the generator field.

The Amplidyne

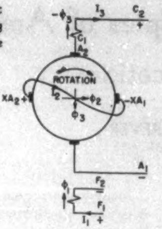
The Amplidyne has been described in detail in other articles.[2–4] It is here desired to point out the features of its design which make it outstanding in its performance.

A review of the operation is desirable to this end. Figure 2 shows a two-pole d-c armature with two pairs of brushes in quadrature. The frame is omitted from the diagram for simplicity, and the assumption is made that commutation is ideal. A flow of current I_1 in the control field F_1F_2 in the load axis creates a load-axis flux ϕ_1. The armature conductors cut this flux, and a voltage E_2 is generated in them to send current I_2 through the brushes of the short-circuit axis. The flow of armature current I_2 through the short-circuit axis of the armature develops a distributed magnetomotive force and flux ϕ_2 in the short-circuit axis. Rotation of the armature in this field generates a voltage E_3 at the load-axis brushes. The magnetomotive force of the armature caused by current I_3 is in opposition to the magnetomotive force of the load-axis control field. However, there is a distributed compensating field which neutralizes the armature magnetomotive force caused by current I_3. Only a very small magnetomotive force is required in the load-axis control field to cause generation of the required voltage and current in the short-circuit axis to give full output voltage at the load terminals. Only a small generated voltage is necessary in the short-circuit axis, and yet the full voltage of the armature is available for forcing a rapid rise or decay of flux ϕ_2.

Analogy and Analysis of Time of Build-Up

The Amplidyne combines into one machine two stages of amplification, each stage of which has a very small time delay. The first stage of amplification is from the load-axis control field to the short-circuit axis, and the second stage is from the short-circuit axis to the load terminals. For purposes of approximate analysis, it is as though two d-c generators were in series and with the first generator just as large as the second as regards voltage output capability. This is illustrated in Figure 3.

Figure 2. Schematic diagram illustrating operation of the Amplidyne



Let a unit voltage E_1 be suddenly applied to the control field.

Let $T_1=\frac{L_1}{R_1}K_1=$ volts E_2 generated per field ampere i_1

$K_2=$ volts E_3 generated per field ampere i_2

$$T_2=\frac{L_2}{R_2}$$

Then

$$E_1=(R_1+L_1p)i_1$$

$$E_2=K_1i_1=\frac{K_1E_1}{R_1+L_1p}=(R_2+L_2p)i_2$$

$$E_3=K_2i_2=\frac{K_1K_2E_1}{(R_1+L_1p)(R_2+L_2p)}$$

$$E_3=\frac{K_1K_2E_1}{R_1R_2(1+T_1p)(1+T_2p)}$$

The solution of this is

$$E_3=\frac{K_1K_2E_1}{R_1R_2}\left[1+\frac{T_1}{T_2-T_1}\epsilon^{-\frac{1}{T_1}t}-\frac{T_2}{T_2-T_1}\epsilon^{-\frac{1}{T_2}t}\right]$$

Figure 4 is a plot illustrating the voltage rise in such a circuit having constants as follows:

$T_1=0.058$ second $T_2=0.092$ second

These are values taken from a typical 1,500-watt 250-volt 1,750 rpm Amplidyne. The time delay T_1 is for a field occupying 25 per cent of the field coil space and capable of carrying continuously ten times the current required to excite the machine to 80 per cent voltage at rated load. The time delay T_2 is on a machine with a series field in the short-circuit axis which is connected in the armature circuit of the short-circuit axis.

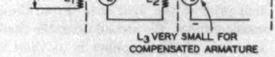

ANALOGOUS TO LOAD AXIS CONTROL FIELD
ANALOGOUS TO SHORT-CIRCUIT AXIS
ANALOGOUS TO LOAD AXIS
E_1
R_1
L_1
E_2
R_2
L_2
E_3
R_3
+
–
L_3 VERY SMALL FOR COMPENSATED ARMATURE

Figure 3. Two-machine equivalent of the Amplidyne

From this analysis, it is evident that the Amplidyne is an ingenious method of obtaining two-stage amplification with a fast response in a single unit. A number of independent load-axis control field windings can be used, so that the resultant control ampere turns will represent the algebraic sum of several independently selected functions.

Circuit Diagrams

To take full advantage of these possibilities and to avoid confusion in circuit diagrams, the symbols shown in Figure 5 have been found convenient. These symbols may be used as a guide for any combination of field windings. Connections of typical machines are shown in the diagram, and it can be seen that they are relatively simple. The only external connections are those of the various load-axis control fields and the power output leads.

Terminal Letters and Nomenclature

The same general rules are used for designating Amplidyne terminals as those in use for other d-c machines. The prefix X is added to the terminal markings of all windings which magnetize in the short-circuit axis, while the windings which magnetize in the load axis are marked without a prefix. See Figure 5 for detailed definitions of the various symbols.

Throughout the paper, the terms *load*

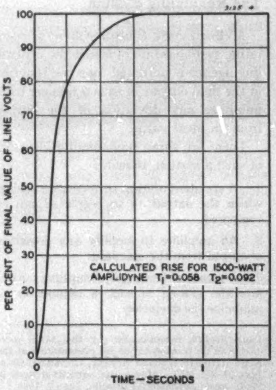


Figure 4. Response curve of typical Amplidyne generator

axis and *short-circuit* axis are used to describe the two axes. Other publications have also referred to the *load axis* as the *secondary axis* and the *direct axis*, and to the *short-circuit axis* as the *primary axis* and the *quadrature axis*.

Polarity

Amplidynes are so marked that for current which flows from the lower to the higher subscript terminal, in any of the control fields which magnetize in the load axis, the polarity of the terminal A_1 of the armature winding of the load axis is negative. This is true for both directions of rotation. Refer to Figure 5 for a detailed explanation of various windings.

Methods of Rating

Amplidyne generators are rated in kilowatts output, the product of rated voltage and rated current. It has been found desirable to select the voltage rating so that the saturation curve is essentially a straight line up to 80 per cent of rated voltage, and to provide a ceiling voltage approximately equal to 120 per cent of rated voltage. Typical saturation curves are shown in Figure 6. Usually, amplidyne generators are designed for 40 degrees centigrade temperature rise at rated voltage and current and to withstand a 200 per cent current peak momentarily at rated voltage. The machines should preferably be subjected to not more than 150 per cent rated current at rated voltage, tapering to 250 per cent at zero voltage.

The control field excitation requirements for typical 1,800-rpm Amplidyne-generator ratings between $^1/_2$ and 25 kw vary from 0.25- to 0.75-watt excitation (in one-fourth field space) with rated current output and 80 per cent rated volts; giving power amplifications (for one-fourth field space) from 2,500 to 40,000

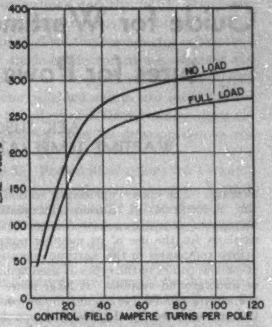


Figure 6. Typical saturation curves for 1,725 rpm 250-volt Amplidyne

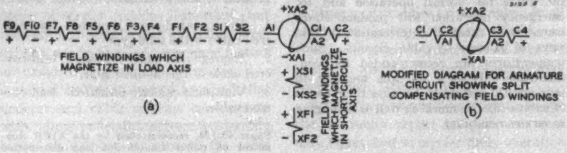

F9
F10
F7
F8
F5
F6
F3
F4
F1
F2
S1
S2
+
−
FIELD WINDINGS WHICH MAGNETIZE IN LOAD AXIS
(a)
A1
+XA2
−XA1
C1
C2
A2
+XS1
−XS2
+XF1
−XF2
FIELD WINDINGS WHICH MAGNETIZE IN SHORT-CIRCUIT AXIS
C1
C2
A1
+XA2
−XA1
C3
C4
A2
MODIFIED DIAGRAM FOR ARMATURE CIRCUIT SHOWING SPLIT COMPENSATING FIELD WINDINGS
(b)

Figure 5. General diagram for clockwise rotation of Amplidyne generators

1. Compensating field windings

The compensating field windings, C_1C_2, are always connected in series with the load axis of the armature winding. Frequently it is convenient to split the compensating field winding so that half is connected in the circuit on each side of the load axis of the armature winding, and the diagram is modified as shown in diagram *a*

2. Commutating field windings

If a commutating field winding is used it will be connected in the armature circuit between terminals *A*1-*A*2 or *XA*1-*XA*2

3. Field windings in the load axis

Each terminal of the field windings in the load axis is marked with a single letter and subscript number. Exciting current which flows from the lower to the higher numbered subscript in any one of these windings produces a magnetomotive force and flux along the load axis and a speed voltage in the armature winding of the short-circuit axis of such polarity as to make XA_2 positive and XA_1 negative with clockwise rotation of the armature. The use of the letters and subscripts has additional significance:

S_1S_2, S_3S_4, and so forth, field windings intended for series connection; usually in the armature circuit of either the load axis or the short-circuit axis

F_1F_2, main control shunt field

F_3F_4, reference or counter shunt field

F_5F_6, auxiliary control shunt field

F_7F_8, antihunt shunt field

F_9, F_{10}, F_{11}, F_{12}, and so forth, shunt fields for other purposes than those already noted

4. Field windings in the short-circuit axis

The terminals of the field windings in the short-circuit axis are marked the same as the field windings in the load axis except that the prefix letter *X* is added. Exciting current which flows from the lower to the higher numbered subscript in any one of these windings produces a magnetomotive force and flux along the short-circuit axis and a speed voltage in the armature winding of the load axis of such polarity as to make A_2 positive and A_1 negative with clockwise rotation of the armature

The use of the letters and subscripts has added significance:

XS_1XS_2, and so forth, field windings intended for series connection; usually in the armature circuit of either the short-circuit axis or the load axis

XF_1XF_2, and so forth, field windings intended for shunt connection

5. Killer windings

A killer winding is sometimes used in special cases to reduce the residual. This may be a special winding or it may be special taps on a winding designed for other purposes. In either event the terminals are marked K_1K_2. Since the exciting current is alternating, the polarity marking is omitted

6. Counterclockwise rotation

The same letters and subscripts are used for both rotations. For counterclockwise rotation the polarities of all field windings and of the armature winding of the load axis remain the same as for clockwise rotation. The polarities of all field windings and the armature winding of the short-circuit axis are reversed. All interconnections of the various windings are the same for both rotations; except that field windings in the load axis which are connected in the circuit of the short-circuit axis, and field windings in the short-circuit axis which are connected in the circuit of the load axis must be reversed when rotation is reversed

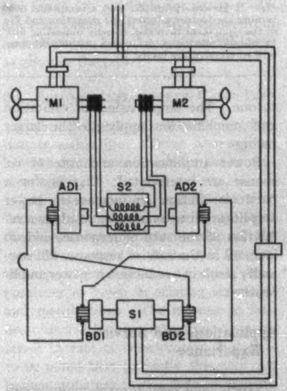


Figure 7. Schematic connection diagram with double motor drive using counterrevolving propeller

*M*1 and *M*2 = 22,000-horsepower slip ring 327-rpm fan motors

*AD*1 and *AD*2 = 2,500-kw d-c generators

*BD*1 and *BD*2 = 3,200-horsepower d-c motors

*S*2 = 20,000-kva 540-rpm synchronous motor

*S*1 = 6,000-kva 514-rpm synchronous generator to one. The larger values of excitation and amplification apply to the larger ratings.

Power amplification and rate of response are interrelated; that is, for a particular design an increase in power amplification will normally result in a reduction of the rate of response, and an increase in the rate of response will normally result in a reduction in power amplification.

Applications and Service Experience

There have been a great many applications of Amplidyne generators in regulating systems, some of which have now been in service from four to five years. Some typical applications were given in a previous article.[1]

The service record of these machines and their circuits has been excellent.

Typical of the type of regulation that can be accomplished is the application of Amplidynes to wind tunnel drives. The arrangement of a wind-tunnel-drive apparatus was described in a previous article.[5] Figure 7 shows the schematic connections of the main drive. In these circuits, the following functions were performed with Amplidynes and associated circuits.

1. Variation of field current of constant speed and variable speed d-c machines—*BD*1, *BD*2, *AD*1, and *AD*2 to

(*a*). Controlled acceleration of the variable speed machines from rest to full speed and matching of the line frequency for synchronizing.

(*b*). Controlled acceleration of the main fan motors from rest

(*c*). Maintain fan motor speed within one-fourth of one per cent.

(*d*). Under any condition impose a definite current limit in the circuit between the d-c machines.

2. Variation of field current of variable speed a-c generator S_2 to

(*a*). Match voltages during the synchronizing period.

(*b*). Maintain system power factor during operation of the tunnel.

Owing to the fact that these circuits involve matching the quantity to be regulated against a standard, it is essential that the proper polarities be predetermined and checked before the apparatus is given full power to regulate. In case of error of connections, the system might cause the value being regulated to depart from the standard with increasing voltage rather than bringing about the desired matching.

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1. INDUSTRIAL APPLICATIONS OF AMPLIDYNE GENERATORS, **D. R. Shoults, M. A. Edwards, F. E. Crever.** *General Electric Review*, volume 43, 1940, pages 114–19.

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3. DESIGN CHARACTERISTICS OF AMPLIDYNE GENERATORS, **A. Fisher.** *General Electric Review*, volume 43, 1940, pages 107–13.

4. STEADY-STATE THEORY OF THE AMPLIDYNE GENERATOR, **T. D. Graybeal.** AIEE TRANSACTIONS, volume 61, 1942, October section, pages 750–6.

5. LARGE ADJUSTABLE-SPEED WIND-TUNNEL DRIVE, **C. C. Clymer.** AIEE TRANSACTIONS, volume 61, 1942, March section, pages 156–8.

Guide for Wartime Conductor Temperatures for Power Cables in Service

AEIC SUBCOMMITTEE ON
WARTIME TEMPERATURES FOR CABLE CIRCUITS

Preface: The cable engineering section of the Association of Edison Illuminating Companies recently sponsored a guide particularly for the use of its member power-utility companies in the wartime operation of electric cables of the types as used mainly in underground systems. A large share of the underground cable circuits in this country are operated by the AEIC companies. The details of preparing the guide for the consideration, revision, and approval of the section were handled by a special subcommittee.

The AIEE cable working group sponsored a conference on emergency rating of power cables at the AIEE technical meeting on January 26, 1943, at which nine papers were presented.* Subsequently it seemed desirable to the cable working group to have the AEIC guide also presented for the purpose of giving information to all interested in this country and for stimulating discussion. The information and diversified views in the nine papers were considered in preparing the guide.

This guide is similar in two respects to wartime guides recently prepared by various AIEE committees and subcommittees on various kinds of equipment, namely:

(*a*). It presents principles and assumptions concerning the factors affecting the operation and life of the equipment involved, thereby indicating how to obtain maximum utilization within the prescribed temperature limits.

(*b*). It presents tables of temperatures for occasional emergency use which are above the limits in the AIEE Standards.

Furthermore, this Guide has tables of temperatures for normal operation, that is, day-in and day-out operation, where the temperatures are higher than the limits given in various specifications and standards, including the AIEE Standards. The usage of temperatures such as set forth in the guide for normal operation and for emergency operation will produce sharp increases in rates of deterioration and in rates of failures of cable circuits. Such temperatures are recommended for consideration only as a wartime measure to save critical materials, because their use is otherwise uneconomical as well as damaging to service reliability.

1. Scope

THIS guide has been prepared as an aid in determining reasonable cable operating temperatures for wartime service for both normal and emergency operating conditions. The aim has been to obtain the maximum practical utilization of materials and facilities consistent with maintaining tolerable rates of failure. Tables are presented showing recommended maximum temperatures for representative systems, but the values must be adjusted, in many cases, to take into account the various special conditions that are involved in specific situations. As an aid to the engineer who must make the decision as to the needed adjustments, a large part of the guide is devoted to a discussion of the principles on which the recommendations are based and to suggestions as to the adjustments appropriate for particular cases.

The recommendations cover four types of power cable used mainly in underground systems, namely:

1. Impregnated-paper-insulated lead-covered cable of the solid type.
2. Impregnated-paper-insulated lead-covered cable of the oil-filled type.
3. Varnished-cambric-insulated lead-covered cable.

Paper 43-104, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943. Manuscript submitted April 21, 1943; made available for printing May 20, 1943.

Personnel of the AEIC subcommittee: Herman Halperin, *chairman;* C. T. Hatcher, and H. W. Collins.

Personnel of the AEIC cable engineering section: W. F. Davidson, *chairman;* G. M. Armbrust, T. J. Brosnan, W. R. Bullard, H. A. Clark, M. T. Crawford, G. E. Dean, R. L. Dodd, F. M. Farmer, C. W. Franklin, T. H. Haines, Herman Halperin, L. I. Komives, C. H. Kraft, S. J. Lisberger, R. E. Morse, H. S. Phelps, F. E. Pinckard, and C. T. Sinclair.

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* A single pamphlet containing all nine papers is available in limited quantity at Institute headquarters at 75 cents a copy.

4. Rubber-insulated cable with and without lead sheath.

This guide is not intended to supersede existing standards and recommended practices for operating temperatures under peacetime conditions of service. Further, it is recognized that if these higher temperatures are used the manufacturer cannot be held responsible under the usual guarantees in standards and specifications for troubles resulting from operation at the increased temperatures.

2. General Principles

Under wartime conditions, the normal balance between many factors that enter into the determination of proper cable operating temperatures is upset. Maintenance of service remains important, but the very high standards of peacetime service may not be justified; economic factors are assigned only small consideration. As a general matter it may be assumed that underground cable systems can operate with failure or trouble rates considerably higher than those reported in the cable operation reports of the Edison Electric Institute without materially affecting the reliability of service to the customers. Some increase in maintenance cost is to be expected, but this is partially offset by the increased use of the investment. Some reduction in life is to be expected; the extent of this is indicated in the assumptions.

Establishment of increased temperatures and increased load ratings under these changed conditions requires a large measure of engineering experience and judgment. However, it is practical to make recommendations as to temperatures for wartime conditions for representative systems and to give sufficient discussion for use in making adjustments in the temperatures as dictated by circumstances peculiar to specific systems.

2.1 Type of System

The recommended temperatures given in Tables I, II, and III apply to representative systems meeting three general conditions. These are:

(*a*). The cable in service was purchased almost entirely under the following standards and specifications: "AEIC Specifications for Impregnated Paper-Insulated, Lead-Covered Cable, Solid Type" (the "Simplified Practice Schedule" issued in 1941 not being included); "AEIC Specifications for Impregnated Paper-Insulated Lead-Covered Cable, Oil-Filled Type"; "AIEE Standard Number 30," issued at various times; "Insulated Power Cable Engineers Association Specifications for Varnished Cambric-Insulated Cables" (the 1939 edition which applies to the recently developed varnished-cambric tapes not being included); the "National Electrical Code," and "ASTM Standard Number 469" for rubber-insulated cable.

(*b*). The system had rates of trouble of the same general order as the national rates of trouble as given in the "Cable Operation Reports" of the Edison Electric Institute. The cable and accessories have no especially bad condition that would lead to a very large increase in troubles with the use of higher temperatures.

(*c*). The power cable systems involved will have reasonably good maintenance, which includes periodic inspections in manholes and repairs when found necessary.

2.2 Temperatures—Definitions

2.21 The temperatures referred to are those at the hottest portion of the cable in a given line or circuit. Where these limiting temperatures exist in only a small portion of the line, attention should, obviously, be given to a determination of the extent to which corrective measures may be justified. (Section 3.8)

2.22 The "normal" temperatures are the maximum copper temperatures that may be reached in regular daily operation during wartime when all of the lines or circuits for a given supply are in service. Where, for example, the daily load cycles for the first five days of the week are practically the same and then the load drops considerably over the week end, then the suggested maximum allowable normal temperature would apply to the conditions that produced the maximum temperature during the week.

2.23 The "emergency" temperatures apply to special operation occurring not more than four periods in any 12 consecutive months for all of the types of cable except the oil-filled type, for which the limiting number of periods is two in any 12 consecutive months. The duration of the special operation is assumed to be a continuous period of time of 24 hours or less for all the types of cable except for the oil-filled type, for which it is assumed that each period is 60 hours or less.

2.24 It is assumed that, both in normal and in emergency operation, the cable is at approximately the maximum temperatures for a few hours or more in each daily load cycle, which is practically the condition when lines or circuits have their daily heavy-load periods for eight or twelve hours or more.

2.3 Recommended Temperatures

2.31 For impregnated-paper-insulated cables of the solid type the temperatures for normal operation are from 4 to 15 degrees and for emergency operation from 10 to 40 degrees centigrade higher than the temperature limits given in the AEIC Specifications and the AIEE Standards. Lower temperatures are shown in Table I for shielded three-conductor cables than for single-conductor cables, because the maximum temperatures for three-conductor shielded cables are more limited by the effect of cable movement upon the sheath life than by deterioration of the insulation.

2.32 For oil-filled cables the temperatures for normal operation are ten degrees centigrade above the limits given in the AEIC Specifications except for voltages below 26 kv where the increases are smaller, and the proposed emergency temperatures are from 25 to 30 degrees centigrade above the AEIC limits for emergency operation except for voltages below 26 kv.

2.33 For varnished-cambric cables the proposed normal temperatures are from 8 to 14 degrees centigrade above the limits given in AIEE Standard 30, and the proposed emergency temperatures are from 23 to 31 degrees centigrade above those limits.

2.34 For rubber-insulated cables except those with ozone-resistant compounds, the proposed normal temperatures are from 5 to 20 degrees above the limits given in the 1940 National Electrical Code and the proposed emergency temperatures are from 15 to 40 degrees centigrade above those limits. No standard limits are available for ozone-resistant compounds, but general values from manufacturers have been about the same as given in the table.

2.4 Assumed Influence on Cable Life

Higher temperatures result in reduction in cable life. Three assumptions have been made as a basis for the recommendations.

2.41 It is assumed that the wartime "normal" temperatures will at least double the rate of deterioration of the cable as compared with the rate for previous practices based on "normal" temperatures as given in existing standards and specifications. The increase in failure rate will be one indication of an increase in rate of deterioration, but it is to be noted that generally this may not become apparent in less than six months or a year.

2.42 It is assumed that wartime "emergency" temperatures would cause a loss of life, for each period they are used, of less than one per cent of the expected life of the cable and accessories when they were new, and that they would cause no immediate failures.

2.43 The increases in the rate of deterioration for normal operation and in the loss of life of one per cent or less for emergency operation are meant to apply to either the insulation or the sheath as the case may be. It should be recognized that in many cases cable systems have operated at temperatures considerably below the limits given in national standards. In such cases the use of the recommended temperatures in this guide and their accompanying increased temperature ranges may cause increases of 200 or 400 per cent or even more in the rates of deterioration over the rates that previously obtained. The suggested maximum allowable temperatures for wartime operation are based on a large amount of experimental data and operating experience. "Special Considerations" discusses this at some length for a number of cases, while there is more extensive discussion in several papers listed in the bibliography.

3. Special Considerations

3.1 INFLUENCE OF OPERATING VOLTAGE

Temperatures are given in the tables for various nominal operating voltages and were determined by the formulas shown. For other nominal operating voltages, the suggested allowable temperatures can be determined by interpolation from the temperatures in the tables or by the use of the formulas.

3.2 LEAD SHEATH

3.21 For normal temperatures which apply to day-in and day-out operation, one of the main factors affecting the determination of the temperature for lead-sheathed cable is the effect of daily movement of the cable in the manhole incidental to cyclic loading. Such movements may lead to cracking through the sheath before the insulation becomes unserviceable. The temperatures given in the tables are based on the assumption that the daily load varies considerably through each 24-hour period and there is one general movement of the cable in and out of the duct in each 24 hours. If the loading is such as to have two general cycles of movement back and forth, then the temperatures should be reduced in order to avoid considerable cracking through the sheaths in the manholes. On the other hand, if the loading is fairly steady through each 24-hour period, then this factor may permit the use of higher normal temperatures.

3.22 In connection with sheath cracking in manholes incidental to daily cyclic loading, another factor is the training of the cable in the manhole as well as the size of manhole. Where the offset between the center line of the duct and the center line of the line is small, or the bending radii of the cable are small, or the total length between joint wipe and duct mouth is small, then the effect of a given movement of the cable at the duct mouth in causing sheath cracks is increased. Where these characteristics are particularly below what may be considered good practice, then there should be a corresponding reduction made in the allowable temperatures, especially where the daily temperature ranges are large.

3.23 The amount of cable movement at the duct mouth is affected by the length of cable between manholes, within limits. For short lengths of cable, such as 100 or 200 feet, the allowable copper temperatures might be set somewhat higher than given in the tables as far as considerations of the lead sheath are concerned.

3.24 For a given length of cable and temperature range, the amount of movement at the duct mouth is somewhat proportional to the ratio of the total cross section of the copper conductors in the cable to the weight of the cable. For a cable having relatively small conductors, the temperatures might be set at somewhat higher values than given in the tables.

3.25 The strain of the sheath in the manhole incidental to a given movement at the duct mouth is somewhat proportional to the diameter of the cable. Where the cable diameters are considerably more than about one and one half or two inches for low-voltage cables or about two or three inches for high-voltage cables, then the maximum temperatures for normal daily loading might well be less than given in the tables. Where the cable diameters are particularly small, then higher temperatures than given in the tables may be permissible for normal operation.

3.26 Impregnated - paper - insulated cable installed vertically or on steep slopes may be subject to excessive sheath expansion from the internal hydrostatic pressure. The expansion of the lead sheath is accelerated by the rise in temperature of the sheath. For practical purposes it is well to assume that the rate of creep doubles for an increase of about 17 degrees centigrade.

3.27 Where solid-type cables are operated with reservoirs under positive pressure connected to joints, consideration must be given when increasing operating temperatures to the possibility of increased sheath expansion and an increased rate of sheath bursting. Reduction of reservoir pressure may be considered as a compensating measure.

3.28 In locations where corrosion is a problem, increased sheath temperatures may increase the rate of chemical reaction and failures resulting from corrosion.

3.3 CONDUIT

3.31 Another important factor related to the use of higher temperatures for normal operation is the effect of the accompanying increase in conduit temperatures. In some cases the use of the increased temperatures will result in drying out the conduit and surrounding soil with a resultant increase in the heating constant, that is, the thermal resistance between the cable and base ambient, which is unheated earth some distance from the conduit for underground installations. As regards temperatures for emergency operation, the main point on the heating of the conduit is the fact that the tem-

Table I. Maximum Conductor Temperatures for Impregnated Paper-Insulated Lead-Covered Cable for Wartime

Caution: Values in tables are not to be used without full consideration of limitations discussed in text

Nominal Operating Voltage—Kilovolts (Phase-to-Phase)	Maximum Conductor Temperature—Degrees Centigrade	
	Normal Operation	Emergency Operation
Single-Conductor Cable, Solid Type		
0–1	100	125
4	98	121
13	92	113
22	87	105
27	84	100
33	81	95
44	75	84
66	64	70
Three-Conductor Shielded Cable, Solid Type		
13	87	108
22	82	100
27	79	95
33	76	90
Multiple-Conductor Belted Cable, Solid Type		
0–1	98	125
4	94	115
13	85	104
22	76	94
27	71	88
33	65	80
Oil-Filled Type of Cable		
35 and less	85	115
66	85	110
132	80	105

Rules for establishing temperatures shown were as follows:

Type of Cable	Normal Operation	Emergency Operation
Single-conductor, solid	$100-E$ (100 degrees centigrade for 0–1 kv and minimum of 64 degrees centigrade)	$125-1.6E$ (125 degrees centigrade for 0–1 kv and minimum of 70 degrees centigrade)
Three-conductor shielded, solid	$95-E$	$120-1.6E$
Multiple-conductor, belted, solid	$98-E$ (98 degrees centigrade for 0–1 kv)	$120-1.2E$ (125 degrees centigrade for 0–1 kv)

E is kilovolts between phase and ground for single-conductor and three-conductor shielded cables and between phases for multiple-conductor belted cables.

perature of the conduit may go up a few degrees for each 24 hours of emergency loading.

3.32 In all calculations as to carrying capacities for a given installation or installations of cable circuits, certain assumptions are made as to the effect of the heating of the cables in raising the temperature of the conduit or air surrounding the cable. If the assumptions are based on factual data obtained by surveys of the conduits and the installations are further followed up by surveys after the circuits are in operation, then the copper temperatures can be calculated with a good degree of accuracy. In such cases the load ratings may be determined without making especially liberal allowances for the temperature rise of the duct or other ambient over the base ambient. If surveys of conduits are made where the cables are operating at relatively high temperatures, then the load ratings can be made somewhat higher than would be the case if no surveys are made. If no factual data are available on the heating characteristics of the medium in which the cable is to be installed and no surveys are to be made, then the temperature that is used in calculating the load ratings might well be five or ten degrees centigrade less than given in the tables. On the other hand, where complete data are available and field surveys are continually made, it may be feasible to use temperatures of a few degrees up to ten degrees centigrade above the limits given in the tables for paper-insulated cables, the larger increases applying to the lower-voltage cables.

3.33 The total carrying capacity of all the cables in a conduit, expressed in kilovolt-amperes, may be increased in some cases by relocating some of the relatively lower-voltage cables into other conduits. Although the increase in current-carrying capacity for the higher-voltage cables might be small, the increase in kilovolt-amperes might be rather large.

3.4 INSULATION

3.41 For, particularly, impregnated-paper-insulated cable made prior to 1920 and having high dielectric losses, and for varnished-cambric-insulated cable used at about nine kv and higher, the existence of large variations in dielectric loss, especially at the higher temperatures, should be recognized in calculating the carrying capacities. Where the information on dielectric losses throughout the expected temperature range of operation for a particular kind of cable is not available, then reasonably large allowances should be made for the dielectric losses or the temperature limits used should be somewhat less than given in the tables for both normal operation and emergency operation. Special attention should be given to the possibility of failures resulting from thermal instability for old cables with high dielectric losses.

3.42 Another effect of the temperature range is in the production of ionization in the solid-type paper-insulated cable. For old belted-type cable such as made in the period of 1920–30 with somewhat less insulation than used prior to 1920, the effect of ionization in normal operation may be considerable in some instances. If the operating experiences have indicated this to be the case, a small reduction below the temperatures given in the table for normal operation should be made, and a somewhat larger reduction should be made in temperatures for emergency operation.

3.43 Cable that has operated at temperatures corresponding to voltages much below its rated voltage may become unfit so far as the insulation is concerned to operate subsequently at rated voltage.

3.44 The insulation of recently made paper-insulated cable of the solid type can withstand somewhat higher temperatures for emergency operation than given in the table.

3.45 Varnished - cambric - insulated cable made in the past few years can withstand temperatures roughly five degrees centigrade higher than given in the table for both normal and emergency operation.

3.46 It should be noted that the resistance to effects of heat by rubber compounds of a given class has been found to vary considerably. If test or operating data are available for a given installation, the data may be used to modify the temperatures in the guide somewhat up or down.

3.47 Deterioration of rubber is greatly accelerated by oxidation. As a consequence, higher permissible temperatures are shown in Table III for lead-sheathed cable than for cables without lead sheath, except for ozone-resistant compounds, where the maximum temperatures are limited by softening of the insulation. Where the ends of lead-sheathed cables are of such construction as to allow air to be more or less in free contact with the rubber insulation, then, excepting ozone-resistant compounds, somewhat lower temperatures than given in the table may be advisable.

3.5 ACCESSORIES

3.51 In some instances the loading of the cables in emergencies to the temperatures given in the tables will result in movements at the duct mouth of one and one half or two and one half inches or even more. Training of the cable in the manhole should be such as to permit these large movements without buckling of the cable or jamming of the joint against the manhole wall or other joints or undue mechanical strains on connections to the joints to cause serious damage. If this is not feasible, then lower temperatures than given in the tables for emergency operation are suggested.

Table II. Maximum Conductor Temperatures for Varnished-Cambric-Insulated Lead-Covered Cable for Wartime

Caution: Values in table are not to be used without full consideration of limitations discussed in text

Nominal Operating Voltage—Kilovolts (Phase-to-Phase)	Maximum Conductor Temperatures—Degrees Centigrade	
	Normal Operation	Emergency Operation
Three-Conductor Shielded and Single-Conductor Cable		
0–1	88	105
4	85	102
13	77	94
22	70	86
27	70	82
Multiple-Conductor Nonshielded Cable		
0–1	88	105
4	82	99
13	70	85

Rules for establishing temperatures shown were as follows:

Normal Operation	Emergency Operation
$88 - 1.5\ E$ (88 degrees centigrade for 0–1 kv and minimum of 70 degrees centigrade)	$105 - 1.5\ E$ (105 degrees centigrade for 0–1 kv)

E is kilovolts between phase and ground for single-conductor and three-conductor shielded cables and between phases for multiple-conductor nonshielded cables.

3.52 In some cases joints and potheads may develop troubles if operated at the emergency conductor temperatures given in the tables. This applies particularly to the higher-voltage cables. In general, however, the accessories on an underground system operate with ambients 15 or 25 degrees centigrade below the maximum duct temperature along the circuit at the time of emergency operation, and so the possibility of trouble with the accessories in such instances is thereby reduced or eliminated. The point, however, is of significance where the cable and accessories are all exposed to the same ambient, as might be the case with an open installation of a circuit in a generating station.

3.53 Where joints are filled with viscous or hard compound, increased tempera-

Table III. Maximum Conductor Temperatures for Rubber-Insulated Cable in Wartime

Caution: Values in table are not to be used without full consideration of limitations discussed in text

Nominal Operating Voltage—Kilovolts	Maximum Conductor Temperature—Degrees Centigrade			
	Normal Operation		Emergency Operation	
	No Lead Sheath	Lead-Sheathed	No Lead Sheath	Lead-Sheathed
Code and Intermediate Compounds				
0–1	60**	70*	70	90*
30 Per Cent Class *AO*, Performance, and Other Similar Compounds				
0–7.5	65	75*	75	95*
Ozone-Resistant Compounds (Oil Base)				
0–7.5	75	75	85	85
7.6–15	70	70	80	80

* Terminal ends of cable assumed to be sealed against entrance of air.

** These temperatures are based on the 1940 National Electrical Code, which gave 50 degrees centigrade for code rubber together with Interim Revision Number 41 dated February 26, 1942, which permitted the use of the higher current ratings given in the previous editions of the code.

tures may cause trouble because of bulging of the casing or because of repeated bulging and collapsing of the casing.

3.54 Reservoirs may be subjected to volume expansion to beyond their limits when cables are operated at some of the higher temperatures given for oil-filled cables, and for those installations of solid-type cable having oil-filled reservoirs connected to the oil-filled joints. Therefore, special provisions should be made to take care of extra expansion of the impregnating compound or oil of the cable where the present reservoirs are inadequate.

3.55 Consideration must be given for oil-filled cable installations to the fact that dropping of heavier loads corresponding to higher temperature limits will result in hydrostatic pressure drops in the cable system greater than contemplated in the original design of the oil supply system. Provision must be made to prevent pressures below atmospheric forming at any point of the line under these conditions.

3.6 Load Cycles

As indicated under "General Principles" the recommended temperatures are based on the assumption that the cable may operate at about these temperatures for a few hours or more in a day. The heavy portion of the load of a given day may be of such short duration that no stable temperature conditions are attained and the maximum temperature is reached only momentarily. In such an event, if the temperature before the time of heavy load is considerably below the permissible temperature in the tables, then the maximum temperature could be five or ten degrees centigrade above the value in the tables, the higher temperatures applying to the lower voltages. This statement applies to temperatures during normal or emergency operation as the case may be.

3.7 Inspection

It is recommended that when lines or circuits are operated at temperatures such as given in the tables, the cable systems be given increased inspection. In the case of emergency operation the inspection should be made with little delay.

3.8 Special Measures

As previously indicated, the utilization of lines and circuits already in service can be increased within the allowable temperature limits in this guide through certain practices and arrangements, some of which are listed below:

1. Temperature surveys.

2. Increased inspection and maintenance program.

3. Changing solid bonding to special bonding for single-conductor cables to eliminate sheath losses.

4. Forced cooling by water or air during special temperature peaks at especially hot spots.

5. Elimination of local thermal bottlenecks by

(*a*). Use of larger conductors.

(*b*). Change of soil around conduit, as by replacing cinder fill.

(*c*). Relocating sources of external heat.

(*d*). Spreading of cables in crowded location.

6. Relocation of one or two cables to permit an increase in kilovolt-ampere loading of the remaining cables above the load carried on the removed cables.

7. Improvements in training of cable in manholes.

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Interim Report on Overloading Current-Limiting Reactors

AIEE TRANSFORMER SUBCOMMITTEE

Preface: The present war emergency requires that the maximum use be made of existing equipment and systems and that a minimum of critical material be used for new equipment.

This publication, as well as other guides and reports in this series, has been prepared for the information of users during the war emergency. Upon termination of the war emergency, they will be reconsidered by the Standards committee and the committees that prepared them, and will be approved, revised for normal use, or rescinded.

This procedure is being followed in preference to the preparation of special emergency Standards which might involve redesigning and drastic changes in manufacturing practices. These guides will accomplish the maximum conservation of critical materials, since they provide for the maximum use of existing equipment and systems, as well as new equipment, without changing the fundamental basis on which the present Standards have been prepared.

THIS report presents a general guide for loading current-limiting reactors above rating where specific design and operating data suitable to determine such loading are not available. It should be used in conjunction with the "Interim Report on Guides for Overloading Transformers and Voltage Regulators," presented on June 22, 1942, by the AIEE transformer subcommittee at the summer convention, which appeared in the September 1942 issue of ELECTRICAL ENGINEERING, as many data included there are not repeated in this report.

Temperature Limits

The present Standards for reactors provide that the temperature rise of the hottest spot of the windings shall not exceed 65 degrees centigrade for oil-insulated reactors and for dry-type reactors with Class *A* insulation or 90 degrees centigrade for dry-type reactors with Class *B* insulation, and that they shall be suitable for continuous operation at rated load, provided that the ambient temperature does not exceed 40 degrees centigrade and the daily average ambient temperature does not exceed 30 degrees centigrade.

This means that the total temperature of the hottest spot of the windings should not exceed a daily average of 95 degrees centigrade for oil-insulated reactors and dry-type reactors with Class *A* insulation or a daily average of 120 degrees centigrade for dry-type reactors with Class *B* insulation.

Reactors built to the present standards are often installed in compartments with restricted ventilation where the temperature of the reactor may be 10 to 15 degrees centigrade higher than when installed in the open. Under such conditions, dry-type reactors with Class *A* insulation are applied and operated under conditions that may result in an average daily hot-spot temperature of 105 degrees centigrade, and dry-type reactors with Class *B* insulation are applied and operated under conditions that may result in an average daily hot-spot temperature of 135 degrees centigrade.

Because of the aforementioned experience, it is undoubtedly safe to operate at these increased temperatures during this period. In order to prevent confusion in the case of reactors not enclosed in compartments, the ambient temperature is intended to mean the temperature of the air at a sufficient distance from the reactor not affected directly by the losses of the reactor. Likewise, in the case of reactors enclosed in compartments, it is not intended to refer to the temperature within the compartment but to the air surrounding the compartment.

Effect of Ambient Temperature on Continuous Loading for Normal Life Expectancy

The effect of ambient temperature on the loading of reactors is as follows:

If the daily average ambient temperature is below the reference of 30 degrees centigrade ambient, the loading may be increased, and, if the daily average ambient is above the reference ambient, the loading should be decreased as follows:

	Correction Per Cent Per Degree Centigrade Difference From the Reference
For oil-immersed reactors	1.0
For dry-type Class *A* reactors	0.75
For dry-type Class *B* reactors	0.5

Effect of Load Factor on Loading for Normal Life Expectancy

LOAD FACTOR

For daily load factors below 100 per cent, the loading may be increased 0.3 per cent for each per cent that the load factor is below 100 per cent, with normal life expectancy. Corrections in greater detail, based on an actual load curve, are not usually justified for normal loading. In no case should the overload permitted by this factor exceed 15 per cent, corresponding to 50 per cent daily load factor.

Paper 43-109, recommended by the AIEE committee on electrical machinery for presentation at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943. Manuscript submitted May 21, 1943; made available for printing June 6, 1943.

Personnel of the transformer subcommittee: M. S. Oldacre, *chairman;* F. S. Brown, E. S. Bundy represented by R. T. Henry, J. E. Clem, I. W. Gross, V. M. Montsinger, J. R. North, W. C. Sealey, F. J. Vogel, C. F. Wagner.

This interim report was prepared by the AIEE transformer subcommittee of the committee on electrical machinery for the purpose of making essential information immediately available to war industries, thus furthering the conservation of valuable material for the war emergency. It is educational and in no way mandatory. It is not intended as a "Standard," and has not been formally approved by the Standards committee nor the board of directors.

Emergency Short-Time Loading for Normal Life Expectancy

The following paragraph and Tables I and II indicate the short-time loads that can be carried without affecting normal life expectancy in 30 degrees centigrade daily average ambient.

OIL-IMMERSED REACTORS

Oil-immersed reactors may be overloaded under emergency conditions in accordance with the curves given in American Standard *C*57.3, "Guides for Operation of Oil-Immersed Transformers."

Emergency Loading With Moderate Sacrifice of Life Expectancy

Tables III, IV, and V indicate the emergency overloads that can be carried with moderate sacrifice of life, probably not exceeding one per cent for each application, in 30 degrees centigrade daily average ambient.

Use of Other Corrections With Overload Values Given in Tables

Care should be taken that the percentage change from each of the various causes be based on the reactor current rating and not on the increased loading resulting from any of the other corrections.

The effects of the various following factors may be added directly:

For normal operation with normal life expectancy the effects of load factor, ambient temperature, and supplemental cooling may all be added in determining the load capability.

Table I. Dry-Type Class *A* (Enclosed in Compartment)*

Duration of Load (Hours)	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current		
		Following Full Load	Following Three-Quarter Load	Following No Load
1	120	1.21	1.40	1.65
2	115	1.10	1.17	1.27
4	110	1.04	1.06	1.08

* See footnote for Table V.

Table II. Dry-Type Class *B* (Enclosed in Compartment)*

Duration of Load (Hours)	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current		
		Following Full Load	Following Three-Quarter Load	Following No Load
1	150	1.15	1.35	1.60
2	145	1.07	1.15	1.25
4	140	1.03	1.05	1.07

* See footnote for Table V.

For short-time emergency operation with either normal life expectancy or moderate sacrifice of life, the effect of the actual ambient temperature during the emergency and of supplemental cooling may be added to the value for the emergency operation.

This method of adding corrections is not rigorously correct, but it is sufficiently correct for practical purposes and is simple to apply.

Supplemental Cooling

The design of dry-type reactors is such that forced-air cooling may be readily applied, provided sufficient space is available for the cooling equipment. For best results, the flow of air should pass over all the windings of the reactor. The permissible load can be increased in some cases as much as 30 per cent for the same temperature rise. The efficiency of forced-air cooling varies, and each application should be carefully investigated to determine the permissible increased loading.

Table III. Oil-Immersed Reactors

Duration of Load (Hours)	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current	
		Following Full Load	Following No Load
1	137	1.9	2.00
2	130	1.65	2.00
4	125	1.45	1.7
8	120	1.30	1.4
24	110	1.15	1.15

Table IV. Dry-Type Class *A* (Enclosed in Compartment)*

Duration of Load (Hours)	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current		
		Following Full Load	Following Three-Quarter Load	Following No Load
1	137	1.40	1.60	1.80
2	130	1.25	1.30	1.40
4	125	1.15	1.16	1.18
8	120	1.10	1.10	1.10

* See footnote for Table V.

Table V. Dry-Type Class *B* (Enclosed in Compartment)*

Duration of Load (Hours)	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current		
		Following Full Load	Following Three-Quarter Load	Following No Load
1	160	1.25	1.45	1.70
2	155	1.15	1.20	1.3
4	150	1.08	1.10	1.12
8	145	1.05	1.05	1.05

* Dry-type reactors with both Class *A* and Class *B* insulation installed in the open with three or more feet of head room will carry 7.5 per cent more load than when enclosed in compartments. In all cases where these conditions are not met the extra overload should be obtained from the manufacturer.

Cautions and Limitations

It must be recognized that overloads should not be applied to current-limiting reactors without a thorough study of the various limitations involved. If reactors are operating at higher than standard temperatures, the permissible duration of short-circuit may be reduced slightly. Among other limitations are: soldered connections, leads, effects of increased magnetic fields on surrounding equipment, as well as the thermal ability of connected equipment.

Before overloading current-limiting reactors to the full extent covered in this report, it is recommended that overload capabilities of the reactor be checked with the manufacturer.

Design Relationships for D-C Generators for Use in Aircraft

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Synopsis: The paper sets forth the different operating conditions met with in aircraft generators as compared with standard types of industrial generators. It discusses certain features inherent in applications to aircraft, such as reliability and light weight. Certain requirements in both electrical and mechanical design are imposed by the fact that a plane is flying through varying altitudes resulting in changing atmospheric conditions, such as rarefication, low temperature, and ionization. Other unusual conditions are created from the motion of the plane setting up accelerating forces on the generator, as well as mechanical forces caused by engine vibrations. Because of important features such as commutation, voltage regulation, cooling, driving means, and light weight, special and unusual methods must be employed. In solving these problems the most perfect methods known in the art of building d-c machines must be resorted to, and the paper lays particular stress on the use of compensation.

Main and Accessory Engine-Driven Generators

THERE are two different methods of driving the generator:

1. The generator is driven from the main engine, in which case it is bolted to the engine and splined to a driving gear inside the engine. In some cases the generator is located in the nacelle and driven through a shafting containing two universal joints and a telescoping shaft so as to take up engine vibrations.

2. The generator is directly connected to an accessory engine. In this case the generator must be designed with relatively high efficiency so as to minimize the weight of gasoline consumed by the accessory engine.

The efficiency of main engine-driven generators runs between 70 and 80 per cent for different sizes (see Figure 3), and those driven by accessory engines should have an efficiency of from 85 to 90 per cent. The limitation of the application to accessory engines lies in the fact that they lose power quite rapidly with altitude since no satisfactory supercharger for this type of engine has as yet been developed.

Weights

Low weight is the natural requirement of all parts in airplanes. The weight of d-c generators for aircraft has been reduced to about one tenth of that of industrial generators of the same rating, and this has been accomplished with only a slight suffering in the efficiency. As an example: A d-c airplane generator rated 30 volts, 300 amperes, 4,400/10,000 rpm weighs 45 pounds—that is, five pounds per kilowatt—and has an efficiency of 77 per cent; an industrial-type generator of the same kilowatt rating at 1,750 rpm weighs about 400 pounds—that is, 45 pounds per kilowatt—and has an efficiency of 80 per cent. However, the airplane generator has at full load a temperature rise of 100 degrees centigrade employing an insulation consisting of glass and mica; whereas the industrial type of generator has a 40 degrees centigrade rise, using mainly cotton as a base insulation.

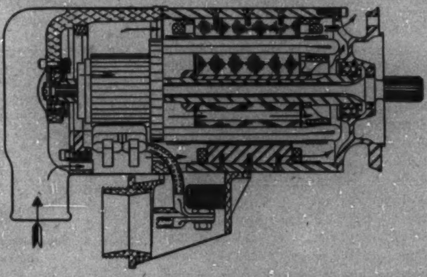

Figure 1. Assembly of d-c aircraft generator showing blast cooling

Future developments will be along the lines of better heat-resisting insulation. Varnishes have already been developed which will stand 200 degrees centigrade continuously without deterioration. Taking advantage of these higher temperatures, we may expect a further reduction in weight.

Influence of Atmospheric Conditions

The atmospheric conditions influence the cooling of the generator, affect the wear of the brushes, and because of rarefication and ionization influence insulation and creepages.

In the region in which the engine-driven generator is installed between the rear of the engine and fire wall, the ambient temperature is rather high, varying from 35 degrees centigrade to 65 degrees centigrade, and remains independent of the altitude. Since the density of the air decreases with the altitude, the cooling decreases in all self-ventilated generators. The permissible output is, therefore, reduced with the altitude and at a certain point, usually within the range of the plane, it becomes zero. Therefore, self-cooled engine-driven d-c generators are not advocated. Another type of cooling system consists of scooping the air from the outside of the plane and forcing this air through the generator. Since this cooling air has a very much lower temperature at high altitudes, it compensates for the decreased density. Investigation shows that the permissible output remains constant independent of the altitude. Blast cooling has, therefore, been generally adopted. In Figure 1 is shown how the ventilation is accomplished in a blast-cooled d-c generator.

The influence of the atmospheric conditions at high altitude on the wear of the brushes is a serious one. While considerable progress has been made in the solution of this problem, it is not as yet fully solved. It is a well-known fact that a good commutator which has operated for some time at sea level builds up a glossy film, the surface of which consists mainly of carbon. At high altitudes, ordinary brushes have been found to wear down quite rapidly. Experiments undertaken in altitude chambers have shown this effect is related to the reduced moisture and oxygen content of the air, but special brush types have been found which give longer brush life. It has also been found that a cool commutator

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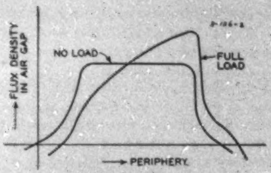

Figure 2. Flux distribution of a noncompensated d-c generator

and brushes are beneficial to brush life. Perfect commutation is important since it contributes to low temperatures. While it is difficult to give any definite rules for the design of commutators and brushes to give satisfactory brush wear, it may be stated that if the temperatures of commutator and brushes are limited to 100 degrees centigrade rise at sea level, and if the current density in the brush contact is also limited to less than 120 amperes per square inch, a reasonable brush life may be expected, provided that proper brushes are used.

The rarefication of the atmosphere at altitude in combination with ionization influences the breakdown voltage of the air. Between small spheres the breakdown voltage is proportional to the air density, for example, at 30,000 feet it is about one third of that at sea level. The ionization within the range of flight is comparatively small, and its influence on the sparkover voltage is probably quite small. However, if an arc is started, then, because of the great mobility of the charged particles, intense ionization will take place, and the arc will extend itself more quickly than at sea level. Therefore, it is quite important that no arcs or sparks are started. This requires three conditions to be fulfilled:

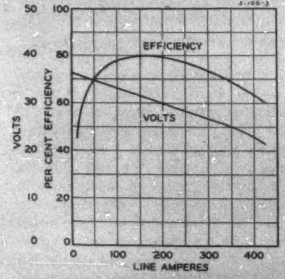

Figure 3. Regulation and efficiency curves of a compensated d-c generator

1. The commutation should be perfect.
2. The brush rig should be so designed as to prevent the sudden lifting of the brushes from vibrations.
3. Sufficient creepage distances should be provided for. For example, in a 30-volt generator the creepage distances should be no less than three sixteenths of an inch.

These circumstances must be taken into consideration when the voltage of the d-c system is determined. The 24-volt system has been found to be quite satisfactory. It has also been found that in certain motor generator sets which are used for turret and gun control 60 volts seems to be satisfactory. Engine-driven generators rated 120 volts have been built and mounted in the nacelle of flying boats; however, the ceiling for these ships is comparatively low, probably from 20,000 to 30,000 feet, and further experience will be required to evaluate fully the merits of voltages higher than the 30-volt system.

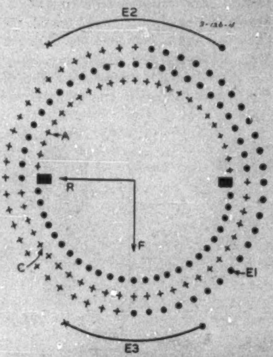

Figure 4. Schematic sketch of compensation and excitation

Vibrations

The vibrations of the engine are very severe, and the magnitude of the forces are not fully known. To guard against failure of the generator, any new design should have an engine test, preferably on the engine for which it has been designed. This run should not be less than 125 hours at different speeds. The engine vibrations are transmitted to the brushes of the generator, tending to throw them off the commutator. To prevent this, spring pressures should be comparatively high, but not so high as to cause excessive brush wear. It has been found that about five pounds per square inch of the brush contact is a good compromise. It is also advantageous to use a large number of small brushes, so that if a few brushes are thrown off, there are others to do the work. The inertia of the brushes should be made as low as possible to minimize these forces.

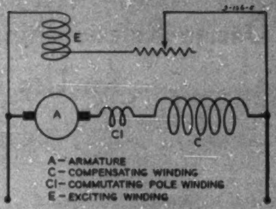

Figure 5. Winding diagram

Another kind of vibration built up by the engine is the torsional vibration transmitted to the shaft. To take care of these vibrations a quill drive is employed (shown in Figure 1). In order to avoid resonance an asbestos packing is used between the inner and outer shafts, which serves as a damping medium. It may be pointed out that this is a nonwearing packing, since it does not slide but depends entirely upon internal molecular friction.

Electrical Characteristics

The electrical design must secure reliability of operation and light weight.

The reliability requires sparkless commutation at all speeds and for short heavy overloads; even short circuits should not be injurious to the commutator or brushes. In Figure 2 is shown a typical flux distribution curve in a noncompensated generator. It may be noted that

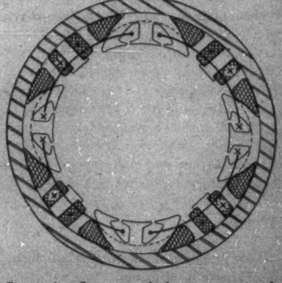

Figure 6. Structure of d-c generator with salient poles and compensation

at load the flux has been distorted in height and also shifted at the same time. This flux distortion is reflected in a similar distortion of the potential around the commutator, resulting in an increased maximum voltage between bars and also a piling up of the voltage toward one of the brushes. Under extreme conditions the maximum volts between bars may be increased from 30 to 40 per cent at full load, and near the brush it will be multiplied several times. Should excessive sparking occur, particularly when the air is rarefied and ionized, an arc may be formed and carried around to the next brush, very likely ruining the generator. This phenomenon is eliminated by the use of compensation, which has for its main object the stopping of flux distortion.

Since the heating is the limiting condition for light weight, an electrical design should be adopted which gives minimum losses. One solution which meets this requirement is the use of compensation, as it eliminates the load losses. Since compensation neutralizes the armature reaction, no field distortion takes place; hence, no extra core loss occurs. Compensation also eliminates sparking and thus does away with extra losses in the brush contacts and in the short-circuited coils. The extra losses in generators without compensation may be considerable, particularly in high-speed machines at weak field. In extreme cases it has been observed to amount to eight points in the efficiency.

Another feature of reliability of the generator is the proper shape of the regulation curve, showing the relationship between amperes load and the terminal voltage. As shown in Figure 3 this curve should be a uniformly drooping line, which is helpful in giving stable operation of the voltage regulator and also contributes to equal current distribution when several generators are operated in parallel, as is the case in multiengined planes. In Figure 3 is shown a typical regulation curve of a compensated generator, showing its uniformly drooping character.

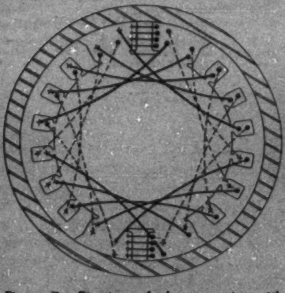
Figure 7. Structure of d-c generator with distributed windings

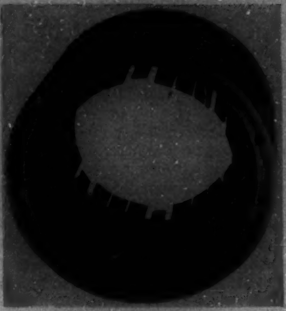
Figure 8. Six-pole field structure of compensated d-c generator

Compensation

The principle of compensation is shown schematically in Figures 4 and 5. Referring to Figures 4, *A* represents the armature winding which may be wound with any of the usual types of windings. Surrounding the armature is a field structure carrying the compensating winding *C*, which is connected in series with the armature, in opposition to it and equally as strong as the armature reaction *R;* thus these two windings constitute a nearly noninductive pair; that is, no flux except a small leakage flux will be produced. In order to commutate, a certain amount of commutating flux is necessary, and this flux is produced by overcompensation located on commutating poles, which for simplicity is not shown in Figure 4.

Figure 9. Complete field of four-pole compensated d-c generator

The field excitation *F* may be applied in two different ways:

1. In concentrated coils *E*2 and *E*3 assembled on salient poles.
2. It may be distributed in a winding *E*1 in which case we arrive at a d-c generator in which both the excitation and the compensation are fully distributed.

In Figure 6 is shown the structure of a d-c generator with salient poles and concentrated excitation. It also shows the compensating winding and the commutating poles with their windings.

In Figure 7 is shown the structure of a d-c generator with both the compensation and excitation distributed. Full drawn lines indicate the compensation, and dotted lines the excitation. It also shows the commutating poles with their windings. When inspecting the winding in Figure 7, it is of interest to observe that these windings are similar to a two-phase winding in a two-phase induction motor, one phase corresponding to the excitation and the other phase corresponding to the compensation. These two windings are 90 electrical degrees apart in space. It should also be noted that the pitch of these windings is 50 per cent, which secures short end windings and thus reduces losses.

Figure 10. Complete field of an accessory engine-driven d-c generator with fully distributed windings

In order to further illustrate the principle of compensation, reference is made to Figure 8, which is a photograph of a six-pole field structure rated 30 volts, 200 amperes, 2,500/5,000 rpm.

Figure 9 shows a photograph of a four-pole structure with four salient poles rated 30 volts, 300 amperes, 4,400/10,000 rpm.

Figure 10 shows a photograph of a complete field with windings and brush holders of an eight-kw accessory engine-driven generator with fully distributed windings

Frequency Modulation for Power-Line Carrier Current

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FREQUENCY modulation is known to most people today as a system of broadcasting that is remarkably free from the effects of interference and is capable of providing high fidelity reception. These advantages have been realized largely by employing frequencies of 40 megacycles and higher which are well above the present broadcast band. A great amount of effort has been devoted to this field, and therefore, frequency modulation has been well analyzed both in theory[1-3] and experiment.[4,5] However, most of the work has been done with systems using a large deviation ratio which is usually defined as the ratio of the carrier deviation from normal to the highest modulating frequency.

This paper presents results of investigations carried on with a frequency-modulated system for power-line communication, employing frequencies below 200 kilocycles and a deviation ratio of one to one. In this way, the same band width as required for amplitude modulation, or *A-M*, can be utilized. The results of the investigation substantiate the work of others[2] and indicate that even with a one-to-one deviation ratio an *F-M* system is less disturbed by interference than a comparable *A-M* system. In addition, when such a system is subjected to the type of noise encountered on some power lines, the difference between *A-M* and *F-M* is still more prominent.

Outline of Theory

Unfortunately frequency modulation is not as widely understood as amplitude modulation. If *F-M* had been introduced and used for a number of years before *A-M*, the reverse would probably have been true. Both *F-M* and *A-M* are descriptive of the manner in which a modulating quantity alters a sine function that represents a carrier frequency. Since a carrier frequency is an alternating quantity having both amplitude and frequency as its principal characteristics, it is possible to operate on either one of these to cause the carrier frequency to carry intelligence.

One effective way to compare two different modulation schemes is by the equations for the modulated signal. The equation for an *A-M* signal is merely a sine function whose amplitude, A, is varied in some fashion and is usually expressed as

$$e=A(1+m\sin\alpha t)\sin\omega t \qquad (1)$$

where

$\omega=2\pi$ times the carrier frequency
$\alpha=2\pi$ times the modulating frequency
$m=$ amplitude of the modulating frequency and is one for 100 per cent modulation

This equation can be expanded by trigonometric identities into three separate sine functions

$$e=A\sin\omega t+\frac{mA}{2}\cos(\omega-\alpha)t-\frac{mA}{2}\times\cos(\omega+\alpha)t \qquad (2)$$

which can be used to determine the band width of various type channels.

In a similar manner, the equations for a frequency-modulated signal can be developed. Since this has often been done in the literature,[1,3] the important steps only will be given. The equation for the modulated signal is

$$e=A\sin\left(\omega t+\frac{\Delta\omega}{\alpha}\sin\alpha t\right) \qquad (3)$$

To grasp the significance of this equation, it is only necessary to remember that $\Delta\omega$ is directly proportional to the amplitude of the modulating frequency. As the amplitude of a sine function is varied in equation 1 for an *A-M* signal, the frequency of a sine function is varied in equation 3 for a frequency-modulated signal. This last equation can be expanded by means of identities into

$$\begin{aligned}e=A\Bigg\{&J_0\left(\frac{\Delta\omega}{\alpha}\right)\sin\omega t+\\&J_1\left(\frac{\Delta\omega}{\alpha}\right)[\sin(\omega+\alpha)t-\sin(\omega-\alpha)t]+\\&J_2\left(\frac{\Delta\omega}{\alpha}\right)[\sin(\omega+2\alpha)t+\sin(\omega-2\alpha)t]+\\&\cdots\cdots\cdots\cdots\cdots\cdots+\\&J_n\left(\frac{\Delta\omega}{\alpha}\right)[\sin(\omega+n\alpha)t+(-1)^n\sin(\omega-n\alpha)t]\Bigg\}\end{aligned} \qquad (4)$$

where $J_n\left(\frac{\Delta\omega}{\alpha}\right)$ is the *n*th order Bessel function of the first kind.

Therefore, instead of one upper and one lower side band as in equation 2, the possibility of many side bands exist. The magnitude of these side bands are fixed by the Bessel functions $J_0\left(\frac{\Delta\omega}{\alpha}\right)$ to $J_n\left(\frac{\Delta\omega}{\alpha}\right)$. However, if $\frac{\Delta\omega}{\alpha}\leq 1$, only the first two terms are significant, and the values of the Bessel functions of the second to *n*th order

$$\left[J_2\left(\frac{\Delta\omega}{\alpha}\right)\text{to } J_n\left(\frac{\Delta\omega}{\alpha}\right)\right]$$

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rated 30 volts, eight kw, 4,000 rpm.

Both the salient pole construction and the field using distributed windings are being used for aircraft generators. It has been found that the salient pole structure is the lightest of the two structures by about 10 per cent. Since weight is of paramount importance, the salient pole construction is now given preference. While both methods lead to very good commutation, the distributed windings seem to be the most powerful means of obtaining perfect operation, the reason being that it minimizes the leakage and secures a very clean neutral. As an example of how powerful the combination of a distributed compensation and excitation winding is, the author built a separately excited d-c generator rated 150 kw, 15,000 volts, having an average volts per bar of 90 which at full voltage withstood dead short circuits with only a minor flicker on the commutator.

Large industrial machines which have to meet severe operating conditions have been made for many years with compensation. In this paper it has been proved that small d-c generators of the compensated type can be made in a very compact form with light weight.

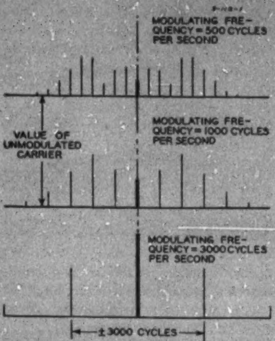

Figure 1. Chart of the amplitudes of the side bands for three different modulating frequencies when the deviation ratio is one to one

are small. Advantage is taken of this fact in the system discussed in this paper.

Another type of modulation has been used that is closely related to *F-M*, namely, phase modulation or *P-M*.[6] Actually, a frequency shift always changes the rate of phase change and vice versa so that the two functions are dependent on each other. By definition, the two types of modulation, *F-M* and *P-M*, have become associated with the nature of the modulated signal. The equation for a phase-modulated signal, comparable to equation 3, is

$$e = A \sin(\omega t + \phi \sin \alpha t) \qquad (5)$$

This equation can be expanded to be the same as equation 4 except with ϕ substituted for $\frac{\Delta\omega}{\alpha}$. Both of these terms have the dimension of phase angle. For the same intensity of all modulating frequencies, in phase modulation the phase shift is constant, but the frequency shift is proportional to the modulating frequency; while in frequency modulation, the frequency shift is constant for all modulating frequencies and the phase shift is inversely proportional to the modulating frequency. Also, the band width of the former is approximately proportional to the modulating frequency, and the band width of the latter is practically constant. Sufficient swing or modulation cannot be obtained easily with phase modulation, and, since reactance tubes can produce enough frequency modulation at low carrier frequencies, strictly frequency modulation was used in the investigation reported here.

Deviation Ratio

The choice of a one-to-one deviation ratio was influenced principally by the desire to restrict the band width of individual channels to the same space that would be required for a comparable *A-M* channel. This is quite important from the standpoint of any user of power-line carrier. The art has progressed to such a stage that many pieces of auxiliary apparatus have been designed and standardized around amplitude-modulated systems. Therefore, existing apparatus as well as equipment already in production could be used with an *F-M* transmitter and receiver to set up a channel. In other words, if the band-width characteristics of line traps and tuning units are applicable to *F-M*, then there would be no change in the application and installation practices already in use.

Even with a one-to-one deviation ratio, the frequency shift is a large percentage of the carrier as compared to the per cent frequency shift used in present day *F-M* broadcast practice. For instance, a 3,000-cycle shift is six per cent of a carrier frequency of 50 kilocycles, whereas the standard 75-kilocycle shift of a broadcast transmitter at 40 megacycles is only about 0.2 per cent. A greater deviation could be generated at these low frequencies, but the band-width requirements of the receiver and coupling apparatus would be excessive. On the other hand, a smaller deviation would not reduce the required band width proportionately and would only result in a lower degree of modulation.

A better picture of what a one-to-one deviation ratio produces in the way of side bands is given in Figure 1, which shows a chart of the amplitudes of the various side bands for different modulating frequencies. This chart was made up by plotting the amplitudes of the various terms given in equation 4, in which $\Delta\omega$ corresponds to a frequency of 3,000 cycles in all cases. From this diagram it can be seen that the bulk of the side bands are confined to a band width of ±3,000 cycles. Of course, if the utmost in fidelity is desired, some of the side bands extending beyond ±3,000 cycles would have to be transmitted and received. Actually, if all the side bands outside of the ±3,000 cycle band were eliminated, only about four per cent of the total energy in the modulated wave would be lost. This would cause a small amplitude modulation of the carrier to appear, but the limiter in the receiver restores the carrier to a constant amplitude. However, the distortion resulting from eliminating the higher side bands is small compared to the total distortion in any *A-M* carrier channel.

Noise Considerations

Noise reduction by means of frequency modulation has been the largest factor in promoting broadcast *F-M*. Interference to the reception of a desired signal may come from many sources and be of various types. In space radio, the most uncontrollable interference is that which takes the form of modulated frequencies in the band accepted by the receiver. The energy in these frequencies is usually uniformly distributed over the band of any channel and is often termed external random noise. With varying magnitudes of this type noise, *F-M* has the characteristic of keeping the interference well rejected until the magnitude of the noise bears a certain ratio with respect to the desired signal. Then the noise is accepted, and the desired signal is rejected. In wide-swing or large-deviation *F-M* this threshold is quite abrupt and occurs when the signal-to-noise ratio becomes about two. It has been demonstrated[8] that in

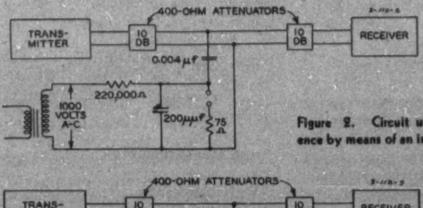

Figure 2. Circuit used to produce interference by means of an intermittent arcing air gap

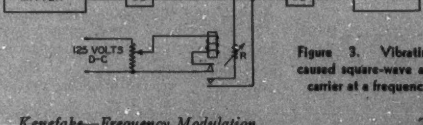

Figure 3. Vibrating-relay circuit which caused square-wave amplitude modulation of carrier at a frequency of about 100 cycles

a small-deviation system, this threshold is not as abrupt and occurs at a lower signal-to-noise ratio. Therefore, it develops that a small-deviation system can deliver usable intelligence from a signal whose signal-to-noise ratio is less than two. This is a definite advantage when the primary function of a service is to get intelligence from one point to another.

There always exists on power lines a fairly high level of random noise caused by corona and arcs of various types acting as sources of amplitude-modulated radio frequencies. In these cases more transmitter power helps in improving the signal-to-noise ratio. Noise of this nature when added to the desired carrier causes a complex type of amplitude variation and phase shift. This produces a signal which is similar to a carrier and a single side band. It is possible for the limiter in an *F-M* receiver to remove the amplitude modulation, but of course it is impossible to eliminate the phase shift. Therefore, it is this phase shift that is the source of noise for an *F-M* channel. It is possible in an *A-M* system for noise to cause a 100 per cent modulation of the carrier, and this is also the limit to the degree of modulation by the desired signal. In frequency modulation it is impossible for noise to cause a phase shift of more than about one radian, but it is possible for the desired signal to cause a much greater phase shift. The phase shift is one radian for the highest value of α when a one-to-one deviation ratio is used. This phase shift becomes greater as α decreases and is the reason for more noise interference present at the higher modulating frequencies. By assuming a random-type noise, the noise reduction theoretically possible by means of *F-M* over *A-M* is six decibels. This is computed from the fact that noise in *A-M* is the same over the audio spectrum while in *F-M* the noise is zero at zero audio frequency and increases linearily to the same value as in *A-M* at the highest audio frequency. Hence, when the noise is integrated over the audio range, it is just half as great in *F-M* as in *A-M*.

Random noise may not be the only type of interference encountered in carrier current or any system where metallic circuits of one kind or another are used for the medium of transmission. In fact, another type of interference, apparently caused by corona, has also been observed on power lines. The carrier in traversing the power line becomes amplitude modulated with noise, and for lack of a better name this phenomenon has been called "corona modulation." The modulation is apparently a result of corona producing an impedance change to the flow of the carrier-current signal. If only this type of noise is present, an increase in transmitter power to surmount the noise does not help because the per cent modulation from the noise stays the same. This modulation is a function of the total attenuation between the transmitter and the receiver and the amount of corona present on the lines. A simple experiment that can be performed to detect this type of noise is to listen to a signal in a carrier receiver with no automatic volume control in operation. When the transmitter is turned off, this type of noise will decrease or completely disappear, indicating that it comes in as modulation of the carrier signal. In this case, the noise is carried by symmetrical side bands about the carrier as represented by the last two terms of equation 2. However, when this type of noise is fed into an *F-M* receiver which contains a balanced discriminator, these side bands are balanced out, since a discriminator is a slope filter whose output is proportional to the frequency deviation from normal. Because an *A-M* signal has amplitude variations but no phase or frequency variations, the output of the discriminator in this case would be zero. Herein is the outstanding advantage of frequency modulation for power-line carrier because it provides the only real solution to corona-modulation noise.

In an effort to improve the discrimination against interference in an *F-M* system still further, advantage may be taken of the energy versus frequency characteristic of the voice.[11] This characteristic indicates that the bulk of the energy in voice exists in the low audio frequencies. Therefore, it is possible to pre-emphasize the higher audio frequencies in the transmitter without exceeding the modulation capabilities of the transmitter. This, of course, makes it necessary to use a de-emphasis circuit in the receiver. Then the scheme is in effect a combination of phase modulation and frequency modulation. It provides a means to get the greatest frequency shift for any modulating frequency without exceeding the band width of any particular channel.

Noise Measurements

With these known facts, a preliminary setup was made in the laboratory consisting of one transmitter and one receiver. The functioning of various circuits was checked, as well as the characteristics of *F-M* when noise was present. These initial results were so promising that a complete communication channel was later set up in the laboratory which involved two transmitters and two receivers and some auxiliary equipment with which more elaborate measurements were made. After considerable development work, a third group of samples was built which embodied all of the improvements and circuit designs worked out in the previous samples. The final samples involved commonly used *F-M* circuits such as a pre-emphasized frequency modulator, a modulation limiter, and a class *C* amplifier in the transmitter; with a radio-frequency amplifier, a limiter, a discriminator, a de-emphasized audio amplifier and a carrier-off noise suppressor in the receiver. Naturally, certain modulations and improvements were made in order to adapt these *F-M* circuits to low-frequency operation.

Extensive tests were made with the samples, of which the noise-reduction measurements were the most interesting. Various schemes were used to produce noise, but the two that proved most acceptable are shown in Figures 2 and 3. The circuit shown in Figure 2 introduced a type of noise which was principally characterized by an arc as a source of radio-frequency energy. The circuit in Figure 3 amplitude-modulated the carrier with a square wave at a rate of about 100 cycles so that several simultaneous noise side bands were produced. Measurements were made to indicate differences between an *F-M* channel and *A-M* channel having the same transmitter power, band width, audio characteristic and attenuation between transmitter and receiver. These measurements were made after both systems were set up in such a way that a 500-cycle modulating frequency produced the same output from both receivers and caused 100 per cent modulation of the *A-M* transmitter and a deviation of 3,000 cycles in the *F-M* transmitter. This particular audio frequency was used because it was just below the point in frequency where the pre-emphasis and de-emphasis circuits began to function. Table I gives the amount the noise level in *F-M* was found below the noise level in *A-M* in decibels. The results in Table I are an average of several measurements with each of the samples mentioned.

Table I

Type of Noise	Unemphasized F-M	Pre-emphasized F-M
Figure 2	11	16 to 19
Figure 3	19	23 to 25

The results show considerably more noise reduction than theory predicted. The theory is based on the assumption that the noise is random, while circuits used here did not necessarily produce random noise. It is felt that the noise encountered on power lines is similar to some combination of the two types of noise employed in these tests. It may be all of one type or any combination of the two. The noise reduction is greater than expected, principally since the bulk of the energy in the noise is in the lower audio frequencies.

Based on these noise measurements made with the arc-noise generator in Figure 2, which utilizes the less favorable type of noise, it would take the following transmitter powers to give the same signal-to-noise ratio in a receiver under the same conditions.

System	Watts
AM	100
Unemphasized *FM*	8
Pre-emphasized *FM*	2

This indicates the possibility of greater transmission ranges with medium power levels which may allow some communication problems to be solved without repeating stations.

Other Advantages

Noise reduction is not the only achievement of frequency modulation as far as communication for power-line carrier is concerned. There are other characteristics which can be utilized to advantage in the various types of communication. The presence of a limiter in the *F-M* receiver is analogous to a very fast and flat type of automatic volume control. The automatic volume control used in *A-M* receivers is of necessity reasonably slow and usually not very flat. One type of communication wherein this characteristic is advantageous is the two-frequency duplex where one frequency is used to transmit in one direction and the other frequency is used to transmit in the opposite direction. The transmitter and receiver audio circuits must then connect to a hybrid circuit and can be set closer to the critical point if the audio output of the receiver is quite constant. Since the limiter in an *F-M* receiver can hold the output extremely constant, a two-frequency voice channel can be operated with higher audio gains, from terminal to terminal.

The other type of communication commonly used is the single frequency duplex or automatic simplex where the voice starts the transmitter and blocks the receiver on outgoing speech. To reduce the amount of clipping of words, it is desirable to accomplish the switching of the transmitter and receiver in as short a time as possible. One inherent limitation in an *A-M* system for fast switching of a channel is the surge of rectified carrier in the detector of a receiver when the transmitter starts. For smooth operation it is necessary to hold the receiver in an inoperative condition until this surge dies down. This difficulty cannot be completely overcome with an *A-M* system because turning the transmitter on and off is fundamentally amplitude modulation, and the detector is designed to detect amplitude modulation. When *F-M* is used, the detector in the receiver is insensitive to amplitude variations and is only sensitive to any phase variations that may come about in starting and stopping the transmitter. Since any phase shifts are small compared to the phase shift caused by modulation by the desired signal, this surge is greatly minimized in the receiver when *F-M* is used.

The transients in an automatic-volume-control circuit of an *A-M* receiver at the moment carrier is received prevents the possibility of opening the receiver quickly and getting an undistorted signal in the output. These transients in automatic-volume-control circuits change the gain of the receiver and hence the output of the receiver is modulated by these transients which causes distortion. On the other hand, a limiter in an *F-M* receiver is practically instantaneous in action, and the receiver can be opened very rapidly without the possibility of the receiver output becoming distorted by transients.

It has been shown[12,13] that two broadcast *F-M* stations operating on the same frequency have considerably more interference-free operating area than two *A-M* broadcast stations on the same frequency. This would still be true of two *F-M* power-line carrier communicaton channels, except to a lesser degree, since the deviation ratio is one to one instead of five to one. With the rapid installation of carrier-current apparatus throughout the country, there are many places where frequency congestion is becoming a problem. One partial solution to this problem would be by means of *F-M*, since a certain frequency could be repeated throughout a system more often than with amplitude modulation.

This paper has largely dealt with aspects of frequency modulation as applied to voice communication. Obviously, there are certain other services where *F-M* is much better than *A-M*. It has been demonstrated that a frequency-shift-type signaling channel[10] is superior in most all respects to a keyed-carrier type of signal channel. It is much easier to transmit frequency or phase accurately through nonlinear tube circuits than it is to transmit an amplitude faithfully through these same type circuits. Therefore, frequency modulation seems basically advantageous when tube circuits are involved. Also, all radio-frequency circuits associated with an amplitude-modulated signal must be linear whereas they need not be linear with a frequency-modulated signal.

Conclusion

It seems certain that the characteristics of *F-M* make it desirable for many applications in the carrier-current field. The outstanding characteristic of *F-M* is the rejection of the interference resulting from undesired amplitude modulation of the carrier.

The chief advantages for *F-M* when applied to power-lines may be summarized as follows:

1. Capability of noise reduction.
2. Possibility of longer channels or less power in transmitters.
3. Less interference between channels on the same frequency.
4. Flat automatic volume control in receiver.
5. Fast switching of channel possible in single frequency voice communication.
6. Same auxiliary apparatus as used for *A-M* channels because of the same band-width requirements.

These advantages have been discussed to a considerable extent in the body of the paper.

It should be pointed out that *F-M* equipment cannot be applied to operate with existing *A-M* equipment. A signal from an *F-M* transmitter can be reproduced by an *A-M* receiver by tuning the receiver to one side of the carrier[8] so that the selectivity characteristic performs similar to a slope filter. However, this is not acceptable for general application because of the distortion resulting from the nonlinearity and narrowness of the sloping part of the characteristic. Also, any frequency drift which may occur in the *F-M* carrier results in excessive distortion in the *A-M* receiver because the slope filter which is obtained by the one-

The Calculation of Unbalanced Magnetic Pull in Synchronous and Induction Motors

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Synopsis: Unbalanced magnetic pull may be defined as the net sideways force between the stator and rotor of an electric machine resulting from a difference in the air-gap flux densities on opposite sides of the machine. This difference in flux density is, in general, caused by a difference in the air gaps on the two sides.

There is outlined herein a method of calculation of unbalanced pull which takes into account the combined effects of saturation, parallels, and primary reactance. The increased accuracy which this method affords should make possible more accurate predictions of mechanical deflections and critical speeds.

I. Induction Motors

INDUCTION motors may operate at any speed from zero to synchronous speed and at any applied voltage from zero to 110 per cent of normal. Operation of the machine under these conditions may be illustrated on the no-load saturation curve. The no-load saturation curve will be represented by a power equation of the form:

$$i_m = eI_g + e^m I_s$$

Assume that the flux density over a certain section of the air gap of a machine is uniform and that a uniform change is made in the length of the air gap over this section while the magnetizing current is maintained at a fixed value. It is shown in Appendix I that the resulting change in gap density is given by the equation:

$$b = \frac{\left(1 - \frac{K_c'}{K_c}\frac{g-\Delta}{g}\right)eB_g}{\frac{K_c'}{K_c}\frac{g-\Delta}{g} + me^{m-1}\frac{I_s}{I_g}}$$

This equation may be used to give the rise in gap density caused by a given change in the air gap for an induction motor with a series or two-parallel primary winding operating at any speed and voltage. However, it should be noted that the no-load saturation curve does not accurately represent the flux conditions in the motor under load. Use of this curve is justified, however, by the fact that the total saturation will normally be divided fairly evenly between the stator and the rotor, and the density rise as calculated by the foregoing equation will be nearly correct.

If a very small deflection is considered, this equation has the following form:

$$b = \frac{\frac{\Delta}{g}eB_g}{1 + me^{m-1}\frac{I_s}{I_g}}$$

This equation may be written in the form:

$$b = K_s\frac{\Delta}{g}eB_g$$

where

$$K_s = \frac{1}{1 + me^{m-1}\frac{I_s}{I_g}}$$

and this represents the effect of saturation on the flux density rise.

A. Unbalanced Pull for a Series or Two-Parallel Primary Winding

Consider now a machine having constant flux density around the periphery in which the rotor is displaced to one side a small amount.

The deflection will then be approximately sinusoidal around the periphery, and the flux density change will also be approximately sinusoidal, since $b = \text{constant} \times \Delta$.

The fundamental formula for magnetic pull is:

$$\text{Magnetic pull} = 0.01386B^2A$$

Using this formula together with the equation for density rise, it is easily proved that:

$$\text{Unbalanced magnetic pull} = 0.01386K_s\frac{\Delta}{g}e^2B_g^2A$$

In another form, this may be written:

$$\text{Unbalanced magnetic pull} = \text{constant} \times \frac{e^2}{1 + me^{m-1}I_s}$$

Solving for a maximum:

$$E^{m-1} = \frac{2}{(m^2 - 3m)\frac{I_s}{I_g}}$$

where E is the per unit voltage at which maximum pull occurs.

This formula gives the voltage at which maximum pull would occur if the gap density were uniform around the periphery of the machine. However, with a sinusoidal wave form and with part of the saturation in the teeth it is apparent that maximum pull for the entire wave will not occur when the peak density reaches the aforementioned value for maximum pull but will occur at some higher density. For an unsaturated machine, when the peak density reaches the aforementioned value, the average pull

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sided tuning of an *A-M* receiver is very narrow.

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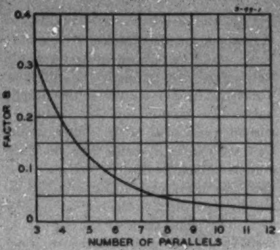

Figure 1. Factor *a* plotted against the number of parallels

will be one half the pull that would be obtained if the density were uniform at this value. However, with saturation, it is a close approximation to take this value as two thirds of the pull calculated with a uniform density. There is an important exception, however; that is, that, when the maximum pull comes at higher than 110 per cent voltage, the unsaturated value of one half should be used.

This gives two cases as follows:

1. For E less than 1.1

Maximum unbalanced pull =

$$2.88K_sE^2\frac{B_g^2A}{g}\times10^{-4}$$

2. For E greater than 1.1

Maximum unbalanced pull =

$$2.62K_s\frac{B_g^2A}{g}\times10^{-4}$$

Both formulas apply to a deflection of $^1/_{32}$ inch.

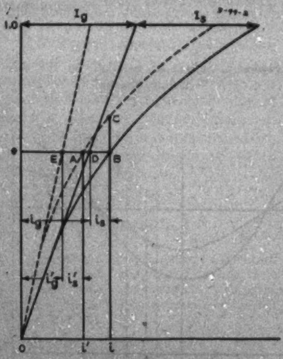

Figure 2. Air-gap flux-density change from the no-load saturation curve

B. Unbalanced Pull for Three or More Parallels in the Primary Winding

Assume a constant gap density under one parallel of the winding, and assume that the gap is changed a constant amount over this section. It is shown in Appendix II that the flux density rise is given by the formula:

$$b=\frac{\frac{\Delta}{g}eB_g}{1+me^{m-1}\frac{I_s}{I_g}+\frac{1}{I_gX_1}}$$

This may also be written in the form:

$$b=K_R\frac{\Delta}{g}eB_g$$

where

$$K_R=\frac{1}{1+me^{m-1}\frac{I_s}{I_g}+\frac{1}{I_gX_1}}$$

and represents the reduction in density rise because of saturation and primary reactance.

It is shown in Appendix III that if there are n parallels and r be any one of these parallels, the total pull of the rth parallel along a given line is given by the formula:

Total pull of rth parallel =

$$1.84\times10^{-4}e^2\frac{B_g^2A}{g}\left[\frac{\sin r\frac{4\pi}{n}-\sin(r-1)\frac{4\pi}{n}}{4}\times K_s+(K_R-K_S)\frac{n}{2\pi}\left\{\sin r\frac{2\pi}{n}-\sin(r-1)\frac{2\pi}{n}\right\}^2+\frac{\pi}{n}K_s\right]$$

For various numbers of parallels, this formula has the following forms:

3 parallels: unbalanced pull =

$$2.88\times10^{-4}e^2\frac{B_g^2A}{g}[0.315K_s+0.685K_R]$$

4 parallels: unbalanced pull =

$$2.88\times10^{-4}e^2\frac{B_g^2A}{g}[0.188K_s+0.812K_R]$$

6 parallels: unbalanced pull =

$$2.88\times10^{-4}e^2\frac{B_g^2A}{g}[0.087K_s+0.913K_R]$$

8 parallels: unbalanced pull =

$$2.88\times10^{-4}e^2\frac{B_g^2A}{g}[0.045K_s+0.955K_R]$$

12 parallels: unbalanced pull =

$$2.88\times10^{-4}e^2\frac{B_g^2A}{g}[0.022K_s+0.978K_R]$$

Infinite parallels: unbalanced pull =

$$2.88\times10^{-4}e^2\frac{B_g^2A}{g}[0K_s+1.0K_R]$$

Generally, the unbalanced pull may be expressed in the following form:

Unbalanced pull =

$$2.88e^2\frac{B_g^2A}{g}[aK_s+(1-a)K_R]\times10^{-4}$$

where a is determined from the curve of Figure 1.

From this equation, it is practically impossible to solve analytically for the voltage at which maximum pull will occur. However, a close approximation will be obtained, if we use in this formula the voltage at which the pull is a maximum, considering saturation alone.

With the constants that are used in most induction motors, the factor $[aK_s+(1-a)K_R]$ is approximately equal to K_s divided by the number of parallels. Therefore, the important fact is established that the unbalanced pull for an induction motor having three or more parallels in the primary winding may be obtained approximately by calculating the pull for a series winding and then dividing by the number of parallels.

II. Synchronous Motors

Synchronous motors have three distinct modes of operation as follows:

1. On open circuit with any speed from zero to synchronous and any field current from zero to full-load field current.
2. Starting as an induction motor with any speed from zero to synchronous and any applied voltage from zero to 110 per cent of normal.
3. Normal synchronous operation with any applied voltage from zero to 110 per cent of normal, any field current from zero to full-load field current and any load.

A. Unbalanced Pull for a Series or Two-Parallel Stator Winding

A synchronous motor with a series winding on open circuit follows the same type of analysis as the induction motor

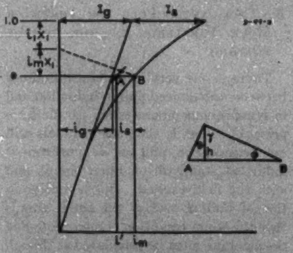

Figure 3. Flux-density change in one parallel of an induction motor

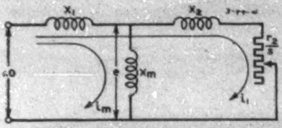

Figure 4. Equivalent circuit of the induction motor

with a series winding. Figure 2 and the equations derived from it apply except that they must be modified to take into account the field form of the synchronous motor. It has been found that this may be done quite accurately by multiplying the formulas for unbalanced pull previously given by C_1, the form factor of the wave.

Under conditions of starting or normal running, the relation between the air-gap flux density and the density in the iron parts of the machine may be different than the relation shown on the no-load saturation curve of Figure 2. However, as with induction motors, part of the iron carries higher flux than the air gap, while part carries lower. This tends to cancel out, and the magnetic pull calculated from the no-load saturation curve will be nearly correct for most machines.

Synchronous motors will normally be limited to about 120 per cent of normal air-gap flux density.

The following formulas, therefore, apply to a synchronous motor with a series or two-parallel stator winding:

1. For E less than 1.2

Maximum unbalanced pull =

$$2.88K_sE^2\frac{B_g^2A}{g}C_1\times 10^{-4}$$

2. For E greater than 1.2

Maximum unbalanced pull =

$$3.11K_s\frac{B_g^2A}{g}C_1\times 10^{-4}$$

B. Unbalanced Pull for Three or More Parallels in the Stator Winding

There is one very important difference between unbalanced pull in induction and in synchronous motors when parallels are present. That is that the parallels will not reduce the pull in a synchronous motor at standstill on open circuit and with the field excited. For a consideration of pulling-over of the rotor, this is then the worst condition of operation, and the machine must be calculated as though it had only a series winding in the stator.

However, for a consideration of critical speed of the shaft the running condition must be analyzed, and it is with this problem that the remainder of this section will deal.

A synchronous motor during the starting period will have an unbalanced magnetic pull which can be calculated in the same way as an induction motor. The following formulas will apply:

1. For E less than 1.1

Maximum unbalanced pull =

$$2.88E^2\frac{B_g^2A}{g}[aK_s+(1-a)K_R]\times 10^{-4}$$

2. For E greater than 1.1

Maximum unbalanced pull =

$$2.62\frac{B_g^2A}{g}[aK_s+(1-a)K_R]\times 10^{-4}$$

In the case of a synchronous motor it will be necessary to use these two formulas rather than merely dividing the unbalanced pull for a series winding by the number of parallels. This is due to the fact that the magnetizing current is normally much larger in synchronous than in induction motors.

Consider now the case of a synchronous motor running on open circuit with any field current and with, theoretically, an infinite number of parallels. It is apparent that with a change in air gap, the flux density rise will be $X_1/(X_1+X_m)$ times the density rise that would have occurred if no parallels had been present. The quantity $X_1, K_s/(X_1+X_m)$ therefore becomes the factor K_R for this condition of operation. The resistance of the stator winding has been neglected, but it should be noted that as zero speed is approached, the resistance limits the circulating current and brings the pull up to the full locked rotor value.

Suppose that the motor is now operated synchronously with a fixed voltage, field current, and load, and a small change is made in the air gap by displacing the rotor to one side. The reduction in air gap on one side releases a certain number of field ampere turns and a certain number of armature reaction ampere turns to force more flux through the machine. This tends to raise the flux in the air gap, but this increase in gap flux produces a current component in the stator parallel (not necessarily in phase with the main current) which produces a leakage reactance plus armature reaction voltage drop exactly equivalent to the original density rise. The phase angle of the current increment is such as to produce an armature reaction exactly in opposition to the original density rise. The net density rise will then be $X_1/(X_1+X_m)$ times the original density rise which gives the same K_R as for the open-circuit condition. As before, use of the no-load saturation curve for the determination of K_s is not exact but is a justifiable approximation.

Since the gap density will be limited to about 120 per cent of normal, we have two cases as follows:

1. For E less than 1.2

Maximum unbalanced pull =

$$2.88E^2\frac{B_g^2A}{g}[aK_s+(1-a)K_R]C_1\times 10^{-4}$$

2. For E greater than 1.2

Maximum unbalanced pull =

$$3.11\frac{B_g^2A}{g}[aK_s+(1-a)K_R]C_1\times 10^{-4}$$

where

$$K_R=\frac{X_1}{X_1+X_m}K_s$$

For most machines, it is found that there is a larger magnetic pull during synchronous operation than during the starting period. Consequently, only this condition need be calculated in order to obtain the worst possible unbalanced pull.

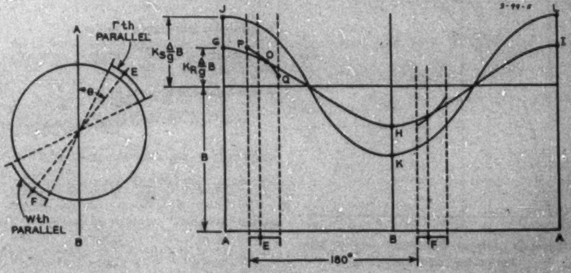

Figure 5. Effect of parallels on the flux density change

Summary

Formulas have been given for calculating first, the per unit voltage at which maximum pull occurs, and then the maximum unbalanced magnetic pull for a synchronous or induction motor with any number of parallels in the stator winding. All quantities used in the formulas may be taken directly from the design sheet with the exception of the exponent m which may be calculated from any two known points on the no-load saturation curve.

In deriving the equations, it was assumed that the air-gap deflection was a static deflection resulting from such causes as misalignment of the rotor in the stator, or the stator being out of round. The same type of analysis may be applied to a deflection that rotates with the rotor, and the same equations for unbalanced pull will apply.

The effect of parallel circuits in the rotor has been neglected, but it should be noted generally that with either a static deflection or a rotating deflection at a speed less than synchronous, the pull will be further reduced below the calculated values.

A two-pole machine will have less unbalanced pull than calculated for certain conditions of operation, but the formulas given should still be used to calculate the worst possible condition.

A two-parallel stator winding has been taken as the same as a series winding because a deflection along the line between the parallels will produce no compensating currents, and the pull will not be reduced. However, it should be noted that the pull will be reduced below the calculated value if the parallels of the different phases are spaced symmetrically around the machine or if the deflection is at right angles to the line between the parallels.

It is apparent then that the formulas given are either correct or somewhat pessimistic for all conditions of operation that could be considered as normal. Special cases of unbalance from such causes as short-circuited coils cannot be treated in a general method and should be solved individually as they arise.

Appendix I

Referring to Figure 2, assume a constant gap density over a section of the machine, and consider that a constant change is made in the air gap while the magnetizing current is kept constant.

$i_g = CK_e g$ and $i_g' = CK_e'(g-\Delta)$

$$i_g' = \frac{K_e'}{K_e}\frac{g-\Delta}{g}i_g$$

$$AB = ED = i_g - i_g' = \left(1-\frac{K_e'}{K_e}\frac{g-\Delta}{g}\right)i_g$$

But $i_g = eI_g$ and $i_s = e^m I_s$

$$i' = i_g' + i_s' = \frac{K_e'}{K_e}\frac{g-\Delta}{g}eI_g + e^m I_s$$

$$\frac{di'}{de} = \cot\theta = \frac{K_e'}{K_e}\frac{g-\Delta}{g}I_g + me^{m-1}I_s$$

$$\frac{BC}{AB} = \tan\theta = \frac{1}{\dfrac{K_e'}{K_e}\dfrac{g-\Delta}{g}I_g + me^{m-1}I_s}$$

$$BC = \frac{\left(1-\dfrac{K_e'}{K_e}\dfrac{g-\Delta}{g}\right)eI_g}{\dfrac{K_e'}{K_e}\dfrac{g-\Delta}{g}I_g + me^{m-1}I_s}$$

$$\frac{BC}{e} = \frac{b}{B} = \frac{\left(1-\dfrac{K_e'}{K_e}\dfrac{g-\Delta}{g}\right)I_g}{\dfrac{K_e'}{K_e}\dfrac{g-\Delta}{g}I_g + me^{m-1}I_s}$$

$$b = \frac{\left(1-\dfrac{K_e'}{K_e}\dfrac{g-\Delta}{g}\right)eB_g}{\dfrac{K_e'}{K_e}\dfrac{g-\Delta}{g} + me^{m-1}\dfrac{I_s}{I_g}}$$

Appendix II

Referring to Figure 3, assume a constant gap density under an independent section of the winding (a parallel), and assume that the gap is changed a constant amount over this section. The most pessimistic condition is obtained if in Figure 4 we assume that the secondary winding is open-circuited.

As proved previously:

$$i' = \frac{K_e'}{K_e}\frac{g-\Delta}{g}eI_g + e^m I_s$$

$$\frac{di'}{de} = \tan\theta = \frac{K_e'}{K_e}\frac{g-\Delta}{g}I_g + me^{m-1}I_s$$

$$\tan\phi = \frac{i_m X_1}{i_m} = X_1 \quad \tan\gamma = \frac{1}{X_1}$$

$$AB = \left(1-\frac{K_e'}{K_e}\frac{g-\Delta}{g}\right)i_g = h\tan\theta + h\tan\gamma$$

$$h = \frac{AB}{\tan\theta + \tan\gamma}$$

$$= \frac{\left(1-\dfrac{K_e'}{K_e}\dfrac{g-\Delta}{g}\right)eI_g}{\dfrac{K_e'}{K_e}\dfrac{g-\Delta}{g}I_g + me^{m-1}I_s + \dfrac{1}{X_1}}$$

$$\frac{h}{e} = \frac{b}{B} = \frac{1-\dfrac{K_e'}{K_e}\dfrac{g-\Delta}{g}}{\dfrac{K_e'}{K_e}\dfrac{g-\Delta}{g} + me^{m-1}\dfrac{I_s}{I_g} + \dfrac{1}{I_g X_1}}$$

If a small deflection is considered

$$b = \frac{\dfrac{\Delta}{g}eB_g}{1 + me^{m-1}\dfrac{I_s}{I_g} + \dfrac{1}{I_g X_1}}$$

Appendix III

Let there be n parallels total in each phase.

Angular spread of each parallel $= 2\pi/n$ radians.

In Figure 5 the equation of curve JKL is $K_s(\Delta/g)B\cos\theta$. The limits on θ for the rth parallel are $(r-1)2\pi/n$ and $r2\pi/n$. The average value of JKL over the rth parallel $=$

$$\frac{1}{\dfrac{2\pi}{n}}\int_{(r-1)\frac{2\pi}{n}}^{r\frac{2\pi}{n}} K_s\frac{\Delta}{g}B\cos\theta\, d\theta$$

$$= \frac{n}{2\pi}K_s\frac{\Delta}{g}B\left[\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right]$$

Similarly the average value of GHI over the rth parallel

$$= \frac{n}{2\pi}K_R\frac{\Delta}{g}B\left[\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right]$$

The drop in density under the rth parallel from the curve JKL to the final position at POQ

$$= (K_s - K_R)\frac{n}{2\pi}\frac{\Delta}{g}B\left[\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right]$$

Equation of $POQ =$

$$K_s\frac{\Delta}{g}B\cos\theta - (K_s-K_R)\frac{n}{2\pi}\frac{\Delta}{g}\times B\left[\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right]$$

Assuming a large number of poles compared to the number of parallels and that part of the saturation is in the teeth of the machine, it may be readily proved that the pull along the line AB

$$= \frac{0.01386}{\pi}\left(\frac{4}{3}\right)BbA\cos\theta\, d\theta$$

For the rth parallel:

$$b = \frac{\Delta}{g}B\,[K_s\cos\theta - (K_s-K_R)\mathrm{B}]$$

where

$$\mathrm{B} = \frac{n}{2\pi}\left\{\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right\}$$

Total pull of the rth parallel along the line AB

$$= \frac{0.01386}{\pi}\left(\frac{4}{3}\right)B^2\frac{\Delta}{g}A \int_{(r-1)\frac{2\pi}{n}}^{r\frac{2\pi}{n}}\{K_s\cos^2\theta - (K_s-K_R)\beta\cos\theta\}\,d\theta = 1.84\times 10^{-4}e^2\frac{B_g^2 A}{g}\times$$

$$\left[\frac{\sin r\dfrac{4\pi}{n} - \sin(r-1)\dfrac{4\pi}{n}}{4}K_s + (K_R-K_s)\frac{n}{2\pi}\times \left\{\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right\}^2 + \frac{\pi}{n}K_s\right]$$

Characteristics and Applications of Selenium-Rectifier Cells

E. A. HARTY
MEMBER AIEE

Synopsis: The rectification properties of selenium cells were first discovered in the year 1883. C. T. Fritts described them in the *American Journal of Science*.[11,12] However, they were never used to any extent, except possibly as photocells.

After the introduction of the copper-oxide rectifier, research activities were stimulated, and selenium cells were again rediscovered. The first commercial cells were made in Germany in the early '30's, and, as the technique of manufacture improved, better cells were made with better life expectancy.

The General Electric Company, after a period of developmental activity, started to make cells in 1938, first in its research laboratories, and subsequently a manufacturing plant was set up which permits producing cells in large quantities within relatively close electrical tolerances.

This paper contains data pertaining to these cells particularly, and the information may not apply in detail to cells manufactured by other methods without some correction factors.

CONSTRUCTION

THE selenium rectifier cell consists essentially of a carrier plate made of either aluminum or iron, supporting on one side a very thin film of specially treated selenium. The adhesion is quite intimate to prevent contact losses. This selenium film is given a series of controlled heat treatments to obtain a suitable crystalline structure.

Finally, a low melting point alloy is metal-sprayed onto the selenium surface. This layer is known as the "counterelectrode."

By means of subsequent electrochemical processes a film or blocking layer is formed between the counterelectrode and the selenium surface. Current flows freely between the selenium and the counterelectrode and is practically blocked in the other direction. Figure 1 shows a typical cross section of a cell.

THEORY

Several theories have been suggested to explain the action of rectifiers of the type under discussion. The physicists do not all agree, and, therefore, no attempt will be made to discuss all these theories in detail.

One of the theories that appears the most logical to understand and that applies to all types of metal rectifiers is the following:

Metal rectifiers consist essentially of a semiconductor and a good conductor separated by a barrier or blocking layer which is, in itself, an insulator but through which electrons can pass in either direction. In the selenium rectifier the selenium layer is the semiconductor and the sprayed-metal counterelectrode the good conductor, the barrier or blocking layer being formed between these two substances. The sprayed-metal layer has an abundance of free electrons, while in the selenium layer, which is a relatively poor conductor, the free electrons are quite small.

When the two electrodes are connected to a source of supply, the opposite polarities set up an electric field across the barrier or blocking layer. Since this layer is very thin, a comparatively small electromotive force will produce a steep potential gradient. If the sprayed metal

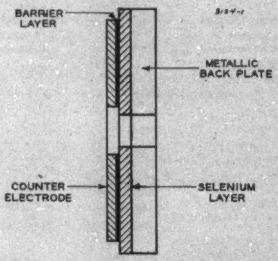

Figure 1. Cross section of a selenium cell

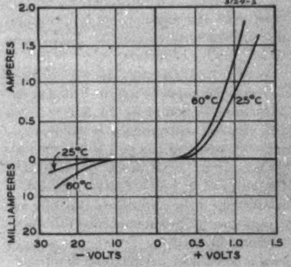

Figure 2. D-c characteristics

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Appendix IV. Nomenclature

A—Area of the stator bore in square inches.
g—Single air gap in inches.
g'—Single air gap with the rotor displaced ($g-\Delta$) in inches.
Δ—Amount the rotor is displaced from the center of the stator in inches.
K_c—Carter's coefficient for an air gap g.
K_c'—Carter's coefficient for an air gap g'.
B_g—Air-gap flux density at normal voltage in kilolines per square inch.
B—Air-gap flux density at any voltage.
b—Rise in the gap flux density.
e—Voltage in per unit corresponding to air-gap flux.
E—Per unit voltage at which maximum pull occurs.
I_g—Magnetizing current for the air gap at normal voltage.
I_s—Magnetizing current for saturation at normal voltage.
i_m—Magnetizing current at any voltage.
X_1—Stator leakage reactance in per unit.
X_m—Magnetizing reactance in per unit.
C_1—Form factor of the no-load field form of a synchronous motor.
m—Saturation curve exponent defined by $i_s=e^m I_s$.
K_s—Factor allowing for the effect of saturation on density rise.
K_B—Factor allowing for the effect of saturation and primary reactance on density rise.
a—Factor depending on the number of parallels.

References

1. MAGNETIC PULL IN ELECTRIC MACHINES, E. Rosenberg. AIEE TRANSACTIONS, volume 37, 1918, pages 1425–69.

2. CRITICAL REVIEW OF THE BIBLIOGRAPHY ON UNBALANCED MAGNETIC PULL IN DYNAMO-ELECTRIC MACHINES, Alexander Gray, J. G. Pertsch, Jr. AIEE TRANSACTIONS, volume 37, 1918, pages 1417–24.

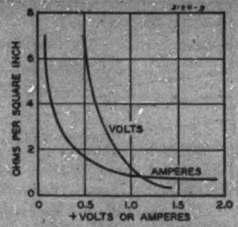


Figure 3. Resistance change with voltage or current

is connected to the negative pole of the source, the free negative electrons are accelerated to a sufficient velocity to enable them to pass through the barrier layer and the intercrystalline spaces of the selenium and reach the metal supporting disk, with the result that a flow of electrons is established which constitutes a current of electricity in the forward direction. When the polarity is reversed, the same action takes place in the opposite direction, but, since the number of free electrons in the semiconducting selenium is less than in the metal disk, the resulting current is much smaller. Because of this asymmetrical property, it is possible to rectify alternating current.

Electrical Characteristics of the Elements

Because it is almost impossible to make all cells exactly alike as to characteristics, even though materials and manufacturing methods are held within very close limits, the data that follow and also all published curves should be considered as representing average conditions. A slight deviation should be expected between individual cells.

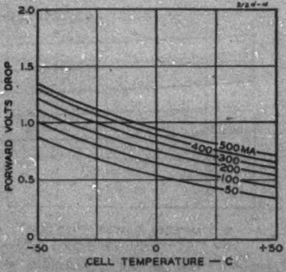


Figure 4. Temperature–forward voltage characteristics

One square inch area

D-C Characteristics

If a d-c voltage of varying potential is impressed first in the blocking direction and then in the flow direction and readings taken of the current that flows, a curve as shown in Figure 2 is obtained. This curve is based on selenium cells having one square inch of effective rectifying area.

In a similar way Figure 3 shows resistance versus voltage and resistance versus current. It should be noted and remembered that these curves are nonlinear and that they do not obey Ohm's law.

Effect of Temperature

The selenium rectifier cells have negative temperature coefficient which results in current increasing for a given voltage as the temperature goes up and decreasing as the temperature goes down.

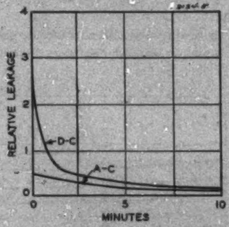


Figure 5. Leakage–time characteristics

Figure 4 shows this relation.

It should be noted that temperature also affects the leakage current in the blocking direction as shown on Figure 2.

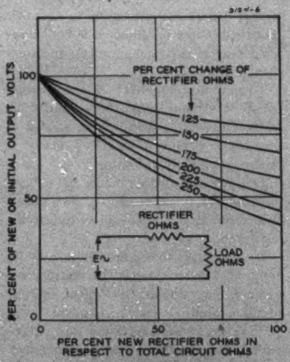


Figure 6. Effect of changing rectifier resistance on output

Because of this characteristic, ratings should be such that the losses caused by heating should be kept under control; otherwise they may keep on adding, resulting in overheating and the eventual destruction of the rectifying film. In general, it may be stated that the rectifier will operate satisfactorily in a range of ambient temperature from −50 to +50 degrees centigrade.

Forward Characteristics and Stability

The forward characteristic is very stable when a-c or d-c voltage is applied in this direction. However, as the cell heats up, changes occur. Over a period of time, the resistance of the cell appears to change and take a set. The rate of change increases with cell temperature, and at 100 degrees centigrade the cell is damaged.

An increase in forward resistance with time means that the difference between the input voltage and the output voltage will become greater. Therefore, to maintain a given output voltage constant, it is necessary to increase the input voltage. Great care must be taken to rate correctly these rectifier cells when new to prevent overloading in voltage after aging takes place.

Reverse Characteristics and Stability

The reverse or leakage characteristic on alternating current is quite good; it is, in general, higher initially but creeps to a minimum value in about two to three minutes. However, when a selenium rectifier cell is subjected to a d-c voltage in the inverse direction, the leakage current is very high initially. A polarizing action similar to that observed with electrolytic capacitors takes place, resulting in a steady decrease in current with time. Typical leakage voltage–time character-

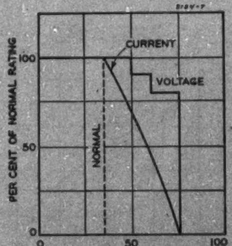


Figure 7. Rating of cells at high ambients

istics are shown on Figure 5. When the cells are continuously de-energized over a period of time, the reverse leakage resistance tends to decrease but will rapidly increase after the cells are again energized. It should again be noted that this same type of characteristic is also present in electrolytic capacitors. This characteristic should be borne in mind when applying these cells in blocking circuits, especially if instantaneous operation is required such as in certain types of high-speed relaying circuits.

CAPACITANCE

A certain capacitance exists because of the presence of the barrier or blocking layer between the two electrodes of the rectifier cell. Measurements indicate this to be of the order of 0.02 microfarad per square centimeter. This may vary somewhat, depending on the past history of the cell.

In high-frequency applications, the capacitance acts like a shunt acrosss each cell, resulting in lowering the leakage resistance and changing the ratio of the forward to the reverse resistance. At normal a-c frequencies up to 2,000 cycles, the capacity effect usually can be disregarded.

Rectifier Circuits

Selenium rectifier cells are readily combined into series and parallel groups, depending on the voltage and current output required. These are used in rectifier circuits to change alternating currents to direct currents. The choice of the particular rectifier circuit rests with the designer. In view of the fact that rectifiers are used by many engineers who are not familiar with these circuits, a tabulation of the more popular circuits is included with this paper. (See Figure 19.)

It should be noted that these data apply to perfect sine waves, perfect rectifiers having no internal resistance and also no leakage whatsoever in the blocking direction. The load is based on a noninductive resistance.

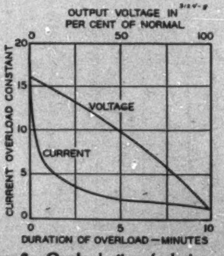


Figure 8. Overload rating of selenium cells

Calculations made with the aforementioned data are only of theoretical use and cannot be applied to actual design work without using correction factors.

Selenium rectifier cells have a fundamental resistance characteristic; this resistance is not constant and varies with current, temperature, and also time. Therefore, computations arrived at from formulas are not absolute, unless all these variables are considered. Design work should be based on empirical data based on years of experience to avoid trouble. Any correction factors must also have their own correction factors, since these will vary with time, temperature, and current.

In designing a practical selenium rectifier circuit, it is evidently very essential to pay particular attention to the fact that the rectifier resistance is not constant and changes with time, current, and temperature.

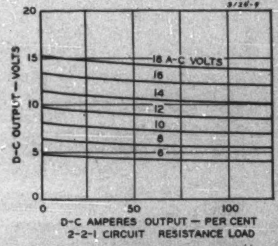


Figure 9. A-c to d-c characteristics of single-phase unit bridge rectifier with resistance load

One square inch area, 25 degrees centigrade

To minimize its effect on the rectifier circuit, it is quite essential to make the rectifier resistance small as compared to the total circuit resistance.[4] This is shown graphically in Figure 6.

The recommended ratings have been so chosen as to make the rectifier resistance about 10 to 15 per cent of the circuit resistance.

By referring to Figure 6, it can be noted that even though the rectifier resistance may double, the effect on the circuit is small.

However, when rectifiers are overloaded, the rectifier resistance in effect becomes a larger percentage of the circuit resistance, and, therefore, more changes should be expected in the output as readily shown in Figure 6.

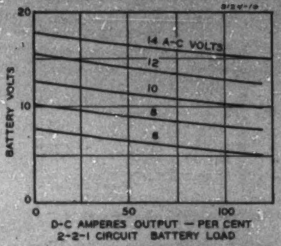


Figure 10. A-c to d-c characteristics of single-phase unit bridge rectifier with battery load

One square inch area, 25 degrees centigrade

MAXIMUM RATINGS

Table I and II show the voltage and current ratings for the various size cells which are available at the present time.

The circuit names as used by the metallic rectifier industry have been retained in Tables I and II.

The circuit symbolic notation is an attempt to overcome the objections raised to the circuit names by replacing them by a symbolic equation 3.

The first digit is the reciprocal of the fraction of time a cell carries current during the cycle. The second digit shows the cells in series and the third digit the cells in multiple carrying current instantaneously. This notation applies to unit rectifiers.

These ratings are based on the usual limiting factors of temperature rise, aging, and voltage breakdown. The in-

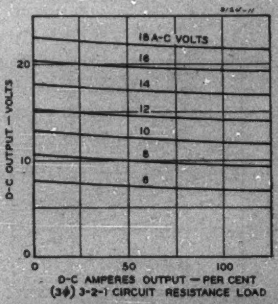


Figure 11. A-c to d-c characteristics of three-phase rectifier, bridge type, with resistance load

One square inch area, 25 degrees centigrade

Table I. Rating as Valves at 35 Degrees Centigrade

Code letter	*F*	*A*	*C*	*H*
Diameter of cells	1 Inch	$1\frac{1}{2}$	$2\frac{9}{16}$	$4\frac{3}{8}$
Inverse rms volts	18	18	18	18
Blocking volts d-c	15	15	15	15

ternal losses heat the rectifier cell, and the final permissible total temperature limits the output rating. It should be borne in mind that aging increases the heating, and, therefore, it is very important to run the rectifier cells somewhat cooler when new. This factor has been taken into consideration in the published ratings. The relation of cell spacing to cell diameter in a rectifier-stack assembly has been chosen for optimum cooling.

PERMISSIBLE TEMPERATURE RISE

Although selenium rectifier cells may be operated up to a total temperature of 75 degrees centigrade, the ratings given in the previous paragraph are based on an ambient temperature of 35 degrees centigrade allowing a maximum temperature rise of 40 degrees centigrade. However, if the normal temperature is liable to be exceeded, the full-load ratings must be changed and reduced to prevent the total temperature from exceeding 75 degrees centigrade.

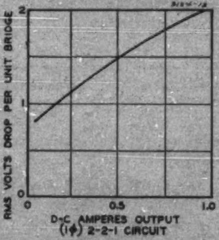


Figure 12. Rms volts drop in a unit bridge single-phase rectifier at different values of current

One square inch area, 25 degrees centigrade

Figure 7 shows the recommended practice.

OVERLOAD AND INTERMITTENT RATING

Selenium rectifier cells will withstand short-time current overloads beyond the normal current, provided the cell is not heated above 75 degrees centigrade. If the cell is allowed to cool back to normal between loading periods, higher current overloads can be applied than in the case of insufficient cooling periods.

Figure 8 shows permissible current overload data and also how the voltage drops as the current increases.

Voltage overloads are not permissible, even for short periods, because of the danger of breaking down the blocking layer.

If the breakdown current is limited, the punctured cells sometimes self-heal. However, every healed spot robs the rectifying surface of some cross section, resulting in increasing effective resistance of the cell.

UNIT RECTIFIER

For convenience in designing rectifiers, data have been prepared on a unit rectifier. This can be defined as any rectifier circuit having one cell in each arm. By reducing all measurements to a unit rectifier, it is possible to obtain general data that will apply to any rectifier, providing the number of cells in series in each arm is known. These same data can also be applied where two or more unit rectifiers are operated in multiple.

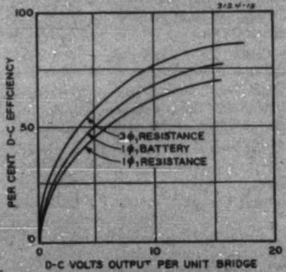


Figure 13. Efficiency of selenium cells at full-load current

Full-load amperes, 25 degrees centigrade

A-C–D-C CHARACTERISTICS OF A SINGLE-PHASE FULL-WAVE UNIT BRIDGE RECTIFIER WITH A RESISTANCE LOAD

By impressing various voltages across a unit bridge rectifier, a group of curves may be obtained as in Figure 9, showing d-c voltages plotted against d-c amperes output for various impressed a-c voltages.

A-C–D-C CHARACTERISTICS OF A SINGLE-PHASE FULL-WAVE UNIT BRIDGE RECTIFIER AS A BATTERY CHARGER

If the resistance load is replaced by a battery load, and data as shown previously are again repeated, a family of curves as shown in Figure 10 is obtained.

A-C–D-C CHARACTERISTICS OF A THREE-PHASE FULL-WAVE UNIT BRIDGE RECTIFIER

If a three-phase rectifier is operated at different a-c voltages and different loads, a family of curves as shown in Figure 11 is obtained.

A-C VOLTS DROP WITHIN A UNIT BRIDGE RECTIFIER

If a d-c ammeter is used as a load across a unit bridge rectifier and the a-c input is varied, a curve as shown in Figure 12 is obtained. This permits calculating the a-c input voltage provided correction factors are used, depending on the circuit and its form factor.

EFFICIENCY

Because of the presence of an a-c component in the d-c output of all rectifiers, it is quite essential to use the correct type

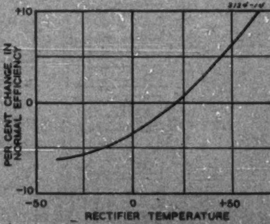


Figure 14. Correction curve to obtain efficiency at different temperatures

Table II. Current Ratings at 35 Degrees Centigrade for Resistance and Inductance Loads

Circuit	1 Inch	$1\frac{1}{2}$ Inches	$2\frac{9}{16}$ Inches	$4\frac{3}{8}$ Inches	D-C Volts	Circuit Symbolic Notation
One-phase half-wave	0.075	0.2	0.500	2.15	6	1-1-1
One-phase bridge	0.150	0.4	1.00	4.3	12	2-2-1
One-phase center tap	0.150	0.4	1.00	4.3	6	2-1-1
Three-phase half-wave	0.200	0.5	1.25	5.3	8	3-1-1
Three-phase bridge	0.220	0.600	1.4	6.5	16	3-2-1
Three-phase center tap with no interphase coil	0.270	0.7	1.8	8.0	8	6-1-1
Three-phase center tap and interphase coil	0.400	1.0	2.5	11.0	8	3-1-2
D-c valves	0.120	0.320	0.80	3.0	15	1-1-1

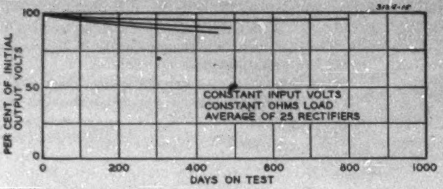


Figure 15. Aging of selenium rectifiers

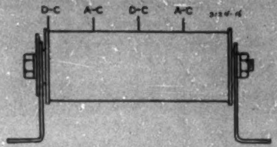


Figure 16 (right). Full-wave single-phase rectifier-stack assembly

of instruments to measure the power output.

The input is always measured with an a-c wattmeter and reads all of the power delivered to the rectifier.

If the nature of the load is such that both the a-c and d-c components of the rectifier output produce useful work, then an a-c wattmeter in the output measures the correct output. Examples of this type of load are resistances, lamps, radio-tube filaments, electromagnets, series motors, and so forth.

However, if the a-c component is not utilized, then the output should be measured with a d-c voltmeter and d-c ammeter of the D'Arsonval type. For example, battery charging, plating work,

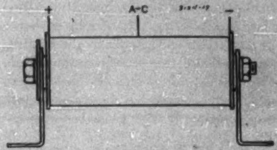


Figure 17 (right). Half-wave three-terminal-type rectifier-stack assembly

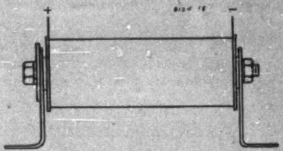
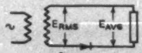
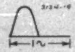
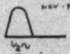
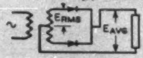
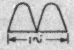
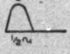
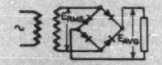
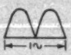
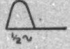
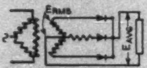
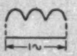
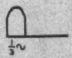
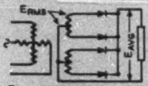
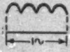
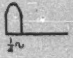
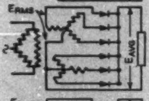
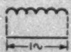
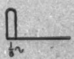
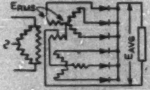
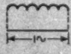
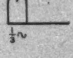
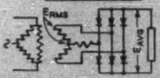
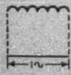
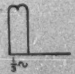


Figure 18 (right). Half-wave two-terminal-type rectifier-stack assembly

Figure 19 (below). Fundamental rectifier circuits

		Output Wave Values				D-C	Rectifier Cell-Wave Values					Trans. Capacity	
Symbolic Notation	Circuit	Wave Form	E_{avg}	E_{eff}	Form Fact.	Ripple %	Wave Form	I_{avg}	I_{eff}	Form Fact.	E Inverse	Pri.	Sec.
1-1-1			0.45 E_{rms}	0.707 E_{rms}	1.57	121		1.00 I_{dc}	1.57 I_{dc}	1.57	1.414 E_{rms}	3.49 EI_{dc}	3.49 EI_{dc}
2-1-1			0.900 E_{rms}	1.0 E_{rms}	1.11	48		0.500 I_{dc}	0.786 I_{dc}	1.57	2.828 E_{rms}	1.235 EI_{dc}	1.75 EI_{dc}
2-2-1			0.900 E_{rms}	1.0 E_{rms}	1.11	48		0.500 I_{dc}	0.786 I_{dc}	1.57	1.414 E_{rms}	1.235 EI_{dc}	1.235 EI_{dc}
3-1-1			1.17 E_{rms}	1.19 E_{rms}	1.02	21		0.333 I_{dc}	0.587 I_{dc}	1.76	2.45 E_{rms}	1.508 EI_{dc}	1.508 EI_{dc}
4-1-1			1.27 E_{rms}	1.28 E_{rms}	1.005	11		0.250 I_{dc}	0.502 I_{dc}	2.01	2.828 E_{rms}	1.116 EI_{dc}	1.58 EI_{dc}
6-1-1			1.350 E_{rms}	1.351 E_{rms}	1.001	4		0.167 I_{dc}	0.408 I_{dc}	2.45	2.828 E_{rms}	1.28 EI_{dc}	1.81 EI_{dc}
3-1-2			1.170 E_{rms}	1.170 E_{rms}	1.001	4		0.167 I_{dc}	0.293 I_{dc}	1.76	2.45 E_{rms}	1.068 EI_{dc}	1.51 EI_{dc}
3-2-1			2.340 E_{rms}	2.341 E_{rms}	1.001	4		0.333 I_{dc}	0.579 I_{dc}	1.74	2.45 E_{rms}	1.047 EI_{dc}	1.047 EI_{dc}

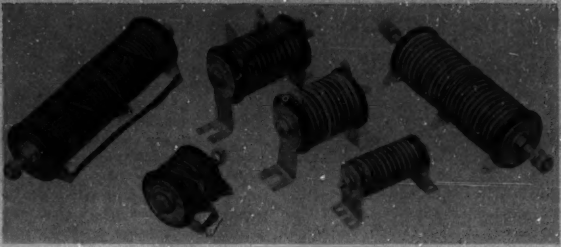


Figure 20. Typical selenium rectifiers for industrial applications

chemical work, electrolysis, shunt motors, and so forth. Therefore, the efficiency can be expressed as either

$$\text{RMS efficiency }\% = \frac{\text{a-c watts output}}{\text{a-c watts input}} \times 100$$

or

$$\text{D-c or average efficiency }\% = \frac{\text{d-c volts} \times \text{d-c amperes}}{\text{a-c watts input}} \times 100$$

In polyphase work, the rms efficiency approximately equals the average efficiency.

However, in single-phase work, there is a difference of approximately 15 points in efficiency between the average and rms values, the latter being the highest.

Typical efficiency curves are shown in Figure 13 at different voltages and at full-load current densities. Figure 14 shows correction factors at different temperatures.

LIFE TESTS

General Electric selenium rectifiers have undergone extensive tests and show promise of better aging characteristics than some of the European elements previously tested.[4] Typical life tests are shown in Figure 15. It should be noted that these tests represent aging as seen from the user's viewpoint.

Practical Rectifier Design

It is believed that a worked-out example will show how to use the data contained in this paper.

For example, assume that we want a 30-volt rectifier at one ampere working on a resistance load from a single-phase circuit. By reference to the tabulation on ratings, we find that the most voltage we can obtain from a unit bridge rectifier at full current density for each type cell is 12 volts. Dividing 30/12 = 2.5, therefore, three is the minimum number of series cells to be selected.

The voltage to be supplied by each unit bridge rectifier will be 30/3 = 10. By reference to Figure 9, we find that, at full-load current and ten volts, we need 13.2 volts alternating current per bridge, or 3 × 13.2 = 39.6 for the rectifier under discussion.

The size of the cell can be determined by reference to the tabulation on ratings. To carry one ampere a type *C* cell having $2^3/_{16}$-inch diameter is required. If the current rating required does not match that of the four available sizes, then it will be necessary either to operate at slightly less than full rating or to use several smaller cells in multiple.

From the aforementioned calculations, to obtain 30 d-c volts at 1.0 ampere direct current, a bridge rectifier using three cells in series would be required. The a-c input is 39.6 volts, and the transformer should have sufficient taps to take care of line voltage variations. To take care of aging, it is advisable to tap the transformer up to 3×18 rms volts or 54 volts. The approximate efficiency is 63 per cent at 25 degrees centigrade as seen from Figure 13 and will be higher at the operating temperature as shown on Figure 14.

Construction of Selenium-Rectifier Stacks

Selenium rectifier cells are assembled into either full-wave or half-wave stacks, depending on their voltage and current rating. Low-voltage stack can generally be assembled into single stack, using a construction as shown in Figure 16.

For higher voltages, two stacks are sometimes used, embodying a construction as shown in Figure 17.

For still higher voltages four units are used as shown in Figure 18.

The cells are spaced to permit free ventilation on both sides, light spring washers being used to collect current.

The stacks are clamped under light pressure and come either with or without mounting brackets as shown in Figure 20.

INSTALLATION OF RECTIFIER STACKS

Selenium rectifier stacks should be installed in well ventilated cabinets to permit free circulation of air.

They should be located preferably at the bottom of cabinets so that any heat from other heat dissipating apparatus does not have a cumulative effect.

If installed in closed cabinets, a certain amount of derating should be made to take care of the higher internal ambient temperatures as previously explained.

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A New High-Interrupting-Capacity Fuse for Voltages Through 138 Kv

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ASSOCIATE AIEE

Synopsis: The reliable performance of high-interrupting-capacity fuses in the intermediate voltage field has led to an increased interest in the application of such fuses for the higher voltages. Unfortunately, a fuse for the higher voltages cannot be made by simply enlarging or lengthening the lower-voltage devices. To interrupt the higher currents in a reasonable structure, arc lengths must be kept short to reduce mechanical stresses. Interruption in these short distances creates high dielectric stresses external to the fuse that would endanger the operation of a device built along conventional lines. Adequate conductors for carrying the higher currents through the longer-length fuse must be moved quickly during interruption.

A new high-voltage fuse in which interruption of the higher currents is accomplished in a short length without undue voltage stresses adjacent to the fuse has been developed. This fuse has a new arrangement of conductors which permits rapid extension of the arc without expulsion of any conductor parts from the fuse. All this has been accomplished with a fuse of such light weight that it permits the use of a drop-out design which is a necessity at the higher voltages to prevent subsequent flashovers caused by leakage currents. The interrupting medium is compressed boric acid which has proved very effective and reliable at the lower voltages. Repeated interrupting and mechanical tests have demonstrated the suitability of the new fuse for service on important transmission circuits.

PRESENT day requirements relating to the transmission of large blocks of electrical energy to large industrial areas and new war industry plants remote from power generation plants demand short-circuit protective devices that will afford the maximum protection with a minimum outlay of critical materials. The excellent operating record established by modern high-interrupting-capacity power fuses in the intermediate voltage field has created an insistent demand for fuses suitable for application on transmission systems in the 44-kv to 132-kv class. Power fuses require less critical material than a conventional hook-stick-operated disconnect switch and can be mounted in the same space, thus effecting economies in substation space and structure. Certain operating characteristics of high-capacity power fuses provide for the maximum continuity of service so essential to modern production methods. Power fuses when applied for the short-circuit protection of transformers or circuits provide ultrahigh-speed clearing of faults. Faulted equipment is isolated from the system in a small fraction of the time required by conventional short-circuit protective devices, thus greatly reducing the damage to the equipment and enabling it to be repaired and returned to service at a much earlier date. Operating speeds equal to or less than the most modern ultra-high-speed circuit interrupters insure maximum system stability since the voltage dip associated with a fault persists for as short a period as one cycle. The short duration of voltage fluctuation insures that relays elsewhere on the system will not operate to stop important continuous process loads. The high-speed operation of power fuses reduces the requirements for elaborate relaying schemes on new transmission circuits and eliminates the need for extensive relay changes when new load centers are added to an existing transmission line.

Requirements for Higher-Voltage Power Fuses

Since electrical power circuits operating at 44 to 132 kv are intended for the transmission of large blocks of energy, it follows that such systems are connected to a generating source of considerable capacity, capable of delivering a correspondingly high value of short-circuit energy to a fault. Unlike conventional circuit breakers which require as long as eight cycles to interrupt a fault and are so rated, power fuses frequently operate in the first half cycle of a fault; hence they may have to interrupt a fully displaced or asymmetrical current. For the systems under discussion the rms value of this current may be as much as 1.6 times the symmetrical short-circuit current. This ratio of currents is dependent on the characteristics of the transmission circuit. The closer the fault is to the generating source the greater ratio of reactance to resistance and the higher the ratio of asymmetrical to symmetrical current which approaches the theoretical limit of 1.73. From this it can be seen that power fuses for use on high-voltage circuits must have an interrupting capacity considerably in excess of the symmetrical short-circuit current. To handle the most severe case a fuse for 1,000,000-kva systems must be able to interrupt at least 1.6 times the current value corresponding to a 1,000,000-kva symmetrical fault. It is obvious therefore that a fuse for use at the higher voltages

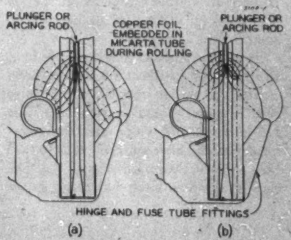
PLUNGER OR ARCING ROD
COPPER FOIL EMBEDDED IN MICARTA TUBE DURING ROLLING
PLUNGER OR ARCING ROD
HINGE AND FUSE TUBE FITTINGS
(a)
(b)

Figure 1. Approximate plot of dielectric field at lower end of fuse hit

Note how electrostatic shielding in diagram b reduces the lines of force or potential gradient at the lower fittings. The reduction of potential gradients at these points eliminates the possibility of external flashovers

(a). Without electrostatic shield

(b). With electrostatic shield

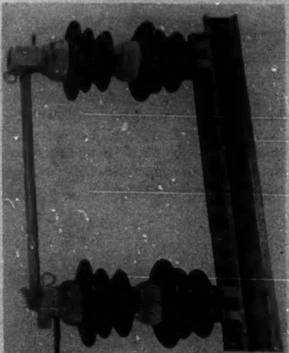

Figure 2. Single-pole unit, 92 kv, 200 amperes

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must have a high interrupting capacity.

A second requirement is the ability to interrupt positively the entire range of fault currents from the minimum melting current of the fusible element up to its full interrupting rating. While such fuses are normally applied solely for short-circuit protection, it is recognized that certain types of high impedance faults may develop, transformer secondary protective devices may be inoperative, or that faults may develop in a period of light generating capacity, requiring interrupting at low values of fault current.

A further requirement, especially important at these higher circuit voltages, is that the restored voltage shall not overstress the insulating material, making up the fuse tube so that there are high leakage currents which may result in flashover or dielectric breakdown. Since circuits of this voltage are sometimes supplied from hydroelectric plants where the sudden dropping of a large load may cause "runaway" of the generators before protective relays operate, the restored voltage may rise to a value double the normal circuit voltage. These conditions make it imperative that the higher-voltage fuses be automatically removed from the circuit immediately after operating.

Additional requirements particularly important to enable operating personnel to locate blown fuses and replace these with new units are positive indication of blown fuses and a lightweight simple unit that can readily be removed and replaced in the fuse support.

Evolution of the New Fuse

An analysis of the characteristics of the dry-type high-interrupting-capacity fuses for operating voltages up to 34.5 kv reveals that there are four major design features which have been responsible for its exceptional performance record.

1. Boric acid in the dry molded form as the lining of the interrupting chamber will interrupt a much higher voltage per inch of length than horn fiber on other fuse-tube materials in an equivalent structure. Since this is especially true in the lower range of currents, fuses utilizing boric acid can be made in a shorter length. At the higher currents this shorter-length boric-acid-lined fuse tube does not produce as vigorous a gas blast as the equivalent longer length of fiber; consequently, mechanical stresses in the tube structure are much less. In addition, the voltage gradient that can be interrupted increases with current much more rapidly. The gases evolved from boric acid are noncombustible and have a much higher dielectric strength than gases from organic materials and are much less liable to cause breakdown between adjacent line parts.

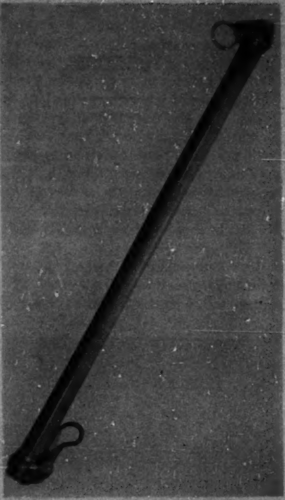

Figure 4. Fuse unit with disconnecting fittings

2. The very high interrupting capacity of this type of interrupter is largely due to the use of a very short fuse element in an enlarged opening of the fuse tube that is freely vented and the drawing of the arc into a more restricted section of the interrupting chamber. There is sufficient gas blast from the boric acid even in this enlarged opening to interrupt the higher currents at the end of the first half cycle of arcing without developing excessive pressures.

3. The use of a solid-rod-type conductor through the arcing chamber further increases reliability of high current interruptions, since there is no flexible conductor or cable to become jammed in the fuse tube by the gas pressure developed on very high fault currents.

4. Interruption of extremely high currents without excessive internal pressure and without severe voltage surges is further accomplished by the use of controlled mass and acceleration of the arcing terminal through the interrupting chamber. The mass of the arcing rod and of the operating spring and shunt assembly is so co-ordinated with the tension of the spring that the arcing rod is accelerated only a few inches or only a fraction of the length of the interrupter in the first half cycle. This short-length arc in the enlarged opening results in minimum mechanical stress in the fuse structure.

The first problem of the designer in converting low-voltage fuse structures to a design suitable for the higher voltage is to determine a suitable length of fuse. Since in the interests of simplicity of handling and replacement by operating personnel, it is desirable to have a disconnecting-type fuse, the minimum length of the fuse is automatically set by the standards for such types of equipment. The actual length of the fuse unit is then dependent upon the required length of interrupting chamber to secure efficient interruption throughout the entire current range. The voltage gradient that can be interrupted in a boric-acid interrupter varies with the diameter of the openings in the boric acid. Furthermore the voltage gradient is not constant for a given bore but varies with the length. For longer lengths, the voltage that can be interrupted corresponds to approximately the 1.7 power of the length. A large bore is required for in-

Figure 5. Sleet hood with dropout mechanism

Note the trigger which is actuated by the moving plunger to initiate the dropout action

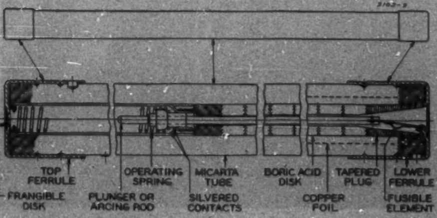

Figure 3. Cross section of new fuse unit

The unit for 138 kv is 72 inches long and 2 1/2 inches in diameter terruption of the higher fault currents to minimize mechanical stresses; however, it would be impractical to build such high-voltage fuses with a constant bore as the length required to interrupt currents in the lower range would be excessive. To make the fuse as short as possible, a stepped or tapered bore is used with the largest opening in the vented end so that gases are readily vented on high-current shots and the bore gradually decreasing to a diameter just larger than the size of the conductor rod. In this way, the fuse can be made just as short as the permissible open-gap spacing for a disconnecting-type unit, since the lower currents are readily interrupted when the arc is drawn into the more restricted bore. The use of the conventional spring and shunt assembly is not practical in the higher-voltage fuse as the collapsed length of such an assembly is approximately one third the length of the entire fuse unit. Therefore, to make the fuse unit as short as desired, a new method of conducting current from the top ferrule to the solid arcing rod has been developed which occupies a length equivalent to the length of the interrupting chamber.

After the minimum length has been established, the next step in the evolution of the higher-voltage fuses is to determine the form of the fuse unit. From a weight and handling standpoint, the use of insulating tube of organic material with a porcelain weatherproof casing is at once ruled out. Since it is recognized that organic insulation, such as a fuse tube, should not be subjected to high-voltage stress, such as restored voltage, after the fuse has blown, a drop-out-type fuse is indicated.

The decision to make a drop-out-type fuse entails further design problems, notably the necessity for a lightweight construction to eliminate any shock to the insulator column supporting the pivot end of the long fuse tube. The use of a permanent fuse holder with a renewable element or refill as has been used with such success in the lower-voltage fuses must be eliminated to avoid duplication of structure. In a design incorporating a renewable refill, the refill unit must be mechanically strong enough to withstand the entire pressure developed in the interupting chamber during interruption of the maximum fault current and as the pressures in the refill are communicable to the fuse holder, the holder must be of equally sturdy construction. The decision to make the higher-voltage fuses in a single nonrenewable unit was aided by early tests on a refill- and holder-type unit. With the lengths involved for the higher voltages it is virtually impossible to maintain the extremely close clearances between the outside of the refill tube and the inside of the holder tube required to prevent flashover in this space. The presence of ionized gases within the holder and the high-voltage gradients present in this space on high current interruption were very conducive to flashovers in this space. As the length of the interrupter increases, the voltage that can be interrupted increases faster than the dielectric strength of the adjacent air gap.

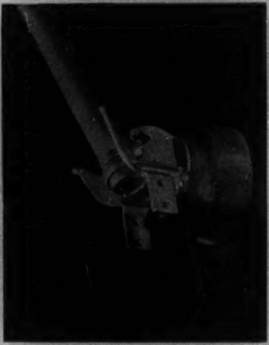

Figure 6. Hinge with fuse unit

With lightweight construction a paramount requisite, it is imperative that the interrupter be very efficient so that the mechanical stresses resulting from gas pressures developed do not require an excessively strong and heavy structure. The efficiency of an interrupter is based on building up high dielectric strength in the arc path at current zero with low arc voltage and correspondingly low arc energy. An efficient fuse design is based on drawing an arc as short as possible in a structure offering a minimum of actual restriction to the arc core and develop sufficient un-ionized gases to effect interruption at first current zero. The interruption of high currents in such a short length imposes very severe dielectric stresses between the end of the arcing rod and the lower terminal of the fuse with its associated fittings.

To prevent external flashovers resulting from the presence of ionized air associated with such extreme voltage gradients, an electrostatic shield has been built into the tube structure. Figure 1*a* shows the voltage stress lines between the end of the arcing rod and the lower terminal. It is readily seen that very high voltage gradients are present at the fittings adjacent to the tube. The corona associated with such high-voltage stresses creates sufficient ionized air surrounding the tube to cause external flashover over the entire length of the fuse unit. Figure 1*b* shows the voltage-stress distribution with an electrostatic shield in the insulating tube. It will be observed that the maximum voltage gradient is now impressed on solid dielectric and not on the air adjacent to the fuse tube, so that external corona is eliminated.

A further requisite of the drop-out type of fuse construction, particularly for the voltages under consideration, is that the drop-out action must not start until the circuit is positively interrupted within the fuse unit. If the fuse unit should start to drop out before the current is interrupted, there will be arcing at the top contacts which would create sufficient ionized gases to cause flashover.

To secure positive time delay between the time of fusion of the calibrated element and the initiation of the drop-out action, a construction has been adopted whereby the drop-out action is not initiated until the moving arcing rod has reached the end of its travel.

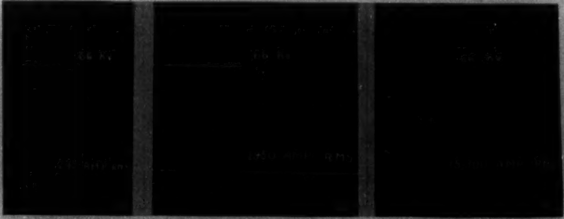

Figure 7. Oscillograms of interrupting tests on 69-kv fuse

(*a*). 240 amperes 66-kv restored voltage
(*b*). 1,950 amperes 66-kv restored voltage
(*c*). 15,300 amperes 66-kv restored voltage

Construction and Operation

A complete fuse consists of a fuse unit which is nonrenewable and is replaced by an entirely new unit after fault interruption and a fuse mounting which includes the necessary fittings to convert the fuse unit into a disconnecting unit. Figure 2 shows this assembly in the closed position.

The Fuse Unit

The new high-interrupting-capacity fuse unit consists of a short low-temperature calibrated element at the end of a solid conductor rod extending through a boric-acid-lined chamber, a copper tube for conducting current from the upper ferrule to the arcing rod through a modified form of tulip contacts and enclosing the operating spring which withdraws the arcing rod from the interrupting chamber, all enclosed in a lightweight Micarta tube with copper ferrules as shown in Figure 3.

The insulating tube is made from a rolled Micarta tube selected for high mechanical and impact strength, high dielectric strength, resistance to moisture absorption, and freedom from warping. The electrostatic shield at the lower end of the tube consists of copper foil rolled into the tube during fabrication in the same manner as used in the construction of high-voltage condenser bushings. This foil is electrically connected to the lower ferrule of the fuse unit. Copper ferrules are shrunk onto the fuse tube and then rolled.

The lower half of the fuse tube is lined with molded boric-acid blocks cemented in the tube with a moisture-resistant high-dielectric-strength cement. The diameter of the opening in the boric acid is a maximum at the bottom of the interrupting chamber tapering down to a diameter slightly larger than the size of the arcing rod at the top. The length of boric acid having maximum opening is chosen so that interruption of the maximum fault currents will take place when the arcing rod has traveled a distance sufficient to prevent severe voltage stress. Moderate fault currents are interrupted when the arcing rod has traversed the intermediate taper, and low currents are cleared when the arcing rod is drawn into the final taper. To insure that low fault currents corresponding to the minimum melting current of the lower rated fuse elements are cleared in the shortest possible arcing time, the arcing rod and the bore of the top section of boric acid are of smaller diameter than that of the rod and boric-acid blocks used for the high current ratings. The minimum current to be interrupted by a fuse will be, of course, dependent on its ampere rating.

An enlarged section at the top end of the arcing rod engages the flexible contact fingers which are part of the conductor tube to the top fuse-tube ferrule. The contact surfaces are heavily silver-plated for low contact drop for the life of the fuse under all atmospheric and temperature conditions. A stainless-steel compression spring assures uniform and continuous contact pressure without binding. These contacts are so biased that the effort required to withdraw the contact from the fingers is only a small fraction of that exerted by the stainless-steel operating spring which is terminated on this enlarged section of contact rod.

Figure 8. Oscillograms of short-circuit tests on 92-kv fuse

(a). 167 amperes, 88-kv restored voltage

(b). 3,600 amperes, 88-kv restored voltage

The calibrated element in this new fuse is of the low-temperature type so that operation at or near full-load rating will not create temperatures that will dehydrate the boric-acid lining or impair the strength of the fuse tube. A high-strength strain element relieves the calibrated element of all spring tension.

When the calibrated element is fused by a fault current, the operating spring draws the arcing rod through the boric-acid arcing chamber. The heat of the arc decomposes the surface of the boric acid into water vapor and inert boric oxide. The blast of water vapor deionizes the arc path and effects interruption. The arcing rod, which is not rigidly connected to the operating spring does not stop when the spring reaches its fully closed position but continues on through the spring by virtue of its inertia until the end punctures a frangible disk in the top ferrule. A special spring washer stops the travel of the arcing rod by arresting its enlarged section. This washer also functions to retain the arcing rod and prevent it from dropping back into the tube. The rod projecting from the top ferrule initiates the drop-out mechanism. It is obvious that the drop-out action cannot start until the arcing rod has traversed the entire length of the interrupting chamber which insures that all fault currents are interrupted before the tripping out action occurs.

The Fuse Mounting

The fuse-unit fittings which comprise

Figure 9. Oscillograms of short-circuit tests on 115-kv fuse

(a). 145 amperes, 110-kv restored voltage

(b). 4,400 amperes, 110-kv restored voltage

Figure 10. Oscillograms of short-circuit tests on 138-kv fuse

(a). 180 amperes, 132-kv restored voltage

(b). 5,300 amperes, 132-kv restored voltage

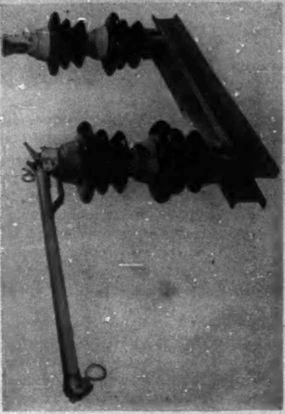

Figure 11. Single-pole unit, 92 kv, 200 amperes, with fuse. Unit in dropout position

a part of the fuse mounting are shown clamped onto the fuse tube in Figure 4. These fittings, comprising a hook eye for the top end of the fuse unit so that the fuse unit can be used as a disconnecting switch and a hinge casting for the lower end of the fuse tube that incorporates a lifting eye to facilitate the removal and replacement of the fuse with a standard hook stick, are readily removed from a blown fuse unit and placed on a new unit. These fittings are so keyed to the fuse unit that the correct fitting can fit only on its end of the fuse unit and assures perfect alignment of the fittings.

The fuse mounting proper consists of a double steel channel base for the higher voltages, and two insulator columns with latch castings and hinge castings mounted thereon. The top contact and trip-out mechanism are enclosed in a sleetproof housing, as shown in Figure 5. Current is conducted from the terminal to the pivoted latch casting by means of a flexible copper shunt. A powerful stainless-steel compression spring insures adequate contact between the latch casting and the hookeye casting clamped to the top ferrule of the fuse unit. It will be noticed that the force exerted by this spring puts the fuse tube in compression and thus avoids warping of the fuse tube from continuous cantilever stress. The drop-out trigger is pivotally mounted on the latch casting and bears on a projection within the sleet hood. Tests have shown that the kinetic energy of the moving arcing rod is more than ample to insure positive tripping regardless of alignment, variations in length of the fuse unit, or any possible friction that might be due to weathering or corrosive atmospheres.

The construction of the hinge is shown in Figure 6. Current is conducted from the fuse unit to the terminal pads by copper alloy leaf springs. The lower or lifting eye casting on the fuse unit and the hinge casting are so designed that the fuse unit is positively guided throughout the disconnecting or closing-in movements. The fuse unit can be removed from the mounting only when the fuse unit is in the 180°-open position. This arrangement also provides for the lower insulator stack taking the entire upward reaction resulting from gas blast on high current interruption thus insuring proper action of the drop-out mechanism. Below the hinge casting is a simple leaf spring bumper to absorb the energy of the falling fuse unit without overstressing the insulator column. This arrangement was chosen in preference to a friction brake because of its simplicity, its reliability under all atmospheric conditions, and the amount of energy to be absorbed in braking the fall of the long fuse unit.

Test Results

Complete interrupting tests have been made in the high-power laboratory covering the range from the minimum melting current of the smallest fusible element up to destruction of the fuse. Test-circuit conditions were modified from a very severe circuit where voltage-recovery rates correspond to a natural frequency of approximately 7,000 cycles per second to circuits with a very low voltage-recovery rate corresponding to natural frequencies as low as 800 cycles per second.

The majority of the interrupting tests were made with full line-to-line voltage impressed across a single fuse unit with the lower terminal at ground potential. Other tests were made with line-to-line voltage across a single fuse with the neutral grounded at the fuse mounting base. Also the fuses have been tested at line-to-neutral voltages to determine the upper current limit at reduced voltage.

Representative oscillograms of interrupting tests are shown in Figures 7 to 10.

The new assembly for carrying current through the fuse unit has been thoroughly tested for reliability on all values of fault current up to those which cause destruction of the fuse unit. In addition, high-current short-duration faults that did not blow the largest fuse rating were applied. In no case was there a failure of these parts to perform satisfactorily. In the case of the high currents applied momentarily there was no welding or damage that might cause failure to operate on subsequent faults. Prolonged temperature-rise tests indicate the complete adequacy of all current-carrying parts and contacts. The silvered contacts on the fuse rod within the fuse unit carried full rated current with an unusually low temperature rise.

High-speed moving pictures were used extensively during interrupting tests as a check on the operating sequence of the drop-out action and for checking the time required to trip out. These moving pictures were of invaluable assistance in locating the cause of flashovers in the early designs. They also presented an accurate picture of the size and shape of the cone of gases evolved from the fuse, thus enabling the designer to design an opening so that gases would not be deflected in such a way as to cause flashover of adjacent apparatus.

Application

The selection of a high-voltage fuse for isolation of faults in potential transformers does not require any special precautions, as the fuse can operate only on a fault in the connected equipment. When high-voltage transmission system is tapped to obtain small blocks of power, it is recommended that high-voltage fuses be connected as close to the power transformer as practicable and that sec-

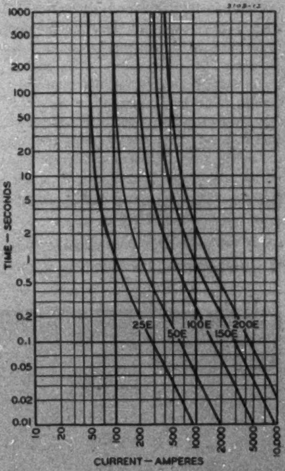

Figure 12. Melting time—current curves for representative fuse ratings

Subcenter Switching Systems for Teleprinter Tie Lines

F. L. CURRIE
MEMBER AIEE

Synopsis: One of the means used by the telegraph companies for improving the speed of their service is the provision of printing telegraph circuits between their central offices and the offices of many users of the telegraph. In the beginning, circuits of that type were restricted to patrons whose volume of telegraph business and proximity to a central office justified the expense involved in their installation and maintenance. A large part of that expense, when the patrons are located remotely from the central office, is in the line-wire costs.

This paper describes an automatic switching means for economically enabling a large number of patrons, when grouped in one locality, to be furnished printing telegraph service with a distant central office by causing them to share the use of a comparatively small number of line wires between that locality and the distant central office.

This paper also cites benefits gained from present installations.

AMONG the methods used by the telegraph companies during the last two decades for improving the speed of their service has been the provision of printing telegraph circuits between their central offices and the offices of many users of the telegraph service. That type of accommodation employes the use of the printing telegraph mechanisms, variously termed "teleprinters," "teletypewriters," or "teletypes," and is known in the Western Union Telegraph Company as teleprinter tie-line service.

Because of the expense involved in providing and maintaining the equipment and line wires essential to that service, it has been restricted to those patrons whose proximity to the central office and volume of business warranted the expenditure. Therefore, many substantial users of the telegraph service, because of their remoteness from a central office, had to be denied the advantages of teleprinter tie-line service because of the high line-wire costs involved. In some cases even groups of patrons were in that category.

The Western Union Telegraph Company undertook several years ago the development of a means for bringing those groups of remote patrons within the scope of teleprinter tie-line service by reducing the amount of line wire required.

Long experience in serving patrons over teleprinter tie-lines through central-office concentrators[1] has shown that most of those lines are in use only a small part of the time, and that the probability of any large percentage being used at any one time is small. That knowledge of their normal requirements led to the development of a remote controlled switching means for enabling a large number of patrons, grouped in one locality, to share the use of a comparatively small number of line wires to a distant central office. In that manner the necessary line-wire savings are effected to make economically feasible the extension to many more patrons the faster service afforded by teleprinter tie-lines.

The switching means, termed "subcenter," is designed to be installed in the same locality as the group of patrons, to be connected to each patron's office by an individual line wire, termed "patron's line," and to be connected to the central office by only as many line wires, termed "trunks," as the peak volume of business at any one time requires. An installation containing a subcenter is termed a "subcenter switching system."

Description

EQUIPMENT

The equipment required for a subcenter switching system is divided into

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ondary circuit breakers or other protective devices be used to interrupt all secondary faults. The high-voltage fuses will then operate only on faults in the power transformers. For such applications the fuse rating selected must provide adequate time for co-ordination with the relays controlling the secondary breaker. The proper rating should be determined from the time–current characteristic curves of the fuses and will usually exceed four times the full-load current of the transformer.

The addition of fuses on a system to isolate faults in equipment will enhance the reliability of the entire system by the rapid interruption of short circuits that would otherwise result in breaker operations or even in ground that might be difficult to locate.

At the higher transmission voltages it is generally not advisable to use fuses to protect even a small tap line that is exposed to lightning, unless the nature of the load is such that an outage caused by fuse operation can be tolerated.

Conclusions

Numerous interrupting tests, covering the complete range of fault currents under all possible circuit conditions, show that these new fuses are entirely satisfactory for application to important high-voltage transmission circuits. Their interrupting ability is such that they may be safely applied to systems approximating 1,000,000-kva short-circuit capacity at 66 kv through 132 kv.

The positive time-delay drop-out action provides an air gap in the circuit after operation which has a breakdown voltage ten per cent greater than the breakdown voltage of the basic impulse level (BIL) insulator columns. These fuses may therefore be applied on hydroelectric transmission circuits where sudden interruption of load currents may cause overshoot of recovery voltages.

These new high-interrupting-capacity power fuses can now be applied to important high-voltage transmission systems to rapidly clear faults in connected equipment, thereby enhancing the reliability of the complete system. They also provide an economical means for tapping off small blocks of power from high-capacity systems.

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2. HIGH-VOLTAGE FUSING OF TRANSFORMER BANKS, H. H. Marsh, G. B. Dodds. AIEE TRANSACTIONS, volume 61, 1942, July section, pages 533–5.

3. SYSTEM SHORT-CIRCUIT CURRENTS, W. M. Hanna, H. A. Travers, C. F. Wagner, C. A. Woodrow, W. F. Skeats. AIEE TRANSACTIONS, volume 60, 1941 September section, pages 877–81.

three general groupings, one for the subcenter, one for the central office, and one for the patron offices.

SUBCENTER

The subcenter switching equipment consists primarily of electrically operated multicontact relays and multicontact switches mounted either on racks or in cabinets, and in their assembled form termed "subcenter switching units." The relays are used primarily for control and signaling purposes, and the switches for making connections between the trunks and the patron tie-lines.

The switches are of the well-known Strowger type of two-motion and rotary switches. The two-motion switch is used to select the desired patron's line wire when a call is originated at the central office, and the rotary switch is used to seek the patron line wire when a call is made from the patron's office. The corresponding contacts of the contact banks which form a part of the two-motion and rotary switches are connected in multiple so that all of the patron tie-lines are available to all of the switches. The wipers of each switch are associated with a group of equipment individual to one trunk and complete the connections between that equipment and the equipment associated with the patron's line.

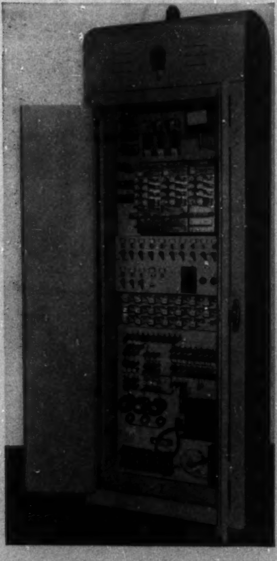

Figure 1. Small-size subcenter switching unit for ten patron tie lines and three central-office trunks

Three types of trunks, termed answering, calling, and combination trunks, are used for connecting the subcenter to the central office. The "answering trunks" are the trunks over which connections are made only when the calls are originated at patron offices; the "calling trunks," only when the calls are made at the central office; and the "combination trunks," when the calls are made at either the patron offices or the central office. The combination trunks, with the exception of the smallest size installations, require as much equipment in the subcenter switching units as an answering and a calling trunk combined, and, therefore, but few of them are used in any one installation. When installed, they are included primarily as overflow trunks, since they function as either answering or calling trunks.

CENTRAL OFFICE

The central office equipment may consist either of a group of operating tables, termed "subcenter operating tables," with each table connected directly to a trunk, or of existing teleprinter concentrator equipment.

The subcenter operating tables are classified as "answering," "calling," or "combination," the classification depending on the type of trunk with which they are to be used. The equipment required is basically the same for all of them. In general, it includes a teleprinter, a signal lamp, control relays, and in the case of a calling or combination trunk, a dialing unit. The teleprinters, signal lamps, and dialing units are mounted on the table tops and the control relays in a metal box underneath.

The subcenters also are designed to direct the calls of all patron offices to the central office over as few trunks as possible by always switching the patron lines to the lowest numbered idle trunk. That feature enables them also to perform the functions of an automatic concentrator for the central office. Although they function as concentrators themselves their central-office trunks are often connected to concentrators at the central office for converging the business of one subcenter with that of another, and also with that of the patron tie-lines connected directly to the concentrator.

The equipment of teleprinter concentrators consists, essentially, of turrets, control relays, signaling lamps, and operating tables. Each turret contains a large number of jacks, and one is located convenient to each operating table. Each turret jack is multipled to the corresponding jack of all of the other turrets, and each of the multiple connections is connected to a separate incoming line which may be either a teleprinter tie-line or an answering trunk from a subcenter. Thus, all of the directly connected patron tie-lines and the answering trunks from the subcenter are connected to all of the turrets so that any line or trunk can be worked from any turret.

Each operating table of the concentrator contains, primarily, a teleprinter and a circuit from the teleprinter through control relays to a cord and plug located at a turret. As many operating tables are provided as are likely to be required at any one time. Connection is made between an operating table and a desired patron's line or answering trunk from a subcenter by inserting the plug of that operating table in the proper jack of the turret.

Each of the calling trunks from a subcenter is connected directly to one of the operating tables of the concentrator and can be worked only from that operating table. A dialing unit containing a dial, similar in general appearance to the dials used in automatic telephone systems, and a control key switch, is added to the equipment and circuits which normally form a part of those operating tables. The key switch is used for adapting the operating table to work with either its directly connected calling trunk from a subcenter or with any line connected to the turret jacks, and the dialing unit is used to select the line of any of the patron offices connected to the subcenter by controlling the selecting mechanism of that equipment.

Each combination trunk from a subcenter is connected both to a multipled jack circuit in the turrets and to one of the operating tables, since it functions as either an answering or calling trunk.

When the combination trunk is in use as an answering trunk, it is automatically disconnected from the operating table to which it is normally connected, and, when it is used as a calling trunk, it is automatically disconnected from the turrets. Also, when it is used as a calling trunk, the equipment associated with it at the subcenter automatically busies it to all patron calls. Hence, safeguards are included to prevent interference to service from any attempt to use this circuit for its two functions at the same time.

PATRON'S OFFICE

The equipment at the patron's office consists, primarily, of a teleprinter, a signal lamp, a polar relay, and a teleprinter operating table. That is the same teleprinter tie-line equipment which is used for operation with concentrators. The signal lamp is controlled by the polar relay and serves to advise the operator when the central office calls.

STANDARD SYSTEMS

Each of the first subcenter switching systems was devised for a specific installation, but the later ones were designed as standard systems for general usage. The latter differ from the first in flexibility, in the ultimate sizes of the installations, and in some details of circuit design. They were designed for three different sizes of installations, small, intermediate, and large, so that an economically feasible one would be available for any probable requirement.

SMALL SYSTEM

The smallest of the systems enables groups of ten patrons to be served by a remote central office over three trunks. The subcenter switching equipment is housed in a single cabinet as illustrated in Figure 1.

Combination trunks, only, are provided between the central office and the subcenter, and the same equipment at the subcenter is used for calls originating at either the central or the patron's office. This differs from the intermediate and large systems, described later, in that those systems require two sets of equipment at the subcenter, one calling and one answering, for each combination trunk.

Provision is made in the design of the units for connecting two of them together in a single installation to provide for 20 patrons and six trunks when the requirements grow beyond the capacity of one unit.

INTERMEDIATE SYSTEM

The next, or intermediate, size system is designed to serve groups of 25 patrons over eight trunks to the central office. The equipment of the subcenter, like that of the small system, is housed in a single cabinet (Figure 2).

Normally, three calling and five answering trunks are provided between the subcenter and the central office. Combination trunks may be provided by connecting together, at the subcenter, the equipment of one calling and of one answering trunk for each combination trunk desired. Hence, each combination trunk provided reduces by one the total number of central-office trunks that can be accommodated by the subcenter unit. A maximum of three combination trunks may be provided in this manner for each unit.

Provision also is made in the design of the subcenter switching units of this system for connecting two of them together in a single installation to double the capacity to 50 patrons, six calling and ten answering trunks.

LARGE SYSTEM

Each of the small and intermediate systems requires that all of the patrons be connected to the one subcenter in order to share the trunks to the central office. The large system, in addition to serving a much larger number of patrons, also provides for serving the trunks of as many as seven smaller size subcenters. Thus, the large system may consist of one main subcenter serving 200 patron tie-lines and as many as seven tributary subcenters, each of which may serve 25 patron tie-lines, combined into a single large system serving a total of 375 patrons. The main subcenter, to which the central-office trunks are terminated, is termed the "subcenter," while the seven tributary subcenters are termed "sub-subcenters." The sub-subcenters may be either the 10 or the 25 patron size already described. Combining a large number of patron tie-lines in one system in that manner considerably reduces the number of trunks that would be required between the locality of the patrons and the distant central office if those patron tie-lines were served by several smaller systems. The plan of this system is illustrated by the diagram of Figure 3.

The equipment of the subcenter is assembled in nine units (see Figure 3). Two of the units contain that portion associated directly with the calling trunks; two, that portion associated directly with the answering trunks; one, that portion associated directly with the sub-subcenter answering trunks; and, four, that portion associated with the directly connected patrons. Each calling trunk unit provides for eight calling trunks to the central office and ten sub-subcenter calling trunks; each answering trunk unit, for 16 answering trunks to the central office; each patron tie-line unit, for 50 patron tie-lines; and, the sub-subcenter answering trunk unit for 35 sub-subcenter answering trunks. Hence, an installation equipped for maximum capacity operation provides for 16 central-office calling trunks, 32 central-office answering trunks, 200 directly connected patron tie-lines, and 55 sub-subcenter trunks.

All of the patrons, regardless of whether they are connected to the subcenter directly or by sub-subcenter trunks, share the use of all of the central-office trunks.

Figure 2. Intermediate-size subcenter switching unit for 25 patron tie lines and eight central-office trunks

The trunks between the subcenter and the central office also may be converted to combination trunks by combining the equipment in the subcenter of one calling and of one answering trunk for each combination trunk desired. A maximum of 16 combination trunks can be obtained in that manner.

Operation

All of the subcenter switching systems mentioned in this paper operate fundamentally in the same manner.

Calls can be originated from either the central or any patron's office, but connections can be completed only between the central and a patron's office. No provision is made for making connections between any two patron offices.

After a connection is made, transmission may be carried on in either direction until the connection is released, regardless of which office initiated the call. The connections normally are released only from the central office.

The operations required at either the central or the patrons' offices for causing the subcenter switching equipment to effect a connection are so simple that no special training of the personnel is necessary for proper operation.

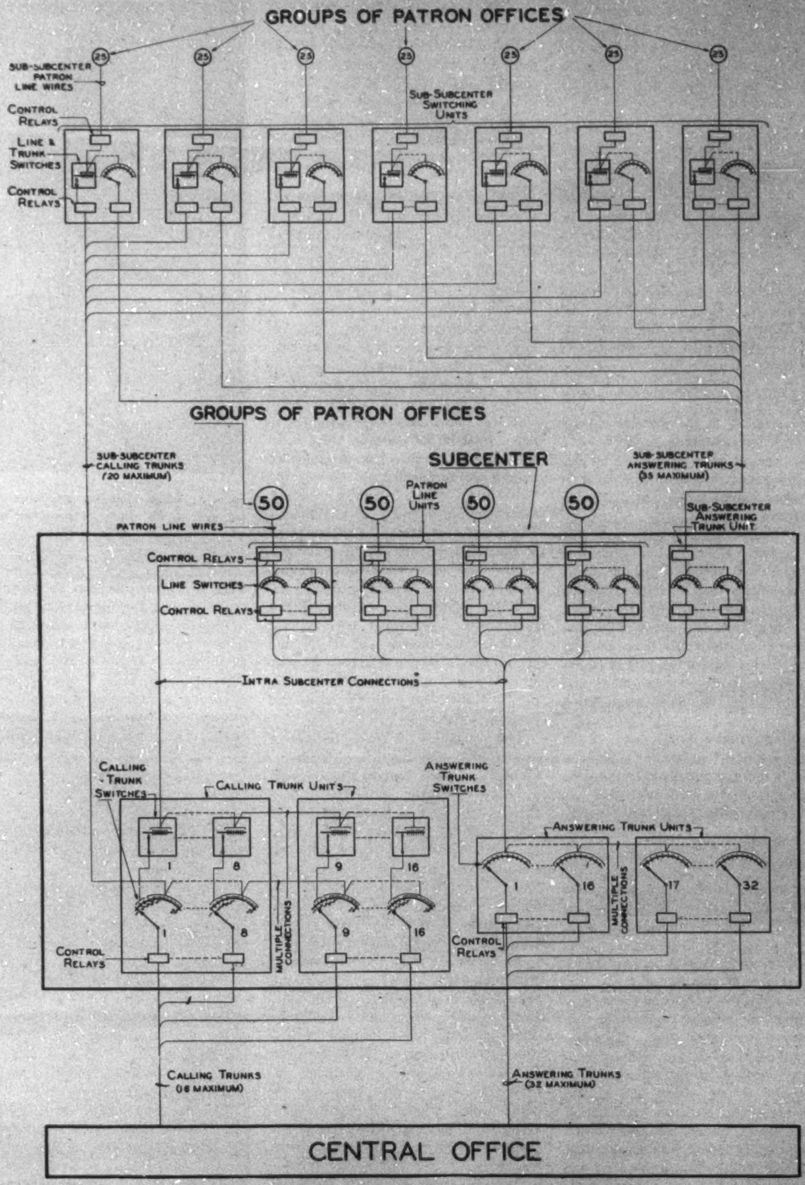
GROUPS OF PATRON OFFICES
25
25
25
25
25
25
25
SUB-SUBCENTER PATRON LINE WIRES
SUB-SUBCENTER SWITCHING UNITS
CONTROL RELAYS
LINE & TRUNK SWITCHES
CONTROL RELAYS
GROUPS OF PATRON OFFICES
SUB-SUBCENTER CALLING TRUNKS (20 MAXIMUM)
SUBCENTER
SUB-SUBCENTER ANSWERING TRUNKS (35 MAXIMUM)
PATRON LINE UNITS
50
50
50
50
SUB-SUBCENTER ANSWERING TRUNK UNIT
PATRON LINE WIRES
CONTROL RELAYS
LINE SWITCHES
CONTROL RELAYS
INTRA SUBCENTER CONNECTIONS
CALLING TRUNK SWITCHES
CALLING TRUNK UNITS
ANSWERING TRUNK SWITCHES
ANSWERING TRUNK UNITS
1
8
9
16
1
8
9
16
1
16
17
32
MULTIPLE CONNECTIONS
MULTIPLE CONNECTIONS
CONTROL RELAYS
CONTROL RELAYS
CALLING TRUNKS (16 MAXIMUM)
ANSWERING TRUNKS (32 MAXIMUM)
CENTRAL OFFICE

Figure 3. Diagram showing plan of large subcenter switching system

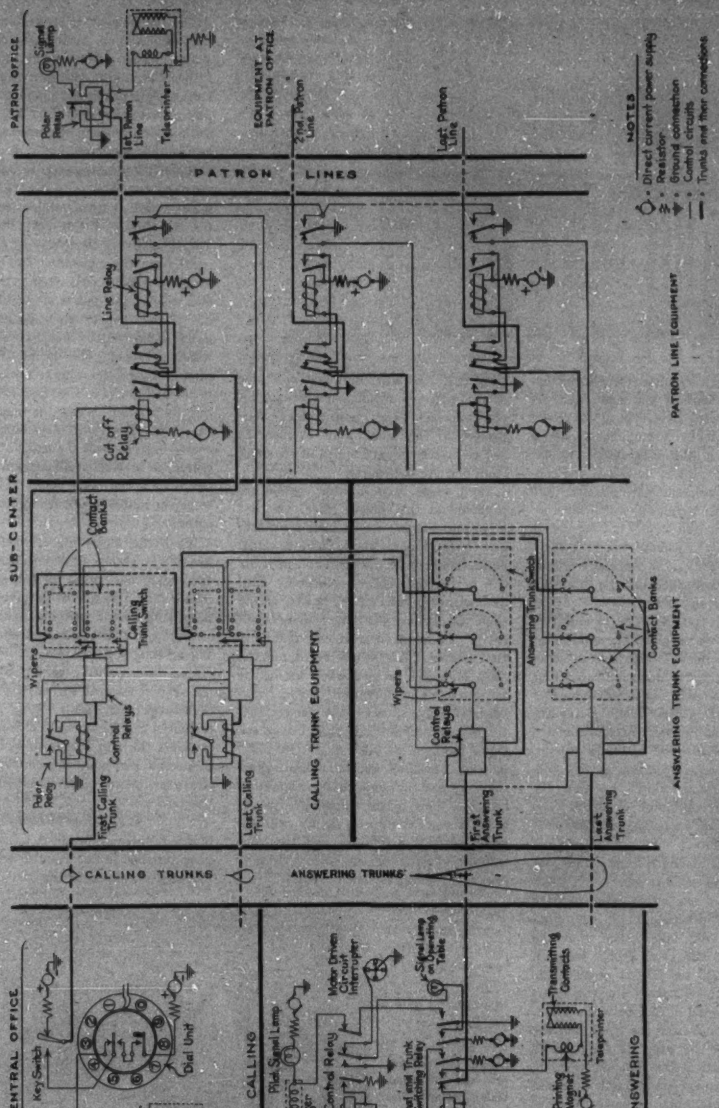
PATRON OFFICE
Signal Lamp
Polar Relay
1st. Patron Line
Teleprinter
EQUIPMENT AT PATRON OFFICE
2nd. Patron Line
Last Patron Line
PATRON LINES
NOTES
Direct current power supply
Resistor
Ground connection
Control circuits
Trunks and their connections
SUB-CENTER
Line Relay
Cut off Relay
PATRON LINE EQUIPMENT
Contact Banks
Calling Trunk Switch
Wipers
Control Relays
Polar Relay
First Calling Trunk
Last Calling Trunk
CALLING TRUNK EQUIPMENT
Answering Trunk Switch
Contact Banks
Wipers
Control Relays
First Answering Trunk
Last Answering Trunk
ANSWERING TRUNK EQUIPMENT
CALLING TRUNKS
ANSWERING TRUNKS
CENTRAL OFFICE
Key Switch
Dial Unit
Teleprinter
CALLING
Pilot Signal Lamp
Buzzer
Signal Control Relay
Motor Driven Circuit Interrupter
Signal Lamp on Operating Table
Signal and Trunk Switching Relay
Transmitting Contacts
Printing Magnet
Teleprinter
2nd Push Button
1st Push Button
ANSWERING

Figure 4. Simplified schematic diagram of the circuit and equipment used in a typical subcenter switching system

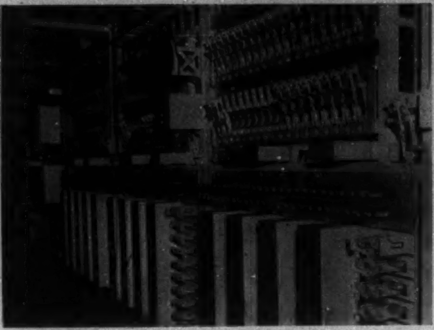

Figure 5. View of switching equipment at Newark for large-size subcenter-switching system

Figure 4 is a simplified schematic diagram of the circuit and equipment arrangements used in a typical subcenter switching system. The trunks are shown terminated on individual tables in the central office. Many details of the control circuits and relays are omitted from the diagram for reasons of simplicity.

A call originated at any patron's office is initiated by the patron's operator opening, momentarily, the normally closed line between her office and the subcenter by depressing any one of the keys of her teleprinter keyboard. The opening of the line allows a normally operated relay, termed "line relay," connected in series with the line in the subcenter switching unit, to release and lock itself released.

The release of that relay causes the wiper contacts of the switch associated with the lowest numbered idle answering trunk, through the medium of control circuits and relays, to seek the patrons' line containing that relay by advancing, step by step, over the contacts of its contact bank.

When that patron's connections are found, control circuits and relays associated with the respective switch and the patron's line function to disconnect ground from the trunk, and power from the patron's line, and after a brief lapse of time (about 0.3 second), to connect the trunk and the patron's line together. The brief interval of time between the removal of ground from the trunk and the connecting of the trunk to the patron's line wire constitutes a sufficient opening of the trunk to allow a normally operated relay whose winding is connected in series with the trunk at the central office to release and operate the central office signaling equipment.

When the answering trunks are connected directly to individual tables at the central office, the release of the relay, termed a "signaling and line switching relay," not only signals the operator but also connects the central-office teleprinter to the trunk and reverses the polarity of the power applied to the trunk from positive to negative.

Both visual and audible signals are provided at the central office. A pilot lamp and buzzer, conspicuously located, give a continuous alarm, and a signal lamp, mounted on the table to which the call is directed, flashes repeatedly until an operator presses a push button on the table top for operating the signal control relay. Those signals advise the operators that a new call has been received, and the push button is pressed when the call is answered. The operation of the signal control relay silences the buzzer, extinguishes the pilot lamp, and causes the signal lamp on the table to glow steadily, instead of flashing, and to continue glowing as long as the connection is allowed to remain. This alarm signaling arrangement is used to insure that all calls are given prompt attention, and the continuous glowing of the signal lamp is provided for supervisory purposes.

The reversal of the power applied to the trunk, from positive to negative, causes a polar relay, connected in series with the trunk at the subcenter, to reverse its armature and prepare the subcenter switching equipment for releasing the connection when the transmission of business is completed.

When the transmission is completed the central-office operator presses a second push button, which reoperates the signaling and line switching relay. The operation of that relay transfers the trunk from the teleprinter and power of negative polarity to a circuit through its own winding and power of positive polarity. It also opens the circuits to the signal lamp and the signal control relay winding causing the lamp to be extinguished and the relay to release. The reversal of power to the trunk from negative to positive causes the armature of the polar relay, whose winding is connected in series with the trunk in the subcenter, to reverse to its original position and release the connection. The trunk is then available for connection to another patron's line wire.

When the answering trunks are terminated at a teleprinter concentrator, the opening of the trunk at the subcenter causes only a signal to be given at the central office for identifying the trunk and attracting the attention of the operator. The reversal of power polarity to the subcenter, from positive to negative, is effected by the operator's inserting the plug of her operating table circuit into the proper jack of the turret. This operation transfers the trunk from its normal idle connection and positive polarity of power to the operating table circuit and negative power.

When transmission is completed, the plug is removed from the jack, causing the power applied to the trunk to be changed to positive, which is the idle condition

Figure 6. Section of the Western Union central office in New York. This view shows some of the subcenter operating tables used with the Newark subcenter switching system

polarity. Hence, the plug and jack, with their associated relays, perform the same functions at the central office when concentrators are used as the signaling and trunk switching relay and the push buttons when individual tables are used.

Calls are initiated at the central office by reversing the power applied to the trunk from positive to negative by operating a key switch and by dialing the numeral assigned to the desired patron office in the same manner as the digits of dial telephone numbers are dialed. The reversal of power polarity prepares the subcenter equipment, associated with the calling trunk, for responding to the dial pulses and for advancing the wiper contacts of the switch to the contacts of its bank associated with the desired patron's line.

When the proper contacts are reached, the patron's line is first tested automatically at the subcenter to determine whether it is idle or is busy. If it is busy, an automatic busy-signal transmitting device sends a busy signal over the calling trunk to the central-office operator. If it is idle, control relays at the subcenter associated with the trunk and the dialed patron's line function to disconnect the patron's line from positive power at the subcenter and connect it to the trunk line which, at that time, has power of negative polarity applied at the central office. This reverses the direction of current over the patron's line and causes a polar relay connected in series with the line at the patron's office to operate and signal the call. The patron's operator answers the call with her teleprinter keyboard.

When transmission is completed, the central-office operator restores the lever of her key switch to its normal idle position which restores the central-office circuits and equipment to their idle condition, and reverses the polarity of the power applied to the trunk for releasing the connection at the subcenter. The trunk then is available for connection to another patron's line.

Several interesting auxiliary features are contained in the switching units. One of them is a means for automatically wiping out false calls on patron's lines which might result in the event of a temporary interruption of the local power supply to the subcenter switching units. Since power is furnished the patron line wires from the subcenter switching units, any interruption of that power results in an interruption of the current flow over the line wires. Any interruption of the current flow over the patron's line wires allows the line relays of all the patron lines at the subcenter to release in the same manner as they do for bona fide patron calls. If automatic preventive measures were not included in the design, each patron line wire would be switched to the central office as fast as answering trunks became available until all had been connected to and released by the central office. The false call wipe-out feature functions after each power failure to prevent all switches associated with answering trunks from operating, to re-establish the patron line-wire relays at the subcenter to their normal idle operated condition, and then to allow all switches to operate normally again.

Another interesting feature is a provision for "busying" the equipment at the subcenter of any answering trunk to all new calls by opening the answering trunk circuit. That enables the central-office attendants to remove any trunk from service for maintenance or for any other reason, and it also automatically prevents the connecting of a patron's line to an open trunk.

Installations

Examples of the benefits which subcenter switching systems can render are afforded by the installations which have been in service for several years between patrons in Oakland and the central office in San Francisco, patrons in Beverly Hills and the central office in Los Angeles, and patrons in Newark and the central office in New York City.

The Oakland–San Francisco system was designed to enable the central office in San Francisco to serve 50 patrons located in Oakland over 16 submarine cable conductors (six calling and ten answering trunks) across San Francisco Bay. All trunks were terminated in an existing teleprinter concentrator at the central office in San Francisco.

The Beverly Hills–Los Angeles system utilizes one of the 25 patron subcenter switching units previously described and shown in Figure 2. All trunks are terminated in an existing teleprinter concentrator at the central office in Los Angeles. This installation relieved an acute wire shortage caused by furnishing direct teleprinter tie-line service between those points to 15 patrons, and at the same time provided accommodations for extending the service to ten more such patrons.

The Newark–New York City system was the first of the larger systems designed and installed. Though the installation as now equipped accommodates only 150 patrons, the design provides for adding equipment to increase this number to 200 whenever the need for it develops.

The equipment of the subcenter, which is located in Newark, is housed in five racks as shown in Figure 5, and these racks are enclosed in an air-conditioned room for protecting the equipment from dust. The three similar racks shown in the foreground are the patron line units, the fourth is the calling trunk unit, and the fifth is the answering trunk unit. One patron line unit has a capacity of 50 patron tie-lines; one calling trunk unit, ten calling trunks; and one answering trunk unit, 36 answering trunks.

The system, at present, is served by 27 trunks, two of which are calling, five are combination, and 20 are answering. These trunks are terminated on individual tables in the central office in New York. Figure 6 is a view of that portion of the central office in New York containing most of those tables.

The first two installations, the Oakland–San Francisco and the Beverly Hills–Los Angeles, effected substantial savings in line wires and increased the capacity of the Western Union to furnish more of the same type of service between those points.

The Newark–New York installation greatly reduced the cost of operation by eliminating one relaying, or retransmission, of the patron business at Newark. In the past, the business was received in the Newark central office and most of it was retransmitted to one of the few large central offices connected to it, for further retransmission to its destination.

At present that part of the business destined for New York is handled directly, and the rest is retransmitted either to its destination or to a point much closer than was possible before, since the New York office is provided with direct connections to many more cities than was the Newark office.

The benefits anticipated from the use of subcenter switching systems have been fully realized in the installations made thus far.

Ease of operation, improved speed of service, saving of line wires, and in some instances, such as the Newark case, the reduction of operating costs, are the factors which make this development promising for future use in the telegraph industry.

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1. A PRINTING TELEGRAPH CONCENTRATOR, W. B. Blanton. AIEE TRANSACTIONS, volume 50, 1931, pages 414–25.

Aircraft Circuit Breakers

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FELLOW AIEE

Synopsis: Airplane circuit breakers are recognized as important units in the electrical systems of military aircraft. The design of such circuit breakers can be combined with switching and contactor functions. This makes possible the use of well-developed switch and contactor structures.

The design of such circuit breakers must provide for operation under the very exacting requirements of military aircraft service.

The principal function of both manual and remote-control circuit breakers is to provide short-circuit protection. There is some question as to the desirability of motor protection for military aircraft. Many motor functions are vital to the operation of the aircraft, and it may be better to operate the motor to destruction rather than to stop at a safe temperature or load value. Where motor overload protection is desired, it is necessary to correlate circuit-breaker and motor characteristics. Both manual and remote-control circuit breakers can be made to provide for holding the circuit closed in emergency to "force" the motor or other equipment.

ELECTRICAL operation and control have contributed largely to the effectiveness of today's military aircraft. It has been necessary to develop new types of electrical equipment to meet the special requirements of this service. Minimum size and weight are of extreme importance.[1] Control devices must operate in any position, must withstand high rates of acceleration and retardation, must operate in ambient temperatures of −50 to 200 degrees Fahrenheit, must operate at high altitude with rarefied atmosphere, and must withstand severe vibration.

Extensive electrical power systems are required for these military aircraft, particularly for the large bombers. Maximum reliability for the various circuits for these power systems is of unusual importance. Any part of the electrical power systems may be damaged in battle. As much of it as possible should remain available. It should be possible to restore power to any circuit as soon as a fault is cleared. Airplane-type circuit breakers are the logical answer to these problems.

Twenty-four volt d-c power with stand-by storage battery is the most extensively used power system for United States military aircraft. The lower voltage as compared to industrial power reduces the interrupting problem for circuit breakers but increases current values for a given power rating. Short-circuit faults in such a system may result in current of from 1,000 to 3,000 amperes. Such service presents serious contact problems.

These contact problems have been largely solved in the development of aircraft manual switches and contactors. One solution to the aircraft-circuit-breaker problem, therefore, is the addition of thermal trip units to these aircraft switches and contactor. Such an arrangement has a number of obvious advantages. The manual switches and contactors can be used for their normal service. The same mounting arrangements are available. Repairs and replacements are simplified. This type of design is a dual purpose one in which circuit or overload protection has been added to the normal switching or contactor function. Our discussion here will be limited to the features of circuit or overload protection.

The thermal trip units must be designed with special consideration for the service requirements of military aircraft. Their tripping values must not be appreciably affected by acceleration of 10g (where g is the acceleration of gravity) or more, or the effect of severe vibration. They must not change in rating in altitude from sea level to approximately 40,000 feet. These results can be accomplished by proper design.

Manual Circuit Breakers

The manual-type circuit breaker consisting of an airplane-type switch with bimetal thermal trip provides both circuit protection and switching functions. The X-ray pictures, Figures 1, 2, and 3, show the general construction of one such airplane circuit breaker. The switch mechanism provides make and break contacts operated by a toggle lever. This toggle lever may be provided with a luminous or fluorescent end. The trip unit is of the bimetal type. The bimetal latches a spring-actuated trip member. When the current through this bimetal trip unit exceeds a predetermined value for a sufficient length of time, the bimetal trips the latch and causes the circuit breaker to open its contacts. The contacts are reset by returning the toggle lever to the "on" position.

A thermal trip unit, in which the current flows through the bimetal, has a relatively sharp knee in the tripping-time curve. This is favorable to the protection of the circuit, as the wiring cable likewise has but little thermal capacity and reaches its maximum permissible temperature in a relatively short time. The relationship between the time re-

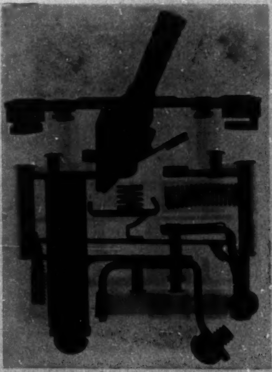

Figure 1. Manual circuit breaker
Contacts open, thermal trip unit latched

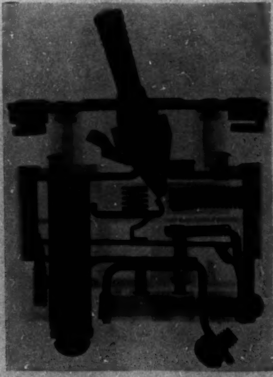

Figure 2. Manual circuit breaker
Contacts closed, thermal trip unit latched

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quired for the cable to reach a temperature of 90 degrees centigrade as compared to the tripping time of a circuit breaker with a tripping temperature of 150 degrees centigrade, is shown in Figure 4.

Short-circuit protection is the principal function of the manual circuit breaker. The amount of short-circuit current is determined by the circuit. High-capacity circuits will generally have higher short-circuit currents than low-capacity circuits. This is illustrated by the accompanying table which shows standard manual-circuit-breaker ratings and short-circuit test currents.

Table II of Army Navy Aeronautical Standard *AN-C-77*, February, 1943

Breaker Rating (Amperes)	Short-Circuit Load (Amperes)
5	1,000
10	1,000
15	1,500
20	1,500
25	2,000
35	2,500
50	3,000

The service requirement for aircraft circuit breakers differs in other particulars from those used in industry. Some of the applications make it desirable to "force" operation of the equipment even though the motors may seriously overheat. Specifications, therefore, require the nontrip-free type of circuit breaker. This is based on tripping of the circuit breaker to protect the circuit and equipment in case of faults, yet permitting the operator to "force" the motors, in case of emergency, by holding the circuit breaker in the closed position.

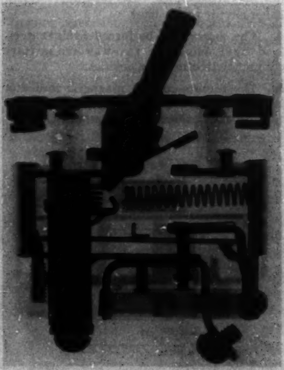

Figure 3. Thermal trip unit tripped, contacts open

Remote-Control Circuit Breakers

There are some applications, particularly on the larger military aircraft, where it is desirable to have the circuit breaker at a location remote from the point of control. Such installation reduces the amount of heavy wiring, thereby reducing the important element of weight. The reduced amount of heavy wiring further tends to increase reliability, because of damage from enemy fire.

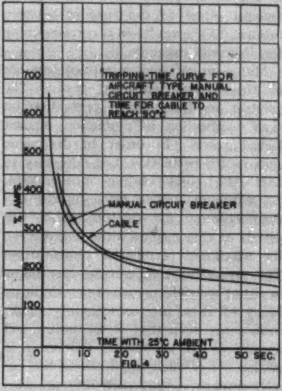

Figure 4

Circuit protection is the primary function of the remote-control circuit breaker. It is also possible to obtain motor protection, that is, to add the overload function to that of contactor and circuit breaker, making a triple-purpose device.

There is some question as to the desirability of motor protection for military aircraft. Many motor functions are vital to the operation of the aircraft, and it may be better to operate the motor to destruction rather than to stop at a safe temperature or load value. The following discussion, however, pertains to those applications where motor protection may be desired.

A remote-control circuit breaker consisting of an airplane-type contactor with thermal trip unit is shown in Figure 5. This combination can be obtained in the various standard sizes of contactor as used for airplane service, that is, 50-ampere, 100-ampere, and 200-ampere ratings. These airplane contactors provide the high interrupting capacity (3,000 amperes) and freedom from welding of contacts as required for circuit-breaker service.

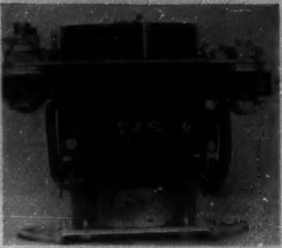

Figure 5. Remote-control circuit breaker

The thermal trip unit for these remote-control circuit breakers is shown in the X-ray photographs of Figures 6 and 7. This provides quick make and break pilot-duty contacts operated by a bimetal trip member. The contacts are insulated from the bimetal trip member by a small bakelite operating pin. The bimetal carries all or part of the main circuit current. The different ratings are obtained by the use of shunt elements which are an integral welded unit with the bimetal.

It is well established that the tripping temperature of an overload device should approximate the danger temperature of the equipment to be protected. For military aircraft it is desirable to operate to maximum capacity to avoid unnecessary weight. Class *A* insulation will permit of operation at temperatures of 125 degrees centigrade. This is recognized in the Underwriters' Laboratories standard on industrial control for built-in cycling overload motor protection.

The heating of any motor is, in general, proportional to the losses in the motor. It is, therefore, possible to determine an approximate heating curve for a motor if

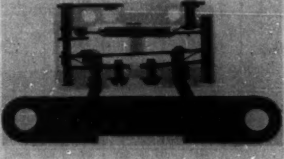

Figure 6. Remote-control circuit-breaker thermal trip unit

Contacts closed

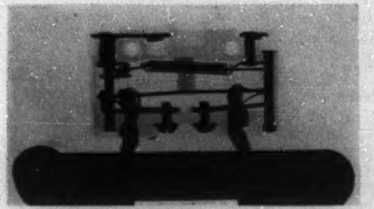

Figure 7. Remote-control circuit-breaker thermal trip unit

Contacts open

we have the efficiency curve and know the temperature rise at some particular loading. Such a study based on performance curves for a two-horsepower aircraft-type motor is shown in Figure 8.

From the heating curve for the motor we can readily determine a load curve for a range of ambient temperatures. This is based on the assumption that the final temperature of the motor will equal the sum of the ambient temperature and temperature rise for a given load. The proper trip temperature, for the thermal trip unit, is readily determined from this curve. Figure 9 shows motor load and thermal unit tripping-current curves. The motor load curve is based on a maximum winding temperature of 125 degrees centigrade and the curve for the thermal trip unit is based on trip temperature of 120 degrees centigrade.

The use of glass-insulated motor windings will of course permit of higher motor temperatures. Such motors require higher tripping temperature for overload protection. This is reflected to some extent in *AN-C-77* specifications which indicate an approximate tripping temperature of 155 degrees centigrade.

For motor protection it is necessary to provide a large number of ratings. These ratings should be spaced not more than ten per cent. If we protect the motor for a predetermined maximum temperature, it will be necessary to overprotect an amount equal to the spacing between ratings. This results in a corresponding loss in motor capacity.

Many aircraft motor applications are of the intermittent-duty type. The greatest need for motor protection on such application is protection against locked rotor. In order to determine such motor protection it is necessary to have the corresponding motor data. These include locked rotor current and the maximum time which the motor will permit of this current without exceeding the maximum permissible temperature. This maximum temperature may occur in either the field or armature windings. Many of these intermittent-duty applications are such that if the motor will operate at all, no further protection is needed. For such applications the remote-control circuit breaker provides motor protection equal to that which could be obtained with built-in overload protection. The remote-control circuit breaker provides quick reset for such applications, whereas built-in overload protection depends upon cooling of the motor and has a much longer reset time.

Circuit-breaker standards specify ratings which will cause tripping at 25 degrees centigrade from 115 per cent to 138 per cent of the established rating. This is primarily intended for circuit protection and to insure that rated load will not cause tripping. This method of rating must be understood when making applications of remote-control circuit breakers for motor protection. These ratings, however, can be readily translated into the usual overload ratings which provide that the overload unit trip under all conditions in a 40 degrees centigrade ambient. The recognized standard of accuracy for overload protection is a tolerance of +0 to −10 per cent.

Remote-control circuit breakers for many military aircraft applications should be arranged for "forcing" the motor in emergencies. It is also desirable to indicate tripping of a remote-control circuit breaker at the control point. These functions can be obtained by control circuits as shown on Figure 10. This circuit requires but a single control wire with grounded return. The use of the two-way momentary contact pilot switch provides six operating functions.

1. The contactor is closed by moving the pilot switch to the close position and is maintained through a control resistance which is mounted as a part of the remote-control circuit breaker.

2. The contacts are opened by moving the pilot switch to the open position.

3. The thermal trip unit contacts reclose automatically after tripping on overload, and operation to the close position re-establishes circuit to the load.

4. The motor can be forced against overload by holding the two-way momentary contact switch in close position.

5. The remote-control magnetic indicator is operated through the single control wire to show the position of the contactor.

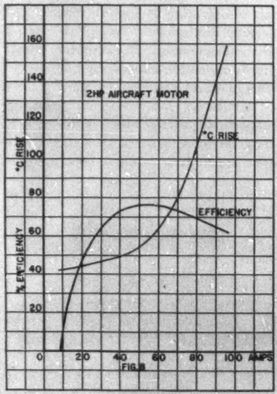

Figure 8

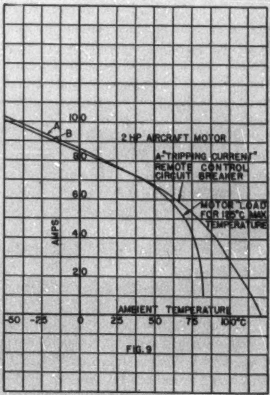

Figure 9

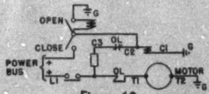

Figure 10

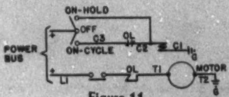

Figure 11

Relay Protection of Tapped Transmission Lines

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ASSOCIATE AIEE

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MEMBER AIEE

Synopsis: The war, with its attendant shortage of critical materials, has led to a considerable increase in the number of tapped lines. This paper deals with the method of protecting circuits of this kind under the three headings:

1. Pilot-wire protection, a-c and d-c schemes.
2. Carrier-current protection.
3. Other protective schemes.
Directional overcurrent or network.
Distance type.

Three-terminal lines are classified into three types with regard to other system connections between the terminals connected by the line in question. These are further classified as to power sources to aid in analyzing limiting conditions for relay applications.

Typical operating characteristics of a-c pilot-wire equipment for three-terminal lines are shown.

In general in dealing with pilot-wire, carrier, and other types of protection, those problems peculiar to application on three-terminal lines are analyzed and methods given for solving some of the more troublesome problems.

Introduction

THE interconnection of several power supply and load points by a single tapped transmission line rather than by a number of two-terminal lines has always presented first cost advantages at some expense in reliability and flexibility. However, the relay protection of such lines has been somewhat of a nightmare to protection engineers. Increased operating times for complete clearing of faults, and even some loss of selectivity have had to be tolerated in many instances. It has been said that there are no good three-terminal lines; some are simply worse than others. While inaccurate, this thought does express the general sentiment of protection engineers toward such layouts.

Nevertheless, the war, with its attendant shortage of critical materials, and manpower for construction, has led to a considerable increase in the number of tapped lines and placed increased load requirements on others already in service. Many war plants, because of their locations and high power requirements have been provided with supply with a minimum use of critical materials by tapping the nearest high-voltage line or lines. The circuit breakers usually have been located on the low-voltage side of the transformer.

The present paper deals with the methods of protection for such lines under the headings of:

1. Directional overcurrent protection.
2. Impedance or distance-type protection.
3. Pilot-wire and carrier-pilot protection

It is hoped that by summarizing the existing schemes and presenting data on certain new schemes, particularly on pilot-wire protection, that some help can be given in the solution of this currently important problem. It is also hoped that this paper will elicit discussion which will bring to light other schemes which various protection engineers have devised to solve the problems here discussed. Such discussions will be of particular benefit to the industry at the present time.

TYPICAL TAPPED LINES

Tapped lines appear in all the categories of main transmission, subtransmission, and distribution. Two hydrogenerating stations may be connected together and to a load and steam generation area over a high-voltage three-terminal line of the main transmission class. The substations of an a-c electrified railway[2] are fed from dual 132-kv lines with as many as ten substation transformers tapped from each line. The trolley system constitutes a 12-kv network. In the subtransmission class the four-kv network[1] ("high-voltage network") is a typical example of multitapped lines, the transformers which feed the network being tapped from lines such as 13.8 kv. Industrial plants will be found tapped at intermediate points of lines of practically all voltage classes. In many cases these plants have generating facilities in order to provide process steam. Hence, they must be relayed for line faults. Numerous other examples could, of course, be cited.

CLASSIFICATION OF THREE-TERMINAL LINES

The simplest three-terminal line is the straight radial arrangement with one

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6. The contactor will maintain its last operating position, should the control wire be broken or shot away.

A second control scheme, Figure 11, provides operation of the remote-control contactor by means of a three-position pilot switch. The contactor can be held closed against motor overload if the pilot switch is in the "on-hold" position. The overload trip unit contacts reclose automatically, and, if the pilot switch be in the "on-cycle" position, the motor will restart automatically, after tripping on overload. If the control wire is shot away or broken, the contactor will open or remain open as the case may be.

Conclusions

Military aircraft have extensive electrical systems. Circuit breakers are an essential part of such an electrical system. Their use insures that as much of this system as possible will remain available when the aircraft is subjected to enemy fire. These circuit breakers can combine switch and contactor functions with that of circuit protection, thereby reducing weight.

Military aircraft service often requires the immediate and maximum operation of motors so that motor overload protection cannot be provided. For those applications, where motor-overload protection is desired, circuit-breaker characteristics must be properly related to motor characteristics. Some such applications are of very intermittent duty and stalled motor conditions is the principle consideration for motor protection. Both manual and remote-control circuit breakers can be of the nontrip-free type to permit the operator to "force" the motor or other circuit devices beyond conditions which cause tripping.

Reference

1. APPLICATION OF ELECTRIC POWER IN AIRCRAFT, T. B. Holliday. ELECTRICAL ENGINEERING, volume 60, May 1941, pages 218-25.

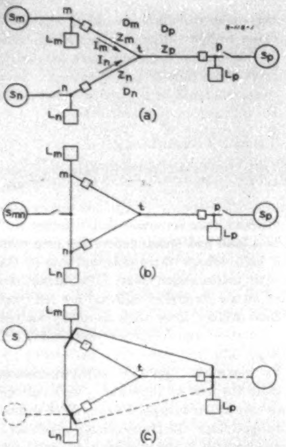


Figure 1. Classification of three-terminal lines

(a). No paralleling ties; one, two or three sources

(b). One paralleling tie; sources on connected terminal, or independent terminal, or both

(c). Paralleling ties between all terminals

S—Power source
L—Load
Z—Impedance

power source and two loads, fed solely over the line in question. As the number of sources is increased or other ties are included between the busses connected by the three-terminal line, additional relaying problems are introduced. Thus, from a relay-protection point of view, three-terminal lines may be classified into seven types as follows:

A. No paralleling ties, Figure 1a.
1. One power source.
2. Two power sources.
3. Three power sources.
B. Paralleling tie between two of the terminals, Figure 1b.
4. Power source on the connected terminals only.
5. Power source on the independent terminal only.
6. Power sources on both.
C. Paralleling ties between all three terminals, Figure 1c (with or without dotted portions).
7. The power source is effective at all terminals.

The classification as regards ground current is, in general, different from that for phase currents. For example, Figure 2 illustrates a three-terminal line with paralleling connections between all terminals for phase currents. However, ground faults on the tapped line cause no ground current in the other two lines. Thus, there are no paralleling connections for ground currents. Also, the two grounded transformers are the only sources of ground current for the tapped line. Thus, the relaying for ground faults is little, if any, different from a two-terminal line. This particular three-terminal line falls in the seventh classification for phase relaying but in the second class for ground relaying. High-voltage bussing throughout would throw it into the seventh classification for ground relaying also.

The Problems of Tapped Lines

The greater difficulty of relay co-ordination with tapped lines is due to a variety of problems that do not arise with the simpler two-terminal lines. For example, the impedance from one terminal to the two others may be different, introducing a problem in setting distance-type relays. Also, an impedance measurement at one terminal for fault near a second is affected by current entering at the tap point from the third terminal, or by load current flowing to the third terminal. These two problems may occur on any of the seven classifications of three-terminal lines.

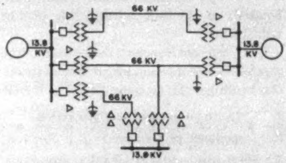


Figure 2. System showing three-terminal line with parallel ties for phase currents and only two sources with no parallel lines for ground currents

In addition, on the latter four classes, which involve paralleling ties, it is possible for fault power to flow out of one terminal for a fault on the line near another terminal. This frequently prevents the use of a straight blocking pilot scheme on lines of this class.

These problems and their solutions are dealt with in the subsequent paragraphs. Quite satisfactory solutions are possible by the use of pilot-wire[3] and carrier-pilot[4] relaying where these are feasible. Frequently the directional overcurrent or distance schemes depend for good operation on the strict maintenance of a normal system setup, or else sacrifice speed to use settings that will co-ordinate under adverse system conditions. The high-voltage network is an exception as will be explained.

Directional Overcurrent Protection

Three-terminal lines in radial circuits can be protected with standard overcurrent time-delay relays, as indicated in Figure 3. When additional sources of power are connected to the circuits, directional elements must be added to the relays. This circuit is indicated by the dotted construction on Figure 3. As the amount of power that must be transmitted over a given circuit is increased, the problem of maintaining system stability during faults becomes more critical and may impose limitations on the amount of time that is allowable for clearing faults. This usually eliminates the use of timed overcurrent relays, except in special cases where instantaneous overcurrent elements will provide the required speed of operation.

The high-voltage network[1] is an ideal example of a multiterminal-line arrangement that can be satisfactorily relayed Each of the many taps is tripped by power reversal to the supply line, the system being so arranged that this can occur only during a fault on the supply line. This requires that all lines feeding a given network emanate from the same bus so that for normal load conditions the supply-line voltages are nearly equal and little or no reverse power flows through any transformer bank.

Many modifications of this basic scheme have been used in which some power reversal can take place when tripping is not desired. If such a reversed flow is due to a fault beyond the next breaker, time is introduced[1] for selection. If it is due to load currents, fault detectors have been successfully used, and, if the reversal can be due to either faults or loads,[1] fault detectors have been used to prevent operation on load currents and timing to provide selectivity for proper fault operation.

Usually such systems are arranged for

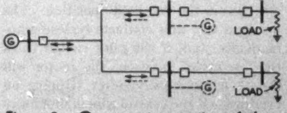


Figure 3. Overcurrent protection of three-terminal line

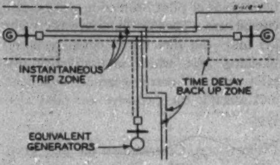


Figure 4. Impedance protection of three-terminal line

only one or two sources of ground current, the taps being tripped by residual voltage.

On long taps with overcurrent protection considerable improvement in selectivity can be secured through the use of angle discrimination. On such lines fault current occurs at a phase angle which could not occur for large load currents or swings.

INDUSTRIAL INTERCONNECTION

When a line is tapped to an industrial plant having generation, it is common practice to segregate essential loads for operation from the plant generator and dump others in event of a line outage. If the same line is tapped for other plants, the problem arises of separating the plant under consideration from the line under conditions hazardous to its operation. One scheme in successful use on many industrial interconnections consists of separation based on any of three indications, provided power flow has reversed and is toward the power company. The three indications are: underfrequency, undervoltage, or generator overload. Any of these occurrences, provided power flow is away from the plant, is taken as sufficient cause for separating and at the same time dumping nonessential loads so that the remaining plant load may be brought within the capacity of the plant generation.

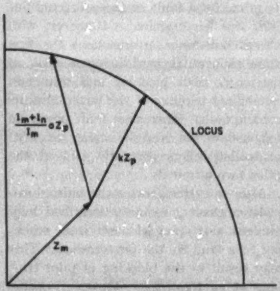


Figure 5. Graphical solution for balance point

$Z_m+\frac{I_m+I_n}{I_m}aZ_p=Z_m+kZ_p$

Given Z_m, Z_p, I_m, I_n (vectors) and either a or k (scalars) the other can be found by drawing the circular locus of impedance viewed by the relay

Refer to Figure 1a for nomenclature

Relay at m. Normal balance point is at Z_m+kZ_p from m

Balance point with mutual effect is at Z_m+aZ_p from m

The relays normally employed are:

Induction-type overcurrent for generator overload.

Induction-type underfrequency relay.

Induction-type undervoltage relay.

High-speed-type three-phase directional relay.

The overload relay is directional controlled so that it will not start timing unless direction has reversed.

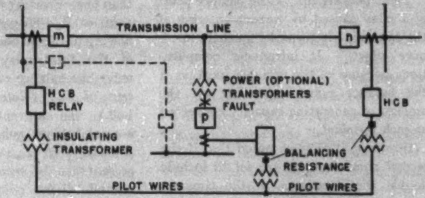


Figure 6. Typical three-terminal power circuit protected by three *HCB* relays

Directional relays are also used, without the voltage, frequency, or current fault detectors, for this purpose.

Impedance or Distance-Type Protection

The general principle of application of distance relays to a three-terminal line is illustrated in Figure 4. As indicated, the instantaneous elements are set to include a zone that has a radius of approximately 90 per cent of the impedance to the nearest station. Faults beyond this zone will be tripped instantaneously by the near relay and, after a time-delay period has expired, at the remote terminals. However, certain problems arise which are not present in the application to two-terminal lines.

UNEQUAL IMPEDANCES TO REMOTE TERMINALS

Even in the simplest case of one power source and no paralleling ties, the three-terminal line presents a problem not present with two-terminal line protection. For example, with a source at *m* only, in Figure 1a, it is desirable to set the first zone element of an impedance relay at *m* for a large part of the line impedance to the next station. If *p* is much further away than *n*, the first zone instantaneous element can be set to trip only to 90 per cent of the distance to *n*. This leaves a considerable section of the line to *p* without instantaneous protection at *m*.

Also, the second zone impedance element at *m* must be set to trip beyond *p* in order to be sure of tripping end zone faults up to *p*. However, with this setting it may reach so far beyond *n* as to encompass another complete line section, whereas for proper co-ordination it should cut off somewhere in the first zone protective region of that next line section.

A similar problem exists with the third zone element which is set where possible to back up completely the subsequent line section.

These problems occur also in the other cases where more sources and paralleling ties are present. It is evident that considerable advantage can be secured from a pilot channel to prevent the high-speed impedance elements from overreaching either of the other terminals, as will be described subsequently.

MUTUAL IMPEDANCE EFFECT

While the impedance relay operates on the ratio of current to voltage it is convenient to hold one quantity fixed and visualize the operation as the other quantity varies. Suppose a constant fault current is considered through the relay at *m* and the fault is gradually moved from the relay toward *p*. At first there is no voltage restraint, and the relay operates. As the fault is moved further away, the voltage restraint on the relay increases directly with the distance or impedance, until a so-called "balance point" is reached beyond which the voltage restraint exceeds the current operating force. Suppose that with a single power source this normal balance point is at 90 per cent of the distance to *p* and takes in 80 per cent of the section from *t* to *p*.

If a second source is introduced at n and the fault is again moved from m toward p, the restraint again increases directly until t is passed. Then, if the source at n provides four times as much current at the first source, the restraint increases five times as fast for distances of fault beyond t, since the current producing the voltage drop is five times as great in this section. The balance point is therefore reached at 1/5th of 80 per cent or 16 per cent of the distance from t to p. Or, in general, if the normal balance point is at an impedance, Z_m+kZ_p, from the relay, with a source at m only, and no paralleling ties, the balance point moves to an impedance

$$Z_m+\frac{I_m}{I_m+I_n}kZ_p$$

from the relay when the source or paralleling tie is added.

This "backing-up" of the balance point of a relay caused by current entering at the tap point is termed "mutual-impedance effect." It introduces complications because of the variable location of the balance point depending on the amount of generating capacity connected at each terminal.

However, it may be noted that if the second zone elements are set to include double the impedance of the common branch, tripping will always be assured although it may be sequential. Any current distribution that more than doubles the common branch as viewed from one terminal m, will less than double it as viewed from the other terminal n. The terminal with the larger current will therefore trip, eliminating the mutual effect, whereupon the other terminal will trip.

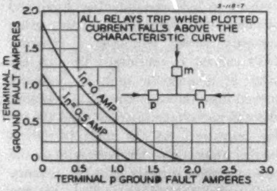


Figure 7. Typical tripping characteristic of *HCB* relays on a three-terminal line for internal ground faults

Relay set for maximum sensitivity. Pilot-wire resistance 200 ohms (each branch)

Negligible capacity between wires

Mutual Effect of Loads

Even with a single source and no paralleling ties, load currents will have some effect on the balance point of impedance relays, particularly when fault and load currents are of the same order of magnitude. In Figure 1a with a fault at p, current to the load L_n, causes mutual drop in the branch m. Since this load current flows through the relay but does not create proportional restraining voltage by flowing over the line clear to the fault, it causes an overreaching effect which must be considered particularly in the setting of a first zone element. In terms of a normal balance point at a distance, or impedance, Z_m+kZ_p, the addition of the load L_n moves the balance point to an impedance

$$Z_m+\frac{I_m}{I_m+I_n}kZ_p$$

from the relay. This expression is identical with that given previously which makes the fraction $I_m/(I_m+I_n)$ greater than one. Starting with a normal balance point at Z_m+80 per cent of Z_p, a load flowing to L_n and constituting 40 per cent of the total relay current I_m, would move the balance point to Z_m+133 per cent of Z_p. (Note: 1/1-0.4 x 0.8=1.33.) To prevent overtripping, the normal balance point must be reduced. A balance at Z_m+aZ_p with the load present can be secured by making the normal balance point:

$$k=\frac{I_m+I_n}{I_m}a \tag{1}$$

Then, if $a=0.8$, $k=1-0.4/1\times0.8=0.48$. A normal balance point at Z_m+48 per cent of Z_p will be moved to Z_m+80 per cent of Z_p when the 40 per cent load is present.

Phase-Angle Effects

In the foregoing discussion the balance-point expression has been treated as algebraic which is, of course, strictly true only if the impedance phase angles are alike and the currents I_m and I_n either in phase or 180° out of phase. More generally the balance point occurs at Z_m+aZ_p where a can be determined by solving the equation

$$\left|Z_m+\frac{I_m+I_n}{I_m}aZ_p\right|=|Z_m+kZ_p| \tag{2}$$

A graphical solution is shown in Figure 5.

Pilot-Wire and Carrier Pilot Protection

Pilot protection using either wire or carrier channels has proved to be quite an effective solution to the problems of relaying three-terminal lines. Two common types of pilot protection will be discussed here. They are known respectively as, first, the directional comparison or blocking type which is used with d-c pilot-wire or carrier-current channels, and second, the two-wire a-c pilot-wire type.

Directional Comparison or Blocking Type

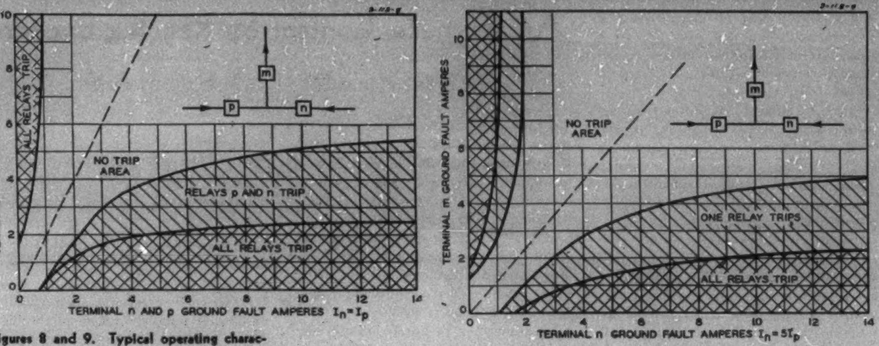
In the blocking scheme, directional fault-detector relays at each terminal trip for any faults in the tripping direction unless blocked by a signal from the far end indicating the fault to be beyond the far end of the line. This signal is usually initiated by a directional element which operates when power of fault magnitude flows out of the line at that terminal. Fault power flowing out at one terminal of a three-terminal line, to an external fault, will correctly block tripping at the other two terminals.

Such a blocking scheme is used with either wire or carrier pilot. One arrangement utilizes the step-type directional impedance relay as the fault-detecting element, and as the directional element for controlling the blocking signal, corresponding ground-current elements being used. The backup characteristics of step-distance relaying are obtained in this scheme without added impedance elements.

As previously explained, on systems with paralleling ties, as in Figure 1b or c, fault power may flow out at one terminal for a fault on the protected line near another terminal. However, with the step-distance arrangement the first zone element is used independently of carrier or pilot blocking and results in immediate tripping of the terminal nearest the fault. Thereafter fault power no longer flows out, and the carrier- or pilot-controlled relays promptly trip at the other two terminals.

Also, in extreme cases the mutual impedance effect, previously described, may prevent a tripping element from reaching to a fault at the far terminal. This may result in the blocking of pilot tripping at all terminals. However, operation of the first zone instantaneous element will in many cases open the breaker nearest the fault and remove the mutual effect. Pilot-controlled relays can then trip normally at the other terminals.

To summarize, distance-type pilot-wire or carrier protective systems can be applied to any type of three-terminal power circuit. Their operation may be slightly delayed when power flows out of one terminal of the faulted line or when mutual impedance effect prevents a



Figures 8 and 9. Typical operating characteristics of *HCB* relays on a three-terminal line

Instantaneous direction of ground-fault currents indicated by arrows, all currents in phase

Relay set for maximum sensitivity (0.5 ampere ground current per terminal)

Pilot-wire resistance 200 ohms (each branch)

Negligible capacitance between wires

tripping fault detector from reaching a fault near a far terminal.

Two Wire A-C Pilot-Wire Type

The *HCB* single-element a-c pilot-wire scheme,[8] developed in 1937, which has been widely used on short two-terminal lines, is also applicable to short three-terminal lines. In fact, such applications have been in successful operation in United States and Canada since 1939. The success with these lines and the greatly increased number of such applications at the present time has stimulated a considerable study of the limits of application of this type of equipment to determine how far it can be extended. Operating characteristics have been determined as the current distribution is varied, or the length of the tapped lines increased with corresponding increase in pilot-wire resistance and capacity. Some of the significant results of this analysis are presented here.

Principle of Operation

The *HCB* scheme of pilot-wire protection operates essentially as an extended bus-protection scheme, tripping all terminals whenever the totalized current to a fault within the protected zone exceeds a predetermined setting. It has variable ratio characteristics, produced by the restraining coils and a saturation device, that makes it relatively insensitive to ratio errors of current transformers on heavy through faults. The ratio characteristics are the same in percentage for any type of fault, phase, or ground; however, the relay has separate adjustments for phase and ground currents, so that the actual magnitude of ground current required to trip can be, and usually is, set considerably lower than the phase-current trip.

It is evident from the foregoing discussion that this scheme differs from the blocking types in that flow out at one terminal will not necessarily block tripping if the total net flow is "in" to a fault on the line. It is, therefore, ideal in principle, producing immediate tripping of all terminals for internal faults.

As the number of terminals is increased above two, and as the length of the pilot wire is increased, departures from the ideal of "extended bus protection" already described begin to be observed. For example, the minimum trip value increases with the number of terminals, and some variation is noted between the fault current required to trip different terminals, depending on the current distribution and the pilot-wire resistance and capacitance.

In the *HCB* pilot-wire scheme, restraint current is circulated through the pilot wires for through-fault or normal load conditions. With three terminals the three pilot wires are all brought to a common point and paralleled, their resistances being equalized up to this common point by means of balancing resistances as shown in Figure 6. For a through fault or load current flowing in at two terminals and out the third, restraining pilot-wire currents flow from the two terminals to the pilot-wire junction where they combine to form the correct pilot current to the third terminal. For faults within the protected zone the relay voltages act in opposition over the pilot wire, reducing the restraining currents and building up voltages which pass large currents through the shunt operating coils.

The operating characteristics of the *HCB* pilot-wire-relay scheme are illustrated by the charts of Figures 7, 8, and 9. If each relay is given a ground-current setting of 0.5 ampere, all relays trip whenever the total current to a fault in the protected zone totals approximately 1.5 amperes, or 0.5 ampere per terminal. The minor variations from this approximation are shown for moderate pilot-wire lengths in Figure 7. With equal distribution 0.5 ampere per terminal trips, whereas 1.9 amperes in terminal *p* alone is required to trip all three relays, the difference being due to the greater restraint current under the latter condition. This difference is usually of small significance.

The characteristic curves are given only for ground current, but it should be understood that the same curves apply for phase currents if the current scales are multiplied by a constant, that is, by the ratio of phase setting to ground setting given the particular relays.

The curves of Figures 8 and 9 are used first of all to show the amount of current-transformer inaccuracy that would be required to cause misoperation on a through fault. All current entering *p* and *n* must leave at *m* for a through fault. The currents *p* and *n* are considered equal and plotted as abscissa in Figure 8, while the current out at *n* is plotted as the ordinate. The curves separate trip from nontrip areas. Thus, two amperes in each of *n* and *p* (abscissa 2), and four amperes out at *m*, falls on the dashed line well within the no-trip area. In fact even should the ratio error of the current transformers at *m* be so great that only two amperes was delivered, the relay would still not quite trip. However, dropping to 1.5 amperes would cause undesired tripping at *p* and *n*. The burden is moderate (1.6 ohms in residual circuit) so that current-transformer errors beyond those permissible according to Figure 8 would not be expected.

It was mentioned that the relay tripped on the basis of net current flow into the protected zone and hence operated even with some flow out at one terminal. There is obviously a limit to how far this can be carried, since the greater the departure from equal feed in at all three terminals the greater the restraint current flowing and the higher the operating value. With sufficiently high current out at one terminal, tripping will be prevented. Figure 8 is also intended for determining whether correct tripping will be obtained during an internal fault with maximum flow out at one terminal. It shows for example that if two amperes (secondary) flow in at each of *p* and *n* terminals, as much as 1.0 ampere leaving at *m* will not block tripping.

Curves similar to Figure 8 have been prepared for other than equal values of currents at *p* and *n*. Figure 9 shows the corresponding characteristic with the current at *n* equal to five times that at *p*.

It has been found from experience with a number of applications that the flow out is not sufficient to block desired tripping, as determined from data such as Figures 8 and 9. It can be concluded, therefore, that in practical cases, the *HCB* relay as used on three-terminal lines provides instantaneous tripping for all internal faults on three-terminal lines. Also it provides adequate margin to prevent operation on through faults even with quite large current-transformer inaccuracies. As mentioned, a number of installations have proved this to be a reliable means of obtaining high-speed (one cycle) relay protection on three-terminal lines. Standard equipment is used so that existing circuits frequently can be expanded simply by adding a third relay.

The pilot wires used should preferably be enclosed in cable sheaths, protected from lightning, induction, and differences in station ground potentials so as to maintain a continuous circuit at all times.[5,6] On three-terminal lines the shunt capacity between pilot wires should not exceed 0.5 microfarad per terminal for 500-ohm circuits. Continuous d-c supervision can be supplied with the relay to ring an alarm whenever any branch of the circuit is opened or faulted.

Conclusions

The co-ordination of directional overcurrent and distance-type relays is more difficult in three-terminal lines because of two principal problems.

1. Unequal impedances to remote terminals.
2. Mutual impedance effects from fault and load currents entering or leaving at the tap.

Blocking-type pilot schemes assist in solving these problems, but on three-terminal lines with paralleling ties undesired blocking may be produced by:

3. Power flow out at one terminal to a fault within the protected zone near another terminal.

The first zone element of step-distance-type pilot schemes solves this problem by operation independent of the carrier-current or pilot-wire blocking, but at the expense of a slight time delay.

The *HCB* two-wire a-c pilot-wire scheme circumvents all of these problems and provides uniform high-speed protection even in the more difficult cases of three-terminal line protection. Typical operating characteristics for three-terminal line operation are presented in the paper.

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Arc-Furnace Control by Regulex Exciters

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FOR a number of years prior to the present urgency for the production of high-quality steel, the development in control methods for electric-arc furnaces remained substantially stationary in this country. These devices were in the general form of vibrating regulators or contact-making ammeters which rapidly connected the electrode motors to and disconnected them from a constant d-c voltage source. In Europe, meanwhile, various methods employing Ward-Leonard or hydraulic control means were developed, some of which were reported to result in smoother operation under the highly fluctuating transient conditions of the arc.

Some six months prior to Pearl Harbor, study indicated that with stepless regulating or control means available in this country improved results might be effected. Such means consists of a rotating regulator in the form of a Regulex exciter. The first of this type control, here discussed, has now been in operation long enough for a definite judgment of operating results.

Qualities Required in a Regulator

1. Actuation from a measure of arc or heating energy.
2. Correction increasing in value as deviation from normal increases and decreasing to zero as deviation decreases to zero.
3. Quick response.
4. Stable antihunt means to prevent overshooting.

These requirements, of course, need to be met in any successful closed control system for governing or regulating. However, in the arc furnace the regulating problem is unusual because of the high degree of instability of the electric current and power in the arc.

By referring to A and A^1, Figure 7, film 2, it will be seen that fluctuations occur about every six cycles[1] near the beginning of a melt in a steel furnace. This is due in part to rapidity of changes in ionization, varying degree of rectification, and so forth. Obviously, it would be impractical to move an electrode system weighing perhaps a ton, in a six-ton furnace, fast enough to correct for these variations by control of arc length. Cur-

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Figure 1. Six-ton top-charge arc furnace with operator's panel at right

rent is limited in such transients to a safe maximum value by transformer and lead reactance and where necessary an external reactance, as $X2$ in the equivalent circuit of Figure 3. But, under the same conditions, the variations in average values, from maximum to minimum as from A to B, Figure 7, film 2, occurs every one to two seconds. This is due in part to metal melting away, electrode melting, and so forth, which changes the arc length.

Thus a good regulating system must hold the average value to minimum variation. The Regulex exciter was selected for these requirements.

Rotating Regulators

Amplification

In a d-c generator rotating at constant speed, such as Figure 2A, the output power is controlled by the excitation winding CF. The machine is recognized as a power amplifier, and the control power in winding CF is a small percentage of the power output. This percentage is two per cent to five per cent in a machine of standard commercial design giving an amplification factor of 50 to 20, respectively. This ratio is dependent on frame size and design, and in general the larger the frame size the greater the amplification factor for a given output voltage because the larger the frame the more flux per watt excitation is set up. Furthermore, by connecting two such machines in tandem as in Figure 2B the ratio of power output to control power is the product of the amplification factors of the individual machines. Therefore, with a given control power available, in the form of a measure of the quantity to be controlled, a wide variety of amplification factors are available to the engineer to obtain the necessary power output. The power output then is used to react on the quantity to be controlled whenever it varies from normal.

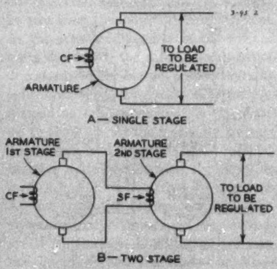


Figure 2. Regulex exciters

For example, in Figure 3, the control power available is current-transformer five-ampere metering and arc potential. Field 4 is designed for current-transformer metering current, and field 5 is designed for corresponding volt-ampere and ampere-turn capacity. The Regulex exciter 3 is the first stage of the amplifier and is of sufficient size to excite the electrode generator 2, which is the second stage. The second stage then is made of suitable capacity to drive electrode motor 1. This motor acts to change arc length when arc current and voltage vary from normal. Obviously for small electrode motors only one generator need be used.

Stability

The next most important consideration is stability of the system to prevent overtravel and "hunting." In the Regulex arc-furnace control, the actuating medium in the system is the resultant excitation of two opposed fields, 4 and 5, Figure 3, in the Regulex exciter. The control power is the sum of the excitation watts of these two fields. These fields are equal in ampere turns at normal value of arc watts or heating energy (the aforementioned quality 1 required in a regulator). No actuating flux being present, under this condition, no voltage is applied to the electrode motor.

Curve B, Figure 4, gives a typical volt-ampere arc characteristic. The change in voltage is not linear with change in current but is sufficiently large to satisfy quality 2. For small deviations from normal the actuating flux is essentially proportional to the deviation of arc watts from normal and the resultant power output to produce correction is essentially proportional to this deviation. This produces an inherently stable system where mechanical and electrical inertias can be neglected, thus signifying quality 4. Where these factors are appreciable, an anticipation of "cutoff" of the actuating flux proportional to such inertias is required. The amplification together with design for low inductance in all circuits satisfies quality 3 as will be later shown.

Sensitivity

The sensitivity of the system depends on amplification ratio, friction in the electrode system, and hysteresis in the electrical machines. As will be seen from the volt-ampere characteristic, curve B, Figure 4, at very low currents the change in arc volts is greater per unit change in current than at higher current values. For this reason good sensitivity is maintained inherently down to very low currents. The system is quite stable at 10 to 15 per cent current which is necessary to prepare furnaces for operation after relining (burning bottom).

Arc-Furnace Control

An arc-furnace installation is subject to greater fluctuation in power and surges[2] than other installations requiring control because of:

(a). More frequent switching.

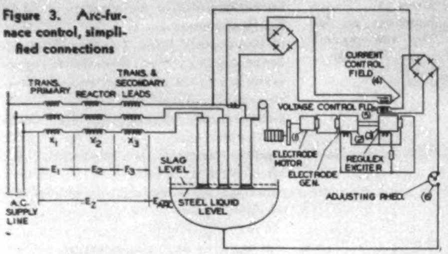
(b). Wide range in primary transformer taps to provide necessary secondary voltage.

(c). Large reactance to limit maximum current.

(d). Varying amount of d-c component in the arc.

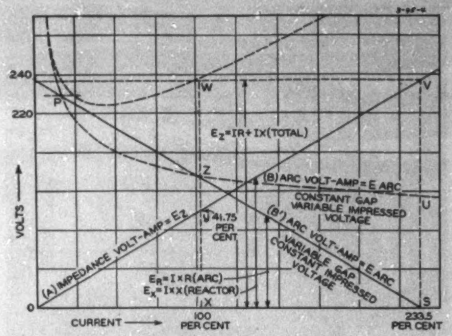
(e). Rapidly fluctuating state of ionization in the arc.

Figure 3. Arc-furnace control, simplified connections


TRANS. PRIMARY
REACTOR
TRANS. & SECONDARY LEADS
X_1
X_2
X_3
E_1
E_2
E_3
E_Z
E_{ARC}
A.C. SUPPLY LINE
SLAG LEVEL
STEEL LIQUID LEVEL
CURRENT CONTROL FIELD (4)
VOLTAGE CONTROL FLD. (5)
(1)
(2)
(3)
ELECTRODE MOTOR
ELECTRODE GEN.
REGULEX EXCITER
ADJUSTING RHEO.
(6)

The last item *e* has greatest effect from the control standpoint.

Figure 4 (right). Typical arc-circuit characteristics



The complete system, here under consideration, consists for each electrode, of a Regulex exciter controlling a generator of suitable size with its armature connected electrically directly to the armature of an electrode motor which raises and lowers the electrode. In addition, there are the necessary control switches, meters, indicating lights, and so forth. This equipment is duplicated for each electrode. For a three-phase furnace three Regulex exciters, three genrators, and an a-c driving motor form a small seven-machine motor generator set. The complete connections are shown in Figure 5. The motor generator set, as shown by the connections, may be supplied from the furnace bus, thus making the entire installation self-contained, and not dependent on a separate d-c power source. Figure 6 shows the control equipment from the rear of the operator's panel. The panel at the left mounts, plate-type rectifiers, control resistors, and so forth. On small electrode motors, generally under one horsepower, the Regulex exciter armatures are connected directly to the electrode motors, thus resulting in a small four-machine control unit.

As shown in Figure 5, current settings in each phase are made by rheostats *R*1, *R*2, and *R*3 in the arc-potential circuit. By changing, for example, the excitation of *VF-A*, the phase current is automatically changed, since the differential flux between *VF-A* and opposed current field *CF-A* produces voltage to run the electrode in the proper direction (raise) until the fields are equalized.

Four voltage positions normally are provided on the transformer tap changer. On the highest voltage all of resistance *R1A*, *R2A*, and *R3A* in the potential circuit are connected in the circuit. When the circuit is changed to the next lower

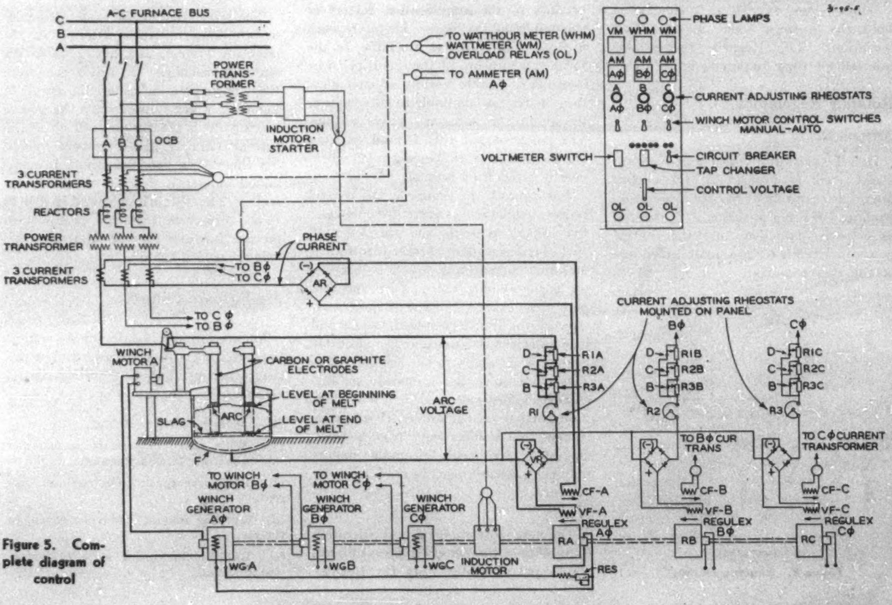

A-C FURNACE BUS
C
B
A
POWER TRANSFORMER
INDUCTION MOTOR STARTER
TO WATTHOUR METER (WHM)
WATTMETER (WM)
OVERLOAD RELAYS (OL)
TO AMMETER (AM) AΦ
OCB
3 CURRENT TRANSFORMERS
REACTORS
POWER TRANSFORMER
3 CURRENT TRANSFORMERS
TO BΦ
TO CΦ
PHASE CURRENT
AR
TO C Φ
TO B Φ
WINCH MOTOR A
CARBON OR GRAPHITE ELECTRODES
LEVEL AT BEGINNING OF MELT
LEVEL AT END OF MELT
SLAG
ARC
F
ARC VOLTAGE
R1A
R2A
R3A
R1
VR
CF-A
VF-A
TO WINCH MOTOR BΦ
TO WINCH MOTOR CΦ
WINCH GENERATOR AΦ
WINCH GENERATOR BΦ
WINCH GENERATOR CΦ
WGA
WGB
WGC
INDUCTION MOTOR
RA
REGULEX AΦ
RES
CURRENT ADJUSTING RHEOSTATS MOUNTED ON PANEL
BΦ
CΦ
R1B
R2B
R3B
R2
R1C
R2C
R3C
R3
TO BΦ CUR TRANS
TO CΦ CURRENT TRANSFORMER
CF-B
VF-B
REGULEX BΦ
RB
CF-C
VF-C
REGULEX CΦ
RC
PHASE LAMPS
VM
WHM
WM
AM
AΦ
BΦ
CΦ
CURRENT ADJUSTING RHEOSTATS
WINCH MOTOR CONTROL SWITCHES MANUAL-AUTO
VOLTMETER SWITCH
CIRCUIT BREAKER
TAP CHANGER
CONTROL VOLTAGE
OL

Figure 5. Complete diagram of control

voltage, contact *B* automatically short-circuits resistor *R3A* so that on the new voltage the current setting is not changed. Contacts of *B* also perform the same function on the other phases, and for other voltages contacts *C* and *D* act in like manner.

Operation of the Control in Service

A good image of the net operation of the control can be obtained from examination of oscillograms taken with a furnace in operation. The films of Figures 7 and 8 were taken on a steel furnace of six tons nominal charge capacity. Arc energy is supplied through a furnace transformer from a 22,000-volt three-phase 60-cycle line. The analysis of the melt was

Carbon	0.35
Manganese	0.14
Silicon	0.43
Phosphorus	0.035
Sulphur	0.037

In all films, which were taken on a single phase, the oscillogram traces starting at the top of the film and progressing downward measured the following:

1. Arc volts.
2. Electrode (or winch) motor, armature volts.
3. Arc amperes.
4. Voltage-control-field amperes (rectified).
5. Current-control-field amperes (rectified).
6. Electrode (or winch) motor armature amperes.

All values are rms except rectified Regulex-control-field currents which are peak values.

In film 2, Figure 7, taken 15 minutes after the start of the melt, just prior to sudden current increase, at *A*, the electrode motor was operating at 75 volts and lowering the electrode at 10.85 inches per minute. With the sudden current increase the motor was reduced to zero speed in 26 cycles. However, it was not decelerated at a constant rate, as can be seen from the motor armature current. As the average difference in current and volts decreased, the rate of deceleration decreased. The motor reached practically zero speed 26 cycles later and remained so for approximately 35 cycles up to point *C*. At this point the average arc energy had dropped below normal, and the motor accelerated for 35 cycles until it had 90 volts across its armature. At this point the average arc energy became essentially normal, and the electrode speed remained at 14.35 inches per minute lowering, and the motor armature current ran to approximately zero.

As will be shown later, between points *E* and *B*, mutual inductance between the two opposed control fields prevents response to the peak values of only a cycle or so duration. However, following point *B*, the current rose sharply from about 5,000 amperes to 18,400 amperes, and the motor was decelerated to zero speed in about 10 to 11 cycles and reversed. Here a high "pump-back" current is regenerated in the electrode-motor armature.

Consider the action at point *F*. Prior to reaching this point because of too low arc energy, the electrode motor had been accelerated to lower the electrode until 106 volts were across its armature. The increase in current at this point, with little decrease in voltage starts decelerating the motor. However, as previously mentioned, at this point in the heat, metal is running away from the electrode as it melts, thus tending to increase arc length inherently and to reduce current. At the same time the electrode is lowering. Consequently, the rate of lowering the electrode decreases only until the average *actuating measure* of arc watts is reduced to normal which is not necessarily *zero speed of the electrode.* In examining the electrode-motor armature volts for the entire length of film 2, the electrode is raised four times, with only slight movement each time. The total area under all of these raising voltage swings below zero, compared to the total area under the lowering loops above zero, shows that the electrode is lowered through the scrap at such a rate as to maintain as near constant average heating value as the response of the system to the extreme fluctuations will allow.

Film 4, the lower one in Figure 7, was taken 65 minutes after the start of the melt of film 2. Much of the steel has been melted down, and conditions are steadier. The film speed is such as to show the general wave shape of current and voltage. At point *H* on the film, it will be noted that current jumped from 5,240 amperes to 13,100 amperes on successive peaks. However, the rectified-voltage control-current peak increased on this half cycle, because of mutual inductance, even though the voltage was sharply reduced. But the average of the next four half cycles is reduced from the previous four half cycles with a net small response from the Regulex unit. This characteristic is of particular value in refining at the end of the melt to prevent false movement when the metal is "boiling." When a bubble rises against the electrode and disappears within a cycle or two, after bursting, no movement of electrode results.

Film 8 at top of Figure 8, was taken one hour and 55 minutes after the start of the heat of films 2 and 4 and consequently during the refining period. Iron ore and

Figure 6. Rear view of operator's panel showing auxiliary panel and Regulex exciter motor generator set

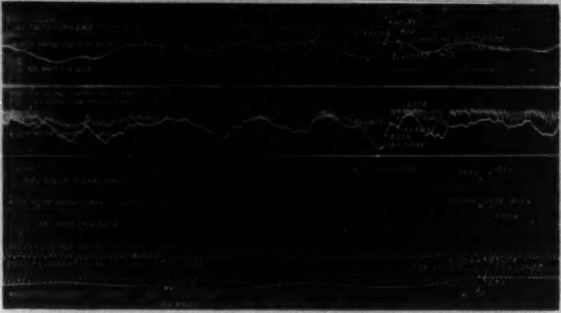


Figure 7 (above). Film 2 and film 4

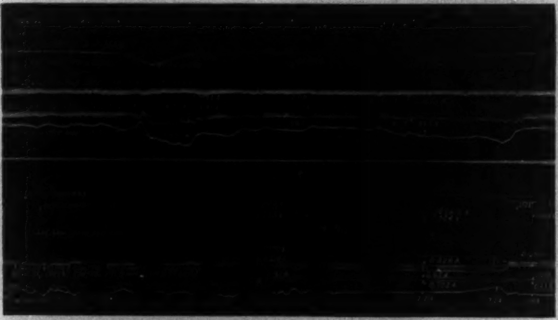
Film 2 taken 15 minutes after start of melt. Transformer on *A* tap, 205 volts phase to phase, 14.3 per cent impedance. Time 10:15 p.m. Film speed approximately $1\frac{1}{4}$ inches per second. Heat 21071. Saturday, November 21, 1942. Charge 2,000 pounds turnings, 9,600 pounds gates, 6,400 pounds plates (nine tons total)

Film 4, same heat, taken 65 minutes after start of melt. Time 11:05 p.m. Transformer on *A* tap, film speed approximately 23 inches per minute

Figure 8 (below). Film 8 and film 11

Film 8, same heat taken one hour 55 minutes after start of melt, 11:55 p.m. Transformer tap C, 127 volts phase to phase, 9.4 per cent reactance. Film speed approximately $1\frac{1}{4}$ inches per minute

Film 11, heat 21075. Sunday, November 22, time 3:10 p.m. (furnace shut down approximately 7:00 a.m. Sunday morning, restarted 3:10 p.m.). Start of melt. Transformer tap *A*, 205 volts phase to phase, 14.3 per cent reactance. Film speed approximately $1\frac{1}{4}$ inches per second. Same scrap and analysis as on film 2



other elements had been added shortly before and created violent boiling sufficient to necessitate a cutoff of power to prevent boiling over. The film shows regulation just after reapplication of power after boiling had subsided somewhat.

Film 11 was taken at the beginning of a heat next day. The control was purposely set so that when it was placed under automatic regulation to lower electrodes onto loose scrap, one electrode would strike the scrap in advance of the one being recorded. At point *I* to the right of the film when the electrode touched the scrap placing full-phase voltage between electrodes, the electrode motor speed was increased correspondingly. At points, *J*, *K*, *L* when the electrode first came in contact with the loose scrap, the arc was "struck" and broken some eight times before being firmly established. Thereafter, it was not lost for the duration of the heat except for interruptions by the operator for tap changing, and so forth.

It is to be noted particularly how electrode motor speed was reduced as the arc was established, until at the extreme left of the film it has reached a speed comparable to the lowering speeds of film 2. Also of interest is the low magnitude of arc current when the arc is initially established.

Power Consumption

Power consumption is, of course, dependent on many variables including type of material melted, method of feeding material into the furnace, use made of the furnace such as melting or refining, and so forth, so that kilowatt hours per ton vary from one installation to another. The control is only one element in any comparison. However, on furnaces of the same type and rating, in the same plant, evidence of more uniform resultant heating has been reflected in more heats per day as compared to older methods of control. Figures are not given because the results are unexpectedly favorable and need verification in other installations to be put into service, where comparisons with previous equipment can be made.

Effects on Power System

Previous papers[3] before the Institute have shown that maximum disturbance on the power system occurs when the arc is made or broken. From film 11, Figure 8, and the other films, it may be seen that once the arc has been established firmly, at the beginning of the melt, it is not broken again before the end of the melt, except for interruptions for voltage change, and so forth.

Also, it has been found that a lesser reactance may be used than with older methods, thus improving power factor to some extent with less net voltage disturbance. Comparative data are not available on this general comment.

Conclusions

1. A stepless direct acting regulator is readily adaptable to highly fluctuating electrical quantities.
2. Application of such control to arc furnace has improved efficiency and reduced power consumption and maintenance.

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Aircraft Electric Power-Supply System

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Synopsis: An ever increasing demand for electric power in aircraft has been felt for some time. The necessity of using an electric storage battery started the practice of accompanying it with a d-c generator, first of low current and voltage capacity, later of 50-ampere 12-volt rating, and for the past few years of 200-ampere 24-volt output. In the largest four-engine aircraft, even four generators of maximum capacity do not furnish ample electric energy. Experiments have proved so far that generation of a-c power and application of lightweight transformers, together with its conversion to direct current by means of selenium rectifiers, is entirely a reliable and practical way of attaining large, trouble-free electric plant capacity in aircraft. The subsequent pages and illustrations portray the current progress made with lightweight power transformers and rectifiers in the a-c-d-c aircraft systems. They also represent at least a partial answer to the questions raised by Lieutenant Colonel Holliday[1] when he discussed problems of applications of electric power in aircraft two years ago.

THE development of a-c–d-c power systems for aircraft application started with a view of achieving two main objectives:

1. Augmenting the source of electric energy offered by conventional d-c generators.
2. Alleviating the difficulties of commutating machinery and voltage limitation under conditions of high-altitude operations.

Furthermore, having alternating current as a primary source of electric energy, some of the a-c powered equipment in the aircraft can readily be used with or without resorting to transformers, whereas d-c operated devices may be powered through rectifiers with or without transformers.

The first stage of early development was to duplicate the largest d-c generator of 200-ampere rating, operating the so-called 24-volt system, with the output voltage set at 28.5 volts in order to maintain the proper charging rate of the battery.[2] The weight, efficiency, and, to some extent, the bulk of the generator were to be at least duplicated or, if at all possible, were to be met more favorably with a combination of alternator and rectifier.

The paramount requirements of the aircraft power rectifier narrowed down to the following three:

1. High efficiency.
2. Minimum weight.
3. Satisfactory service.

An efficiency of 84 per cent appeared to be feasible and acceptable. A unit of six-kw rating, with suitable enclosure and connecting terminals, would receive favorable consideration if its weight could be in the neighborhood of 15 pounds. The larger-sized units are to be a multiple of this one. Satisfactory service could only be expected if the performance of the rectifier is:

1. Fairly uniform under extreme temperature, humidity, and altitude conditions.
2. Unaffected by vibration.
3. Capable of maintaining good operating characteristics for at least 500 hours if not longer and with a reasonably small amount of air-blast cooling.

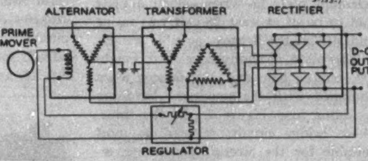

Figure 1. Block diagram of aircraft electric plant of 800-ampere 30-volt d-c rating

The transformer, on the other hand, from the very start presented one important problem, that is, the attainment of the lightest possible weight. The air-blast cooling was set out to be a definite means of meeting this requirement. As it happened, however, the latest kind of material and the unique design produced very gratifying results.

The requirement, design, and performance factors of the alternator constitute an ample subject for their own discussion. It is sufficient to state here that even the early alternator, powering a 200-ampere 30-volt rectifier, was 26 pounds in weight and 90 per cent efficient. Several selenium rectifiers were designed, built, and tested under low and high temperatures, rarefied air, and excessive vibration conditions. Their performance was in every respect satisfactory and, therefore, promising.

In order to evaluate fully the soundness of higher power generations,[3] plans were drawn to build an aircraft electric system capable of delivering a d-c output of 800 amperes, 30 volts, under varying temperature and humidity conditions and with a reasonably small amount of air-blast cooling. Such a system, Figure 1, comprising alternator, transformer, and rectifier, was built and tested.

Rectifier

Even during early experiments with a 200-ampere rectifier, it became evident

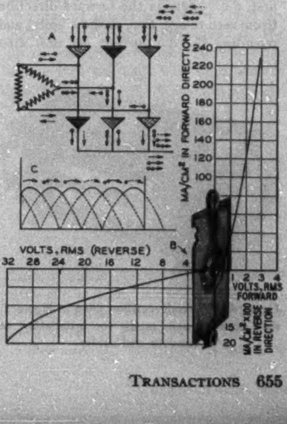

Figure 2 (right). High-voltage selenium rectifier plate

A. Diagram of three-phase bridge circuit and distribution of current in its six arms

B. Operating characteristic, forward and reverse voltages in rms values, and the currents in arithmetical values, as they function in three-phase bridge circuit. Cut shows four layers of rectifying element: back electrode, selenium layer, barrier layer, and front electrode

C. The net result of performance of the six arms of the bridge circuit during each cycle

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The author wishes to express thanks to the officers of the United States Army Air Forces, engineers of the Chrysler Corporation, and to the colleagues of his own organization for their co-operation in carrying on these experiments.

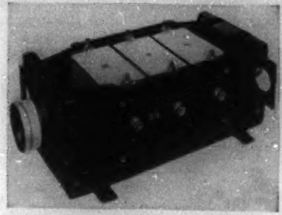
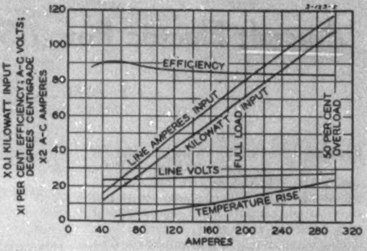


Figure 3. Aircraft rectifier, three-phase input, 200-ampere 30-volt d-c capacity

Top and side covers removed. Weight 15 pounds. Rectifying elements occupy less than 0.25 cubic foot and weigh 11 pounds

Figure 5. Operating characteristics of rectifier unit shown in Figures 3 and 4

Rated at 200 amperes, 30 volts, at ambient temperatures from minus 70 to plus 50 degrees centigrade. Unit is capable of withstanding 50 per cent current overload continuously for ten hours



that if selenium rectifiers were to play a role in 24-volt aircraft systems, the design and technique of the manufacture of selenium plates had to be modified at least to the extent of using a single selenium plate in series. Such a plate had to be of at least 28-volt rms rating and still of the lightest possible weight.

This plate, as finally developed, weighs $1^1/_4$ ounces bare and $1^3/_4$ ounces when equipped with the necessary hardware including fuse, Figure 2. In the three-phase bridge circuit, selected as most suitable for the aircraft power conversion, this selenium plate accounts for 12.5 amperes of the entire current output at 30 volts d-c with 350 cubic feet of air per minute. It is fitting here to once more reiterate the function of this type of rectifying element. The phenomenon of electric current rectification wholly takes place in the barrier layer which, in this particular plate, has been strengthened in the reverse direction but with the minimum adverse change in the forward direction. The static characteristic of a metal rectifier is determined by applying, first, d-c voltage in the forward direction (from zero to one, two, or three volts) and recording the d-c current per unit area (milliamperes per square centimeter); and second, by applying d-c voltage from zero to maximum plate voltage in the reverse direction and measuring the current leakage in milliamperes per unit area.

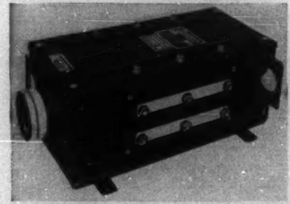


Figure 4. Aircraft rectifier, same as Figure 3, side covers open, ready for installation of conduits and cables

There is a total of six arms in the rectifier of the three-phase bridge circuit, but only two arms function during each one sixth of a cycle. The remaining four arms are blocking, Figure 2*A*. Since the total d-c output is greater than 12.5 amperes, several plates are placed in parallel. The rectifier of 200-ampere 30-volt rating has a total connection of 6–1–16, and the figure 16 designates the number of parallel plates in each arm of the bridge circuit. Physically all 96 plates are assembled into three stacks, interconnected with suitable bus structure and are housed in a lightweight case designed and built in accordance with usual aircraft practice, Figures 3 and 4.

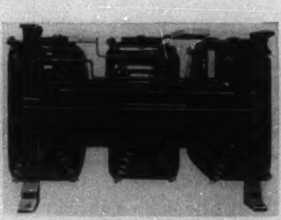


Figure 6. Aircraft transformer, air-cooled, 33.7-kva 400-cycle three-phase, 250-volt primary winding, 28-volt secondary winding

Weight 30 pounds. Front view. Over-all length ten inches

The upper section of Figure 2*B* illustrates the function of the selenium plate in the passing or forward direction. There is a certain amount of voltage drop per plate. This drop varies with the current density or the total output of the rectifier and constitutes forward losses. In a rectifier giving an output of 200 amperes, its value is in the neighborhood of 2.3 to 2.7 volts. This drop, however, changes with the time of service of the rectifier. Its increase during 500 hours of continuous operation may bring a net result of five per cent drop in the d-c output with the same a-c input to the rectifier. For some time, it had been expected that an idle condition of the rectifier would give a marked increase in this voltage drop. To date, however, observations have shown that this is not the case, although some momentary deforming of selenium plates may be expected after long idleness.

With the two arms of the bridge circuit passing the current in forward direction, the remaining four arms are blocking. An ideal rectifier would block reverse current completely. Actually, however, there is a certain amount of current leakage. Its value depends on the operating temperature to some extent but mainly on the length of service of the rectifier itself. The lower curve of Figure 2*B* illustrates a typical leakage current characteristic at an ambient temperature from 10 to 40 degrees centigrade. At both ends of this temperature range, the leakage current increases. Under all temperature conditions, however, the currrent leakage decreases with the life of the rectifier. The curve represents a typical reverse voltage-current density

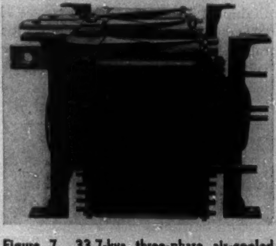


Figure 7. 33.7-kva three-phase air-cooled transformer, same as Figure 6

Side view. Over-all width seven inches

relation observed with two plates opposing each other. In the actual three-phase rectifier circuit, the reverse current is somewhat higher than that shown in Figure 2*B*, since the effects of all plates are integrated over the entire cycle. Computations and subsequent tests showed that the total current leakage of a 200-ampere rectifier is approximately 2.4 amperes at the start of service and is in the neighborhood of 1.6 amperes after 500 hours of continuous operation.

The necessary a-c input voltage to give the required direct current output voltage is computed by means of already established formulas.[4–6]

$$V_{ac} = 0.74 \times 30 + 2 \times 1 \times 2.7 = 27.6 \text{ volts}$$

The performance of this rectifier is illustrated in Figure 5.

Transformer

In the design of the transformer, full advantage was taken of improved materials and latest methods. Following the general aircraft practice of obtaining minimum weight and highest efficiency, high-permeability low-loss silicon steel was selected for the core structure.[7,8] Since the transformer was to operate under conditions of high magnetic induction, this type of steel, insulated with a silicate glass and processed under a rigidly controlled technique of heat treating and crystal orientation, was to give maximum amount of flux, some 40 per cent more than the best transformer steel, with a remarkably small external magnetizing force. Furthermore, this material permits not only lower total losses, including copper losses, but also their better balance, greater short-time overload capacity, lower impedance, and better voltage regulation. The latter measured 12 per cent and with the flux density of 16,150 lines per square centimeter and resulting core losses of 320 watts at 350 cycles per second, the efficiency of the 33.7-kva transformer was 95 per cent at full load. As the frequency increases, the core losses decrease. Even with the frequency 25 per cent higher, when the flux density was in the neighborhood of 12,800 lines per square centimeter, the core losses decreased almost 40 per cent. At these high frequencies, the machined butt joints also added to the high performance of the transformer.

Both windings were designed with a view to keeping copper losses to the minimum. This has been accomplished with the use of a rectangular wire for the primary windings and with the use of very thin and wide flat copper strips for the secondary windings. In this manner, the radiating area of the copper was increased markedly, and, since there is a certain amount of skin effect in the conductor under the conditions of high frequency and high current, this feature helped greatly in keeping copper losses down. With the current density of 6,600 amperes per square inch in the primary winding conductor and some eight per cent less in that of the secondary, the copper losses at 75 degrees centigrade were only 620 watts and 610 watts for primary and secondary windings, respectively.

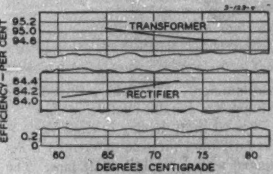


Figure 9. Characteristics illustrating the effect of temperature upon efficiencies of transformer and rectifier

All other material employed in this transformer was also selected with the soie objective of obtaining optimum performance. The winding, for instance, is separated from the core by means of heat-resistant glass cloth-base laminated plastic. The ventilating ducts in the winding are formed by means of sticks made from the glass cloth-base plastic. The frame itself is made of four corner supports welded from pieces of high-strength aluminum alloy. The supports are clamped to the core with a steel tape in such a manner that vibration stresses are taken by the supports rather than by the entire transformer structure. This type of frame construction contributed greatly in achieving lightness of weight, Figures 6 and 7.

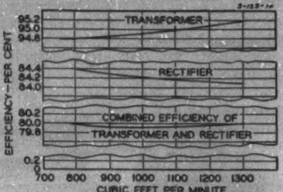


Figure 10. Transformer and rectifier efficiency characteristics as affected by the amount of cooling air

The secondary winding, carrying approximately 375 amperes, is spirally wound and has a-c resistance much lower than the conventional type of winding, The actual amount of 650 watts of copper losses in such a winding at 1,000 cycles per second frequency and 150 degrees Fahrenheit operating temperature would be some six times as great if the same winding were made of a square wire, of the identical cross-section area and arranged in a single layer. Furthermore, the copper-foil winding facilitates cooling, since the thin and wide strip has a surface area of almost six square inches per inch of conductor length, whereas, the square wire conductor has a surface of one square inch per each inch of conductor length.

Alternator

During the past year, several alternators were designed, built, and tested. Two voltage ratings (250 and 28 volts, line) were tried. The alternator used in Figure 1 experiment was of 35-kva 250-volt rating. Two more alternators became available. One is 16 kva to operate with the rectifier delivering 400-ampere 30-volt output; another of still lower kilovolt-ampere rating, Figure 11.

This aircraft alternator is rated at

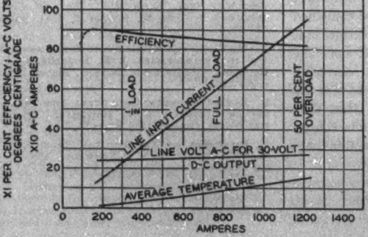


Figure 8. Performance characteristics of 800-ampere 30-volt rectifier used in Figure 1 system

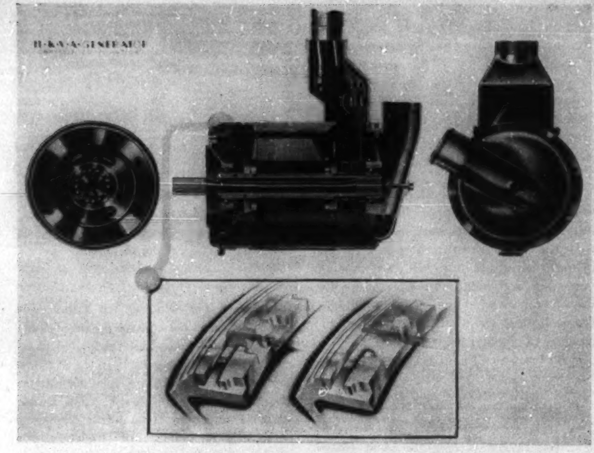


Figure 11. 11-kva three-phase air-cooled alternator, weight 33 pounds

Top, from left to right:
1. Engine pad view of the alternator
2. Section through alternator
3. Cooling cap and terminal block

Bottom, enlarged details of bayonet-type mounting flange. On left, alternator initially placed on flange. On right, alternator in locked position

11 kva, three phase, 30 volts, 211 amperes, 440 cycles and is capable of delivering 10.45 kw at 95 per cent power factor and at the minimum speed of 4,400 revolutions per minute. It was designed for a field current which could be handled by the 28-volt 200-ampere voltage regulator normally available; therefore, it does not represent the lightest possible design which could be achieved by a heavier field current (12 amperes) and a special regulator. Its approximate weight is 33 pounds.

The stator is delta-connected with two parallel circuits per phase. There are 72 semienclosed slots with two slots per pole per phase and full pitch winding. Rectangular wire is used in the stator winding to obtain maximum ratio of copper to slot area. Paper insulation is used since the alternator temperature rise is kept within class *A* insulation limits. The 12-pole rotor is skewed. Metallic slot wedges are used for the rotational speeds encountered. The field coil ends are strapped to metallic reinforcing rings.

Forced draft cooling is employed. A six-inch total pressure differential is required with a two-inch blast pipe. The internal air passages are so arranged that reverse flow of cooling air is caused, and thus more efficient use is made of the available air.[9]

800-Ampere 30-Volt Electric System

The first unit (Figure 1) with rectifier having operating characteristics shown in Figure 8 and supplying a total output of 24.6 kw, 0.6 kw of which was used by the field circuit of the alternator, has been tested under ordinary laboratory conditions and has given an over-all efficiency of 72 per cent. The efficiencies of the transformer and rectifier are affected by the amount of cooling air and, therefore, by the operating temperature of the respective units, Figures 9 and 10. The amount of cooling air was varied from 750 cubic feet per minute to 1,300 cubic feet per minute. Over this range of volume of cooling air, the combined efficiency of the transformer rectifier unit remained practically constant, since the efficiency of the transformer decreases and that of the rectifier increases with the temperature rise. It follows then that the minimum volume of air can be chosen to maintain both components within their safe temperature limits.

High-Altitude Operations

The high-altitude operations constitute unusual conditions of rarefied air and extreme low temperature. The 200-ampere rectifier unit without transformer has been tested in a stratosphere chamber under 45,000 feet altitude and minus 43 degrees centigrade temperature conditions.

The results could readily be analyzed in the light of the same characteristics shown in Figures 9 and 10. Referring to Figure 9, it can be seen that rectifier efficiency decreases steadily as the temperature goes down. At the same time, however, efficiency increases with the reduction of the cooling air as a result of the higher internal temperature of the rectifier. The net result is that both effects tend to balance each other and to maintain the efficiency of the rectifier at a point governed by percentage of loading.

Conclusion

It is gratifying to comment here that at present a large amount of work is being carried on by capable engineers and manufacturers connected with or serving the aviation industry. The results no doubt will bring about the availability of several flexible electric systems from which to choose one meeting particular requirements.[9]

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Formulas for Calculating Short-Circuit Forces Between Conductors of Structural Shape*

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THE mechanical forces acting on bus-bar supports during short circuit are determined both by the magnitudes of the mutual electromagnetic forces exerted among the bus conductors and by the elastic properties of the supporting structure: its motional resistance, natural frequencies, and physical nature and arrangement. Comprehensive mathematical studies,** substantiated by experimental data, of the individual and collective effects of these factors and general methods for calculating the magnitudes involved have been advanced by Schurig and Sayre,[1] Dahlgren,[2,3] and Pilcher.[4] In shorter articles based on the work of the first-named Schurig, Fricke and Sayre,[5] Tanberg,[6] Specht,[7] Edson,[8] and others[9] have presented certain charts, nonograms, and short cuts that facilitate the numerical labor incident to actual calculation.

Now though the circumstances of a particular problem may render certain of these methods more advantageous to use than the others, *all require calculation of the mutual electromagnetic forces exerted among the conductors of the bus.* Commonly the bus is comprised of a group of long linear parallel nonmagnetic cylindrical conductors. If so, calculation of the electromagnetic force acting on a specified conductor resolves into successive determination of the magnitudes and phase relationships of the currents in the conductors, of the mutual force exerted between the given conductor and each of the others, and of appropriate vector summation of these last.

Calculation of the actual short-circuit currents requires use of symmetrical components. General procedures specifically adapted to short-circuit calculation of industrial-plant distribution systems, together with inclusive pertinent numerical data (electrical constants of standard types of conductor, of typical busses, and of the usual attached electrical apparatus), are encompassed in elaborate works by Hanna[10] and by Darling.[11,12] Illustratively, Schulze[13] has given a detailed numerical analysis of the short-circuit calculation of a typical plant distribution system. Other phases of the problem of calculation of the short-circuit currents have been discussed by Papst,[14] Schurig,[15] Leonard and Riker,[16] Gross,[17] Robinson,[18] Tripp,[19] and Kilian.[20,21]

However, between finding the short-circuit currents (for which step are available all the methods and data contained in the papers mentioned in the preceding paragraph) and summing the forces exerted on the specified conductor by the other conductors (which step for a d-c bus requires but simple arithmetic and for an a-c bus the only slightly less simple task of adding complex numbers) is the intermediate and important task of *finding these individual forces.* Theoretically, if the conductor currents and geometry are known, these forces can be calculated by use of fundamental electromagnetic theory. Practically, the mathematical analysis requisite to use of this theory is far from simple—if not actually intractable—and the bus designer turns, perforce, to the use of known formulas or, if these latter be lacking, to some type of approximation, the accuracy of which is more or less indeterminate.

Obviously, resort to such approximation, certainly inconsistent with good engineering practice, is most undesirable. Yet there is little choice to the contrary. For, though the mechanical and electrical advantages stemming from the use of linear conductors of structural shape are so marked that, at present, most busses for heavy-current duty are constructed of such conductor; and, though standard designs of single and polyphase busses utilize strap, rectangular, tubular, channel, angle, tee, and I-beam conductor, a recent exhaustive survey[22] of the literature reveals, seemingly, that explicit formulas for calculating the electromagnetic forces between two conductors of the shapes mentioned are virtually nonexistent. A paper by Dwight[23] contains formulas and curves for calculating the force between two identical conductors having parallel coplanar axes and parallel-sided rectangular cross sections. And papers by Higgins[24,25] contain formulas for calculating the force between two identical conductors having parallel coplanar axes and parallel-sided full or hollow rectangular cross sections. But, despite extended and increasing use, there have not been published explicit formulas for calculating the force between conductors (not necessarily identical) of parallel-sided full or hollow rectangular conductors arranged other than just described; or for any arrangement of conductors of angle, channel, tee, or *I*-beam shape; or for any arrangement of composite conductors constructed of combinations of two or more of these six shapes.

Such formulas are derived in this paper, it being assumed that the conductors are nonmagnetic, are of such length that end effects are negligible, are right cornered, and carry current distributed uniformly over their cross sections.

Of these four assumptions, the first and second are commonly satisfied in practice. The third and fourth assumptions, though not always true in practice (for example, structural shapes built up of strap conductor are usually right cornered, whereas structural shapes drawn or rolled in one piece usually have slightly

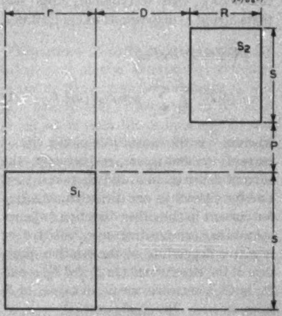

Figure 1

Paper 43-106, recommended by the AIEE committee on basic sciences for presentation at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943. Manuscript submitted April 26, 1943; made available for printing May 27, 1943.

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* As the formulas pertinent to conductors of full or annular circular cross section are well known, they are not treated in this paper.

** While the work of the first named is frequently cited in the American literature, that of the other two, seemingly, has attracted but little attention. Yet Pilcher's work is an extension of the theory delineated by Schurig and Sayre; while Dahlgren's, an extended study of the important phenomenon of mechanical resonance, complements both.

rounded corners; again, skin and proximity effects are always present on a-c busses, though, as Beetz[26] has discussed at length, at power frequencies and normal bus spacings the effect of these phenomena on the magnitudes of the electromagnetic forces is vanishingly small) introduce error negligible with reference to the accuracy required in calculating short-circuit forces and enable solution of an otherwise intractable problem.

If these four postulates are granted, derivation of the desired formulas is not particularly difficult. First, through application of fundamental electromagnetic theory specific formulas are obtained for the components of the electromagnetic force exerted between two conductors, the cross sections of which are *arbitrarily located* parallel-sided rectangular areas. Then these formulas and the same electromagnetic principles are conjoined to obtain general formulas for the components of the force exerted between two conductors, the cross sections of which are arbitrary configurations comprised of parallel-sided rectangular areas (section I). Next, the theory essential to calculation of the derivatives of the geometric mean distances occurring in these formulas is developed (section II). Finally, as the general formulas of section I are applicable to an inclusive category that embraces conductors of structural shape, explicit formulas for the components of the force exerted between conductors of specified structural shape can be deduced from these general formulas as desired (section III).

I. General Formulas for the Components of the Electromagnetic Force

Invoking well-known theory, the inductance L of a circuit comprised of two conductors with rectangular cross sections S_1 and S_2 disposed as in Figure 1 is

$$L = 2\log(D_{12}^2/D_{11}D_{22})$$

$$= -(2/I^2)\sum_{i=1}^{2}\sum_{j=1}^{2} w_i w_j S_i S_j \log D_{ij} \qquad (1)$$

wherein I is the magnitude of the circuit current; w_i and w_j are, respectively, the current densities in S_i and S_j, taken positive for current in one direction, negative for current in the other direction (whence w_i^2 and w_j^2 are positive, w_iw_j positive or negative depending on the relative direction of the two currents in S_i and S_j); and D_{ij} is the geometric mean distance of S_i from S_j.

The energy in the magnetic field associated with the circuit is $W = LI^2/2$; whence equation 1 yields

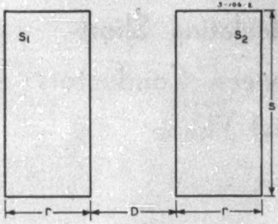


Figure 2

$$W = -\sum_{i=1}^{2}\sum_{j=1}^{2} w_i w_j S_i S_j \log D_{ij} \qquad (2)$$

As is well known, the rectangular components of the electromagnetic force exerted between the two conductors are given by

$$f_x \equiv f_D = \partial W/\partial D;\ f_y \equiv f_P = \partial W/\partial P \qquad (3)$$

Substituting from equation 2 in equation 3 as indicated yields

$$f_D = -\sum_{i=1}^{2}\sum_{j=1}^{2} w_i w_j S_i S_j \partial(\log D_{ij})/\partial D \qquad (4)$$

$$f_P = -\sum_{i=1}^{2}\sum_{j=1}^{2} w_i w_j S_i S_j \partial(\log D_{ij})/\partial P \qquad (5)$$

If each of the two conductors is comprised of several subconductors connected in parallel, one comprised of m subconductors numbered from 1 to m and the other of n subconductors numbered from $m+1$ to $m+n$, equation 23 of reference 27 yields for the inductance of the circuit

$$L = -(2/I^2)\sum_{i=1}^{m+n}\sum_{j=1}^{m+n} w_i w_j S_i S_j \log D_{ij} \qquad (6)$$

As equation 6 is identical with equation 1 except for the difference in the upper limits of the summations, we obtain from equations 4 and 5 by analogy

$$f_D = -\sum_{i=1}^{m+n}\sum_{j=1}^{m+n} w_i w_j S_i S_j \,\partial(\log D_{ij})/\partial D \qquad (7)$$

$$f_P = -\sum_{i=1}^{m+n}\sum_{j=1}^{m+n} w_i w_j S_i S_j \,\partial(\log D_{ij})/\partial P \qquad (8)$$

wherein f_D and f_P are the component forces on a conductor or a specified subconductor according as D and P are parameters locating one conductor with respect to the other or are parameters locating the subconductor with respect to the other $(m+n-1)$ subconductors.

If we have a double circuit comprised of $p+q$ conductors completely *in toto*, one circuit of two conductors being comprised of p subconductors numbered from 1 to p and the other of q subconductors numbered from $p+1$ to $p+q$, the two circuits carrying currents I_p and I_q respectively, equation 26 of reference 27 yields for the mutual inductance of the two circuits

$$M = -(1/I_pI_q)\left[\sum_{i=1}^{p+q}\sum_{j=1}^{p+q} w_i w_j S_i S_j \log D_{ij} - \sum_{i=1}^{p}\sum_{j=1}^{p} w_i w_j S_i S_j \log D_{ij} - \sum_{i=p+1}^{p+q}\ \sum_{j=p+1}^{p+q} w_i w_j S_i S_j \log D_{ij}\right] \qquad (9)$$

As the second term within the braces is a function only of the parameters defining the geometry and relative location of the cross sections of the conductors comprising the circuit carrying I_p; as a similar statement is true for the third term; and as, therefore, both terms are independent of the parameters, say D' and P', used to locate the conductors of one circuit relative to the other, differentiation of the right-hand member of equation 9 yields

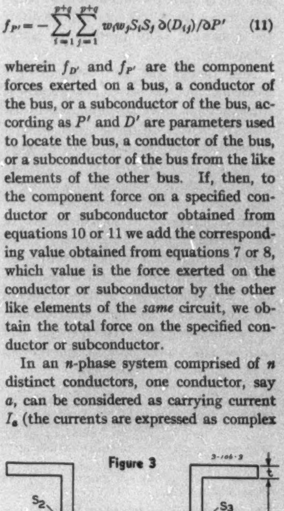
$$f_{D'} = -\sum_{i=1}^{p+q}\sum_{j=1}^{p+q} w_i w_j S_i S_j \,\partial(D_{ij})/\partial D' \qquad (10)$$

$$f_{P'} = -\sum_{i=1}^{p+q}\sum_{j=1}^{p+q} w_i w_j S_i S_j \,\partial(D_{ij})/\partial P' \qquad (11)$$

wherein $f_{D'}$ and $f_{P'}$ are the component forces exerted on a bus, a conductor of the bus, or a subconductor of the bus, according as P' and D' are parameters used to locate the bus, a conductor of the bus, or a subconductor of the bus from the like elements of the other bus. If, then, to the component force on a specified conductor or subconductor obtained from equations 10 or 11 we add the corresponding value obtained from equations 7 or 8, which value is the force exerted on the conductor or subconductor by the other like elements of the *same* circuit, we obtain the total force on the specified conductor or subconductor.

In an n-phase system comprised of n distinct conductors, one conductor, say a, can be considered as carrying current I_a (the currents are expressed as complex

Figure 3



numbers) and the remaining $(n-1)$ conductors can be considered as connected in parallel and carrying the return current. Accordingly, our problem is interpretable as a single-phase circuit comprised of two conductors, one full, the other divided, whence we have from equations 7 and 8

$$f_D=-I_a\sum_{i=b}^{n} I_i\,\partial(\log D_{ai})/\partial D \qquad (12)$$

$$f_P=-I_a\sum_{i=b}^{n} I_i\,\partial(\log D_{ai})/\partial P \qquad (13)$$

wherein P and D are parameters locating S_a with respect to $S_b, \ldots, S_n$.

If the individual conductors are divided, a composed of a' subconductors numbered from 1 to a', b of b' subconductors numbered from $a'+1$ to $a'+b'$, and correspondingly for the others, we have from equations 7 and 8

$$f_D=-\sum_{i=1}^{a'}\sum_{j=1}^{a'+\ldots+n'} w_iw_jS_iS_j\,\partial(\log D_{ij})/\partial D \qquad (14)$$

$$f_P=-\sum_{i=1}^{a'}\sum_{j=1}^{a'+\ldots+n'} w_iw_jS_iS_j\,\partial(\log D_{ij})/\partial P \qquad (15)$$

where now f_D and f_P give the component forces on the entire conductor a or on any subconductor of a, say a', according as D and P are parameters locating conductor a with respect to the other $n-1$ conductors, or are parameters locating subconductor a' with respect to the other $a'+\ldots+n'-1$ subconductors.

With regard to the physical quantities mentioned to this point, all units are in the absolute system: linear dimensions in centimeters, current I in abamperes; self inductance L and mutual inductance M in abhenrys per centimeter of bus length; energy W in ergs per centimeter of bus length; force f in dynes per centimeter of bus length.

Reviewing the analysis of this section, we see that in each case treated the total force, $f=(f_D^2+f_P^2)^{1/2}$, is determined in both magnitude and direction providing the derivatives indicated in the summations are expressible as known functions of the dimensions of the conductors and bus. Accordingly, we turn now to development of formulas for calculating these derivatives.

II. Calculation of the Derivatives of the Geometric Mean Distances

In earlier papers the writer[27,28] has derived the geometric mean distance D_{12} of a rectangular area S_1 from a second arbitrarily located parallel-sided rectangular area S_2, dimensioned as in Figure 1, to be

$$S_1S_2\log D_{12}=-(25/12)S_1S_2-(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}K(A_i,B_j) \qquad (16)$$

wherein

$$K(A_i,B_j)=(A_i^4-6A_i^2B_j^2+B_j^4)\times\log(A_i^2+B_j^2)+4A_iB_j(B_j^2-A_i^2)\times\tan^{-1}(B_j/A_i)-2\pi A_iB_j \qquad (17)$$

and

$$A_1=|D+R+r|;\; A_2=|D+R|;\; A_3=|D|;\; A_4=|D+r| \qquad (18)$$

$$B_1=|P+S+s|;\; B_2=|P+S|;\; B_3=|P|;\; B_4=|P+s| \qquad (19)$$

The parameter D in equation 18, and similarly P in equation 19, may be positive, zero, or negative. Thus, if $r=R$, $s=S$, $D=-r$, and $P=-s$, the two areas S_1 and S_2 are identical and superimposed, and equation 16 yields the formula for the self geometric mean distance of a rectangular area. Other more detailed illustrations of application of equation 16 to the determination of the geometric mean distance between two variously located rectangular areas are contained in section V of reference 27 and in section III of this paper.

Turning to calculation of the derivatives indicated in the summations of the preceding section, we have from equation 16 that

$$S_1S_2\,\partial(\log D_{12})/\partial D=-(1/24)\times\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}\partial K(A_i,B_j)/\partial A_i\,dA_i/dD \qquad (20)$$

From equation 17

$$\partial K(A_i,B_j)/\partial A_i=4A_i(A_i^2-3B_j^2)\times\log(A_i^2+B_j^2)+4B_j^3(B_j^2-3A_i^2)\times\tan^{-1}(B_j/A_i)+A_i^3-3A_iB_j^2-2\pi B_j^3 \qquad (21)$$

From equation 18

$$dA_i/dD=d|D+\text{constant}|/dD=\pm1 \qquad (22)$$

the plus or the minus sign to be chosen according as in a particular problem the specified value of D is such that the quantity within the bars is positive or is negative. Finally, substituting equations 21 and 22 in equation 20 yields the desired formula

$$S_1S_2\,\partial(\log D_{12})/\partial D=-(1/24)\times\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}[4A_i(A_i^2-3B_j^2)\times\log(A_i^2+B_j^2)^{1/2}+4B_j(B_j^2-3A_i^2)\times\tan^{-1}(B_j/A_j)+A_i^3-3A_iB_j^2-2\pi B_j^3]dA_i/dD \qquad (23)$$

Certain variants of equation 23 are of value. If dA_i/dD is of the same sign for $i=1$ to 4, then

$$\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}A_i^3dA_i/dD=2\pi\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}B_j^3dA_i/dD=0 \qquad (24)$$

whence equation 23 reduces to

$$S_1S_2\,\partial(\log D_{12})/\partial D=-(1/24)\times\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}[4A_i(A_i^2-3B_j^2)\times\log(A_i^2+B_j^2)^{1/2}+4B_j(B_j^2-3A_i^2)\times\tan^{-1}(B_j/A_i)-3A_iB_j^2]dA_i/dD \qquad (25)$$

Further, if $dA_i/dD=1$ for $i=1$ to 4, then

$$3\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+}A_iB_j^2dA_i/dD=0 \qquad (26)$$

whence, in turn, equation 25 reduces to

$$S_1S_2\,\partial(\log D_{12})/\partial D=-(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)\quad U(A_i,B_j) \qquad (27)$$

wherein, for convenience of future reference,

$$U(A_i,B_j)=4A_i(A_i^2-3B_j^2)\log(A_i^2+B_j^2)^{1/2}+4B_j(Bj^2-3A_i^2)\tan^{-1}(B_j/A_i) \qquad (28)$$

By virtue of the identity, $K(A_i,B_j)=K(B_j,A_i)$ (to be obtained from equation 17 by interchange of A_i with B_j and subsequent use of the trigonometric identity, $\tan^{-1}(A_i/B_j)+\tan^{-1}(B_j/A_i)=\pi/2$), corresponding expressions for $S_1S_2\partial\times(\log D_{12})/\partial P$ follow from those for $S_1S_2\partial(\log D_{12})/\partial D$ through interchange of A_i and B_j and replacement of D by P. As this operation is easily effected, for economy of space we here set forth only the analog of equation 27:

$$S_1S_2\,\partial(\log D_{12})/\partial P=-(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}(VA_i,B_j) \qquad (29)$$

wherein

$$V(A_i,B_j)=4B_j(B_j^2-3A_i^2)\log(A_i^2+B_j^2)^{1/2}+4A_i(A_i^2-3B_j^2)\tan^{-1}(A_i/B_j) \qquad (30)$$

Equations 16 to 30 inclusive suffice for calculation of the derivatives occurring in the double summations defining f_D and f_P. Accordingly, inasmuch as for any given set of data the components f_D and f_P are completely determinable through use of the analysis developed to this point, it follows that this analysis comprises the general solution of the problem of calculating the electromagnetic force, $f=(f_D^2+f_P^2)^{1/2}$, exerted on a specified conductor or subconductor of a d-c single-phase or polyphase bus comprised of conductors the cross sections of which are arbitrary configurations of parallel-sided rectangular areas.

In general the double summation indicated in the right-hand member of each of equations 23, 25, and 27 is calculated by inserting the numerical values of the parameters A_i and Bj (determined from equations 8 and 9 respectively), obtaining the indicated logarithms and arc tangents from suitable tables, and performing the necessary arithmetic. If, however, it happens—and this case is the rule rather than the exception—that the bus and conductor geometry is such that equation 27 is valid and, further, that therein the summation to be effected is of a form typified by

$$\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}U(A_i, B_j)= \sum_{j=1}^{4}(-1)^{j}F(D, r, B_j) \qquad (31)$$

whereof, typically,

$$F(D, r, s)=2U(D, s)-U(D+r, s)-U(D-r, s) \qquad (32)$$

the U functions being those defined by equation 28, the numerical computation can be effected more quickly and with less chance of numerical error through use of the rapidly converging Taylor's series in which the right-hand member of equation 32, considered as a function of D, can be expanded about the point $D=r$; that is, by

$$F(D, r, s)=12r^2[2s \tan^{-1} (s/D)-D\times \log (D^2+s^2)+(20/12)-Dr^2/6(D_2+s^2)- Dr^4(D^2-3s^2)/90(D^2+s^2)^3- Dr^6(D^4-10D^2s^2+5s^4)/420(D^2+s^2)^5- \ldots] \qquad (33)$$

The details of this expansion are given in the appendix. Substituting appropriately from equation 33 in equation 31, noting that in the resulting expansion

$$\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}(20/12)=0$$

simplifying, and then substituting in equation 27 we have

$$S_1S_2\, \partial(\log D_{12})/\partial D = (-1/2)\sum_{j=1}(-1)^{j}G(D, r, B_j) \qquad (34)$$

wherein

$$G(D, r, B_j)=Dr^2[2n \tan^{-1} n- \log(D^2+B_j^2)-m^2/6(1+n^2)- m^4(1-3n^2)/90(1+n^2)^3- m^6(1-10m^2n^2+5n^4)/420(1+n^2)^5- \ldots] \qquad (35)$$

and $m=r/D$, $n=B_j/D$.

Analogously, if equation 27 holds, we have from equation 28

$$S_1S_2\, \partial(\log D_{12})/\partial P = -(1/2)\sum_{i=1}^{4}(-1)^{i}H(P, A_i, s) \qquad (36)$$

wherein

$$H(P, A_i, s)=Ps^2[2m \tan^{-1} m- \log (P^2+A_i)^2-n^2/6(1+m^2)- n^4(1-3m^2)/90(1+m^2)^3- n^6(1-10n^2m^2+5m^4)/420(1+m^2)^5- \ldots] \qquad (37)$$

and now $m=A_i/P$, $n=s/P$.

Again, it often happens in practice that the right-hand member of equation 34 reduces to, typically,

$$\sum_{j=1}^{4}(-1)^{j}G(D, r, B_j)=-2G(D, r, s)+2G(D, r, 0) \qquad (38)$$

If so, by virtue of equations 35 and 38, we have from equation 34 the very convenient form

$$S_1S_2\, \partial(\log D_{12})/\partial D = DM(D, r, s) \qquad (39)$$

where $M(D, r, s)$ is defined by

$$M(D, r, s)=r^2[2n \tan^{-1} n- \log(1+n^2)+ m^2n^2/6(1+n^2)+ \ldots] \qquad (40)$$

and $m=r/D$, $n=s/D$.

Analogously, from equation 37 we have

$$S_1S_2\, \partial(\log D_{12})/\partial P = PN(P, r, s) \qquad (41)$$

where $N(P, r, s)$ is defined by

$$N(P, r, s)=s^2[2m \tan^{-1} m- \log(1+m^2)+n^2m^2/6(1+m^2)+ \ldots] \qquad (42)$$

and now $m=r/P$, $n=s/P$.

III. Some Illustrative Examples

Example 1 is advanced as corroborative of the preceding analysis. Example 2 is advanced as demonstrative of application of this analysis to calculation of the electromagnetic force exerted between two conductors of structural shape.

EXAMPLE 1

A single-phase bus is comprised of two identical strap conductors (Figure 2). To calculate the electromagnetic force per unit length:

For this case: $r=R$; $s=S$; $D=D$; $P=-s$; $S_1=S_2=rs$; $w_1=-w_2=I/S_1=I/rs$. Then equations 18, 19, and 22 yield $A_1=D+2r$; $A_2=A=D+r$; $A_3=D$; $B_1=B_3=S$; $B_2=B_4=0$; $\partial A_i/\partial D=1$, $i=1, 2, 3, 4$. From equation 4, noting that the self geometric mean distances, D_{11} and D_{22}, are independent of the parameter D,

$$\begin{aligned} f_D &= -\sum_{i=1}^{2}\sum_{j=1}^{2}w_iw_jS_iS_j\, \partial(\log D_{ij})/\partial D \\ &= -2w_1w_2S_1S_2\, \partial(\log D_{12})/\partial D \\ &= (2I^2/r^2s^2)S_1S_2\, \partial(\log D_{12})/\partial D \qquad (43) \end{aligned}$$

By virtue of equations 27, 31, 32, 34, and 39 we obtain

$$\begin{aligned} &S_1S_2\, \partial(\log D_{12})/\partial D \\ &= -(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}U(A_i, B_j) \\ &= -(1/24)\sum_{j=1}^{4}(-1)^{j}[2U(D+r, B_j)- U(D+2r, B_j)-U(D, B_j)] \\ &= -(1/24)\sum_{j=1}^{4}(-1)^{j}F(D+r, r, B_j) \\ &= -(1/2)\sum_{j=1}^{4}(-1)^{j}G(D+r, r, B_j) \\ &= G(D+r, r, s)-G(D+r, r, 0) \\ &= (D+r)M(D+r, r, s) \qquad (44) \end{aligned}$$

wherein

$$M(D+r, r, s)=r^2[2n \tan^{-1} n- \log (1+n^2)+m^2n^2/6(1+n^2)+ \ldots] \qquad (45)$$

and $m=r/(D+r)$, $n=s/(D+r)$.

Finally, substituting from equation 44 in equation 43 we have

$$F_D= [2I^2/(D+r)][(D+r)^2M(D+r,r,s)/r^2s^2] \text{ dynes per centimeter of bus length} \qquad (46)$$

or in units more commonly used: current I in amperes; D, r, s, in inches

$$f_D= [5.4I^2/(D+r)][(D+r)^2\times M(D+r, r, s)/r^2s^2]10^{-7} \text{ pounds per foot of bus length} \qquad (47)$$

The dimensionless term in the braces, commonly represented by the symbol k_D, is termed the *electromagnetic space factor* for f_D.

By symmetry the component $f_p=0$. Accordingly, the total force, $f=f_D$, is given by equation 46 or 47. Allowing for the difference of notation, these formulas for f_D agree with those obtained earlier by H. B. Dwight[23] (who first considered this problem) and by the writer.[24]

EXAMPLE 2

A single-phase bus is comprised of two identical channels placed back to back (Figure 3). To calculate the electromagnetic force per unit length of bus:

Each of the two channels can be considered as comprised of two component conductors: one of full rectangular cross section wherein current exists in the same direction as in the channel, the other of smaller rectangular cross section having current in the opposite direction. Hence $S_1=S_4=rs$; $S_2=S_3=(r-t)(s-2t)$; $w_1=-w_2=w_3=-w_4=I/(S_1-S_2)=I/t(s-2r-2t)$.

From equation 7, noting that the self geometric mean distances, $D_{11}=D_{44}$ and $D_{22}=D_{33}$, are independent of the parameter D, we obtain

$$\begin{aligned} f_D &= -\sum_{i=1}^{4}\sum_{j=1}^{4}w_iw_jSiS_j\, \partial(\log D_{ij})/\partial D \\ &= [2I^2/t^2(s-2r-2t)^2]\,[S_1^2\, \partial(\log D_{14})/\partial D+ S_2^2\, \partial(\log D_{23})/\partial D-2S_1S_3\, \partial(\log D_{13})/\partial D] \qquad (48) \end{aligned}$$

From equations 18, 19, and 22 we obtain for the three sets of (A_i, B_j) parameters

S_1, S_4: $A_1=D+2r$; $A_2=D+r$; $A_3=D$
$B_1=B_3=s$; $B_2=B_4=0$
S_2, S_3: $A_1=D+2r$; $A_2=A_4=D+r+t$; $A_3=D+2t$
$B_1=B_3=s-2t$; $B_2=B_4=0$
S_1, S_3: $A_1=D+2r$; $A_2=D+r+t$; $A_3=D+t$; $A_4=D+r$
$B_1=B_3=s-t$; $B_2=B_4=t$

The values of the first two derivatives in the right-hand member of equation 48 can be written down by analogy with the value found for the derivative in equation 43. Thus

$$S_1^2\,\partial(\log D_{14})/\partial D=(D+r)M(D+r,\ r,\ s) \tag{49}$$

and

$$S_2^2\,\partial(\log D_{23})/\partial D =(D+r+t)M(D+r+t,\ r-t,\ s-2t) \tag{50}$$

Calculation of the third derivative is effected in precisely the same fashion as in equation 44:

$$\begin{aligned}S_1S_2\,\partial(\log D_{13})/\partial D &=-(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}U(A_i,\ B_j)\\ &=-(1/24)\sum_{j=1}^{4}(-1)^j([-U(D+2r,\ B_j)+ U(D+r+t,\ B_j)-U(D+t,\ B_j)+ U(D+r,\ B_j)]\\ &=-(1/24)\sum_{j=1}^{4}(-1)^j[-U(D+2r,\ B_j)- U(D+t,\ B_j)+2U(D+r+2^{-1}t,\ B_j)+ U(D+r+t,\ B_j)+U(D+R,\ B_j)- 2U(D+r+2^{-1}t,\ B_j)]\\ &=-(1/2)\sum_{j=1}^{4}(-1)^j[G(D+r+2^{-1}t,\ r- 2^{-1}t,\ B_j)-G(D+r+2^{-1}t,\ t/2,\ B_j)]\\ &=(D+r+2^{-1}t)[M(D+r+2^{-1}t,\ r-2^{-1}t, s- t)-M(D+r+2^{-1}t, 2^{-1}t, s-t)+ M(D+r+2^{-1}t, r-2^{-1}t, t)+ M(D+r+2^{-1}t,\ 2^{-1}t,\ t)]\end{aligned} \tag{51}$$

Finally, substitution of equations 49, 50 and 51 in equation 48 yields the desired formula

$$f_D=[2I^2/t^2(s-2r-2t)^2]\{(D+r)M(D+ r,r,s)+(D+r+t)\ M(D+r+t,\ r-t,\ s- 2t)-(D+r+2^{-1}t)[M(D+r+2^{-1}t,\ r- 2^{-1}t,\ s-t)-M(D+r+2^{-1}t,\ 2^{-1}t,\ s- t)+M(D+r+2^{-1}t,\ r-2^{-1}t,\ t)+ M(D+r+2^{-1}t,\ 2^{-1}t,\ t)]\}\ \text{dynes per centimeter of bus length} \tag{52}$$

wherein, typically, $M(D+r,\ r,\ s)$ is given by equation 45.

As by symmetry $f_P=0$, we have $f=f_D$. Inasmuch as the term, electromagnetic space factor, has significance—at least in the sense hitherto employed—only if each of the cross sections of the two conductors possess two principal axes of symmetry, k_D is not to be distinguished. Accordingly, equation 52 encompasses the complete solution of the problem.

Appendix

Consider as a function of D alone

$$F(D,\ r,\ s)=2U(D,\ s)-U(D+r,\ s)- U(D-r,\ s)$$

can be expanded in a Taylor's series about the point $D=r$. Accordingly, by Taylor's theorem we have

$$F(D,\ r,\ s)=-r^2\partial^2U(D,\ s)/\partial D^2- (r^4/2\cdot 3!)\partial^4U(D,\ s)/\partial D^4- (r^6/3\cdot 5!)\partial^6U(D,\ s)/\partial D^6-\dots$$

wherein

$$\begin{aligned}U(D,\ s)&=4D(D^2-3s^2)\log\,(D^2+s^2)^{1/2}+ D(D^2-3s^2)\tan^{-1}\,(s/D)\\ \partial U(D,\ s)/\partial D&=6(D^2-s^2)\log\,(D^2+s^2)- 24Ds\tan^{-1}\,(s/D)+4(D^2-s^2)\\ \partial^2U(D,\ s)/\partial D^2&=12D\log\,(D^2+s^2)- 24s\tan^{-1}\,(s/D)+20D\\ \partial^3U(D,\ s)/\partial D^3&=12\log\,(D^2+s^2)-44\\ \partial^4U(D,\ s)/\partial D^4&=24D(D^2+s^2)^{-1}\\ \partial^5U(D,\ s)/\partial D^5&=-24(D^2-s^2)(D^2+s^2)^{-2}\\ \partial^6U(D,\ s)/\partial D^6&=48D(D^2-3s^2)(D^2+s^2)^{-3}\\ \partial^7U(D,\ s)/\partial D^7&=-144(D^4-6D^2s^2+s^4)\times (D^2+s^2)^{-4}\\ \partial^8U(D,\ s)/\partial D^8&=576D(D^4-10D^2s^2+5s^4)\times (D^2+s^2)^{-5}\end{aligned}$$

Substitution of the derivatives in the preceding series yields

$$F(D,\ r,\ s)=12r^2[2s\tan^{-1}\,(s/D)-D\times \log\,(D^2+s^2)+(20/12)-Dr^2/6(D^2+s^2)- Dr^4(D^2-3s^2)/90(D^2+s^2)^3- Dr^6(D^4-10D^2s^2+5s^4)/420(D^2+s^2)^5-\dots]$$

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Distribution Factors and Pitch Factors of the Harmonics of a Fractional-Slot Winding

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Synopsis: The investigation of the fractional-slot windings with respect to their harmonic (differential) leakage, to noise, and so forth, requires the knowledge of the distribution factors of their harmonics.

Very valuable work on distribution and pitch factors of fractional-slot windings has been done by J. F. Calvert.[1] On the basis of charts he worked out distribution-factor tables covering the actual harmonics up to the 24th. Once the chart of the winding has been set up, a simple formula can be used for computing the distribution factors of the harmonics for any three-phase fractional-slot winding.

In the following paper, simple formulas are derived for the distribution factors of the harmonics of fractional-slot windings which make the layout of charts unnecessary. These formulas are similar to those of the integral-slot windings. In deriving them the usual method of attack, namely, the use of charts, had to be abandoned and the vector diagram of the different slots used instead.

The Actual Harmonics of a Fractional-Slot Winding

IT has been shown in a previous paper[2] that

$$\left(\frac{n'}{p}+1\right)\frac{\beta}{2}=K \quad K \text{ integer excluding 0} \quad \textbf{(1a)}$$

and

$$\left(\frac{n'}{p}\mp 1\right)\frac{\beta}{2m_1}=K_1 \quad K_1 \text{ integer including 0} \quad \textbf{(1b)}$$

are the criteria for the existence of the n'th harmonic in the magnetomotive force of a fractional-slot winding. (For symbols see nomenclature.)

When the minus sign is used in the equation 1b, the harmonic travels with the rotation; when the plus sign is used, the harmonic travels opposite to the rotation.

Since n' has to satisfy both equations 1a and b, the actual harmonics produced by the winding will be different for different values of β and p. We have to distinguish between the case when β is equal to an even number and the case when β is equal to an odd number. It can be found easily that the actual harmonics of the three-phase windings are as follows:

When β = even number

$$n'=\frac{2p}{\beta}\nu \quad \text{where } \nu=1, 2, 3, 4, 5 \ldots \qquad (2)$$

For the synchronous (main) wave $n'=p$ and, therefore, $\nu_s=\beta/2$. The number of subharmonics is equal to $(\beta-2)/2$.

When β = odd number

$$n'=\frac{p}{\beta}\nu \quad \text{where } \nu=1, 3, 5, 7 \ldots \qquad (3)$$

For the synchronous wave here $\nu_s=\beta$ and the number of subharmonics is $(\beta-1)/2$.

The harmonics which are a multiple of three do not exist in the three-phase machine (except when zero-sequence currents flow in the winding).

The Slot Star

The use of the slot star instead of the generally used chart not only facilitates the layout of the fractional-slot winding but also makes it possible to derive simple formulas for the distribution factor and pitch factor of the harmonics of this winding.

Figure 1 represents the slot star of a winding with an integral number of slots per pole per phase, namely $q=2$. The integral-slot windings can be considered as a special case of the fractional-slot winding having $\beta=1$. Thus each pole represents a repeatable group (a unit) and only one pole has to be considered. The angle between two slots is

$$\alpha_s=\frac{180}{3\times 2}=30°$$

To two adjacent vectors correspond two adjacent slots. The vector 7, with which the next pole starts, is shifted 180 degrees with respect to the vector 1. As regards the synchronous (main) wave, it is taken care of by a proper connection between the coil groups.

We consider now a winding with $1^1/_2$ slots per pole per phase, $q=1^1/_2$. If we write in general

$$q=\frac{N}{\beta} \qquad \textbf{(4)}$$

β poles represent a repeatable group (a unit), each phase has N slots in β poles, and the total number of slots in β poles is equal to $3N$.

In our example two poles make a unit, each phase has three slots per unit, and the total number of slots per unit is equal to nine. The angle between two slots is as before,

$$\alpha_s=\frac{180}{mq} \qquad \textbf{(5)}$$

and it is equal to 180/4.5=40 degrees. The angles which correspond to the nine slots of the unit are

Slot	1	2	3	4	5	6	7	8	9	10
Angle	0	40	80	120	160	200	240	280	320	360=0°

To slot 10 corresponds the angle zero degrees; slot 10 is the beginning of the next repeatable group. Slots 1 to 5 lie under the first pole, slots 6 to 9 lie under the second pole of the unit. Since the connections between the coil groups take into account a shifting of 180 degrees, the real angles between the slots, that is, the shifting of the slots with respect to each other in the magnetic field, are

Slot	1	2	3	4	5	6	7	8	9	10
Angle	0	40	80	120	160	20	60	100	140	180°

and the slot star of the repeatable group is given by Figure 2. Between vectors 1 and 2, which correspond to slots 1 and 2, lies the vector which corresponds to slot 6; between vectors 2 and 3, which correspond to slots 2 and 3, lies the vector which corresponds to slot 7, and so on. The winding creeps in the magnetic field. In general, between two vectors which correspond to two adjacent slots lie $(\beta-1)$ other vectors. We have to distinguish between the angle between

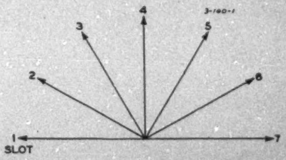

Figure 1. Slot star of a winding with q = 2

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two slots as given by equation 5 and the angle between two adjacent vectors. This latter angle is the magnetic-field angle between the slots of the repeatable group; this angle determines the behavior of the winding with respect to its magnetomotive force and electromotive force, that is, its distribution factors and pitch factors.

The magnetic-field angle is

$$\alpha_m = \frac{180}{Nm} \tag{6}$$

$\alpha_s = \alpha_m$ only for the winding with an integral number of slots per pole per phase, because for this winding $\beta = 1$ and $N = q$. For the fractional-slot windings α_m is always smaller than α_s. It is

$$\frac{\alpha_s}{\alpha_m} = \beta \tag{7}$$

Apparently, the highest distribution factor for the synchronous wave will be obtained when the first three vectors of the slot star, Figure 2, are assigned to phase I, the following three vectors are assigned to phase II, and the last three vectors to phase III. Thus, in each repeatable group phase I will occupy slots 1, 2, and 6; phase II, slots 3, 7, and 8; and phase III, slots 4, 5, and 9.

We consider the sequence of the slots in the slot star, Figure 2. If we start with slot 1, the sequence of the slots follows the series

$1, 1+5, 1+2\times5, 1+3\times5 \ldots\ldots$

Since the total number of slots in the repeatable group is equal to $3\times N$, this value (or a multiple of it) has to be subtracted from the terms of the series when they are larger than $3N$.

In general, the series is

$1, 1+d, 1+2d, 1+3d \ldots\ldots$

where d is the difference between two slots which correspond to two adjacent vectors of the slot star.

This difference d can be found from the following consideration.[3] If we denote by P the number of full pole pitches between two slots which correspond to two adjacent vectors of the slot star (in our example $P = 1$), then there will be

$$d \times \alpha_s = \alpha_m + 180P$$

Inserting α_s and α_m from equations 5 and 6 there will be

$$d = \frac{m_1NP+1}{\beta} \tag{8}$$

For P the smallest integer for which d becomes an integer has to be inserted. P is equal to or larger than one.

The Distribution Factors of Three-Phase Windings

The slot star Figure 2 relates to the synchronous wave, the length of which is equal to 2τ. With respect to this wave the winding behaves as if there are N slots per pole per phase shifted with respect to each other by the magnetic-field angle α_m, which is equal to the angle between two adjacent vectors. Thus, the distribution factor of the synchronous wave is

$$K_d(n'=p) = \frac{\sin N^{\alpha_m}/2}{N \sin^{\alpha_m}/2} = \frac{\sin 30^\circ}{N \sin 30^\circ/N} \tag{9}$$

In order to determine the distribution factor of the n'th harmonic, it is only necessary to determine the angle between two adjacent vectors of Figure 2 for this harmonic. The number of slots per pole per phase N is the same for all harmonics.

We consider first the wave with the length equal to $2\beta\tau$. With respect to this wave the angle between two slots is,

$$\alpha_{s\beta} = \frac{\alpha_s}{\beta}$$

Since the winding behaves with respect to this wave as a normal integral-slot winding the magnetic-field angle must be equal to the slot angle, that is,

$$\alpha_{m\beta} = \alpha_{s\beta} = \frac{\alpha_s}{\beta} = \alpha_m$$

Figure 3 shows the slot star with respect to this wave. Adjacent vectors lie in adjacent slots. To two adjacent vectors of Figure 2 corresponds here the angle $d\alpha_m$. For example, to the two adjacent vectors 1 and 6 of Figure 2 corresponds here the angle $5\times\alpha_m = d\alpha_m$. Thus, for the harmonic the wave length of which is equal to $2\beta\tau$ the angle between two adjacent vectors of Figure 2 is equal to $d\alpha_m$. It is necessary to add to this angle 180 degrees when P is an odd number, in order to take into account the opposite direction of the current.

When we consider now the fundamental wave ($n'=1$), the wave length of which is equal to $2p\tau$, the angle between two adjacent vectors of Figure 2 for this wave will be $d\alpha_m\beta/p$ or $d\alpha_m\beta/p+180$ when P is an odd number. Therefore, the corresponding angles for the n'th harmonics are

$$\alpha_{mn'} = n'd\alpha_m\frac{\beta}{p} \quad \text{(when } P \text{ is an even number)} \tag{10}$$

$$\alpha_{mn'} = n'd\alpha_m\frac{\beta}{p}+180 \quad \text{(when } P \text{ is an odd number and } \beta \text{ is even)}$$

$$\alpha_{mn'} = n'(d\alpha_m+180)\frac{\beta}{p} \quad \text{(when } P \text{ is an odd number and } \beta \text{ is odd)}$$

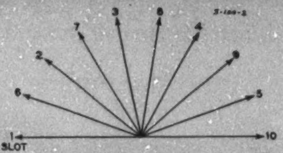

Figure 2. Slot star of a winding with $q = 1^1/_2$ with respect to the synchronous wave

and the distribution factor of the n'th harmonic is

$$K_{dn'} = \frac{\sin N\,\alpha_{mn'}/2}{N \sin \alpha_{mn'}/2} \tag{11}$$

It can be shown that the numerator of equation 11 is equal to sin 30 degrees for all harmonics which are not a multiple of 3. We apply equations 2 and 3 to equation 10. We find:

When β = even number

$$n' = \frac{2p}{\beta}\nu \qquad \nu = 1, 2, 4, 5, 7 \ldots\ldots \nu_s = \frac{\beta}{2}$$

and

$$\alpha_{mn'} = 2d\alpha_m\nu + 180 \tag{12}$$

P is here always an odd number: since N is here an odd number, d can become an integer only when P is an odd number.

When β = odd number

$$n' = \frac{p}{\beta}\nu \qquad \nu = 1, 5, 7, 11, 13 \ldots\ldots \nu_s = \beta$$

$$\alpha_{mn'} = d\alpha_m\nu \quad \text{(when } P \text{ is an even number)}$$

$$\alpha_{mn'} = d\alpha_m\nu + 180 \quad \text{(when } P \text{ is an odd number)} \tag{13}$$

When β is an even number, N and P are odd numbers, d can be an odd as well as an even number, but d is not divisible by three. Thus, with N = odd number and

$$\alpha_m = \frac{180}{3N}$$

$$\sin N\,\alpha_{mn'}/2 = \sin N(d\alpha_m + 90) = \cos d\nu \times 60^\circ$$

The product $d\nu$ is not divisible by three, therefore

$$\sin N\alpha_{mn'}/2 = \cos d\nu \times 60 = \cos 60^\circ = \sin 30^\circ$$

When β is an odd number, N, P, and d can be even as well as odd numbers; but d is not divisible by three; further, d is an odd number when N or P is an even number, and d is an even number when N and P are odd numbers. We have to consider the cases where P is even and those where P is odd.

When P is an even number, then

$$\sin N\alpha_{n'}/2 = \sin Nd\alpha_m/2\times\nu = \sin d\nu \times 30$$

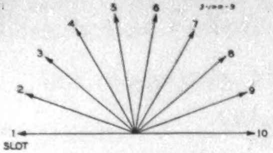

Figure 3. Slot star of a winding with $q=1\,^1/_2$ with respect to the wave whose length is equal to $2\beta\tau$

d and ν are odd numbers, not divisible by three. Thus $d\nu$ is an odd number not divisible by three and

$$\sin N\alpha_{mn'}/2 = \sin d\nu \times 30 = \sin 30 \text{ degrees}$$

When P is an odd number and N is an even number, then

$$\begin{aligned}\sin N\alpha_{mn'}/2 &= \sin N(d\alpha_m/2\times\nu+90)\\ &= \sin Nd\alpha_m/2\times\nu = \sin d\nu\times 30\end{aligned}$$

and the result is the same as for P equal to an even number.

When P is an odd number and N is an odd number, then d is an even number, and there will be

$$\begin{aligned}\sin N\alpha_{mn'}/2 &= \sin N(d\alpha_m/2\times\nu+90)\\ &= \sin(d\nu\times 30+90) = \cos d\nu\times 30\end{aligned}$$

Since $d\nu$ is an even number

$$\sin N\alpha_{mn'}/2 = \cos d\nu\times 30 = \sin 30^\circ$$

Therefore, we can write for equation 11

$$K_{dn'} = \frac{\sin 30^\circ}{N\sin\alpha_n'/2} \tag{14}$$

$\alpha_{n'}$ is given by equations 12 and 13. Inserting these equations and also equations 6 and 8, we find finally

When β = even number

$$K_{dn'} = \frac{0.5}{N\cos\left(\dfrac{d}{N}60\nu\right)} \tag{15}$$

When β = odd number

$$K_{dn'} = \frac{0.5}{N\sin\left(\dfrac{d}{N}30\nu\right)} \quad \text{(when } P \text{ is an even number)} \tag{16}$$

$$K_{dn'} = \frac{0.5}{N\cos\left(\dfrac{d}{N}30\nu\right)} \quad \text{(when } P \text{ is an odd number)} \tag{17}$$

Equations 15 to 17 are valid for the absolute values only. Equations 10 and 11 yield also the right signs.

Examples

Three-phase machine with 14 poles and $q=11/7$. Thus, $N=11$, $\beta=7$. Equation 8 yields $d=19$ with $P=4$. Since β is an odd number and P an even number, equation 16 has to be applied. It is $p=\beta$ and, therefore, $n'=\nu$ (see equation 3).

It is valid

$$K_{dn'} = \frac{0.5}{11\sin\dfrac{19}{11}30} = 0.058 \quad \text{for the fundamental wave}$$

$$K_{dn'} = \frac{0.5}{11\sin\dfrac{19}{11}30\times 5} = 0.0463 \quad \text{for the fifth harmonic}$$

and so forth.

Eight-pole machine with $q=11/4$, $N=11$, $\beta=4$, $d=25$ with $P=3$.

For $2p/\beta=2$, $n'=2\nu$ (see equation 2). Thus,

$$K_{dn'} = \frac{0.5}{11\cos\dfrac{25}{11}60} = 0.0625 \quad \text{for the second harmonic}$$

$$K_{dn'} = \frac{0.5}{11\cos\dfrac{25}{11}60\times 4} = 0.0457 \quad \text{for the eighth harmonic}$$

and so forth.

The Pitch Factors of Three-Phase Windings

For the pitch factor of the synchronous wave

$$K_{p(n'=p)} = \sin\frac{W}{\tau}90 \tag{18}$$

Denoting by Z the coil width in slot pitches, we can write for the synchronous wave

$$K_{p(n'=p)} = \sin\frac{Z}{mq}90 = \sin\frac{Z\beta}{mq\beta}90$$

$$= \sin\frac{Z\beta}{mN}90 = \sin Z\beta\frac{\alpha_m}{2} \tag{18a}$$

Referring α_m as in section 3 to the wave the length of which is equal to the total circumference of the armature ($2p\tau$), we find for the pitch factor of the n'th harmonic

$$K_{pn'} = \sin Z\beta\frac{\alpha_{n'}}{2} \tag{19}$$

$\alpha_{n'}$ is given by equations 12 and 13. As for the distribution factor, it must be distinguished between the case when β is an even number and the case when β is an odd number. Inserting in equation 19 equations 12, 8, and 6 for β = even number, and equations 13, 8, and 6 for β = odd number, we find in agreement with Calvert

$$K_{pn'} = \sin\frac{W}{\tau}\frac{180}{\beta}\nu \quad \text{(when } \beta = \text{even number)} \tag{20}$$

$$K_{pn'} = \sin\frac{W}{\tau}\frac{180}{2\beta}\nu \quad \text{(when } \beta = \text{odd number)} \tag{21}$$

Nomenclature

d—difference between two slots which correspond to two adjacent vectors of the slot star.

K_{dn}—distribution factor of the n'th harmonic.

K_{pn}—pitch factor of the n'th harmonic.

m_1—number of phases.

n'—order of the harmonic with respect to a fundamental whose wave length is equal to the circumference of the armature.

N—numerator of the fraction that fixes the number of slots per pole per phase.

p—number of pole pairs.

P—number of full pole pitches between two slots which correspond to two adjacent vectors of the slot star.

$q=\dfrac{N}{\beta}$—number of slots per pole per phase.

W—coil width.

Z—coil width in slot pitches.

$\alpha_s=\dfrac{180}{mq}$—angle between two slots.

$\alpha_m=\dfrac{180}{mN}$—angle between two vectors of the slot star.

β—denominator of the fraction that fixes the number of slots per pole per phase.

τ—pole pitch (in the units of W).

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See also bibliography to references 1 and 2.

Operation of Nonsalient-Pole-Type Generators Supplying a Rectifier Load

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IT has been recognized that the presence of harmonic currents in the supply circuit are a source of additional heating in the generators supplying a rectifier load, particularly where a six-phase rectifier is the total load. Robert Pohl has presented papers on this subject, and from his work has concluded that an appreciable reduction in the rating of turbine generators is required when the rectifiers are connected for 6-phase and 12-phase operation.[1,2] Experience in this country with turbine generators operating with rectifier loads indicated that the reduction factors presented by Doctor Pohl were too large, so a test program was carried out to determine the practical limits of such loads. The test results and experience both indicate that for 12-phase operation and above, the effect on the supply system is very small and in most cases may be neglected.

Any harmonic current flowing in the stator winding of a generator increases the temperature rise of the stator winding and surrounding parts. Also, these harmonics establish a magnetomotive force which results in currents flowing in the surface of the rotor, adding to the rotor heating. It can be shown that some harmonics rotate backward, and some harmonics rotate forward with respect to the fundamental.[3] Because it rotates against the rotation of the rotor, the magnetomotive force set up by the fifth harmonic results in a sixth harmonic on the surface of the rotor. Since the rotation of the seventh harmonic is in the same direction as that of the rotor, it also results in a sixth harmonic on the rotor surface. These two magnetomotive forces, rotating in opposite directions, result in a pulsating field on some axis of the rotor. Likewise, another pulsating field of 12 times normal frequency is set up by the action of the 11th and 13th harmonics. The harmonics, up to the 25th, present in the stator and rotor are shown in Table I.

Tests

The temperature tests were made on a 1,250-kva 2,300-volt three-phase, 60-cycle 3,600-rpm turbine generator supplying a 600-volt ignitron rectifier connected for double three-phase operation, as shown in Figure 2. Double three-phase is a form of six-phase operation. The regulation of the rectifier was approximately ten per cent. That is, the impedance of the lines, transformer, and rectifier was of such a value that there was a drop in voltage equal to ten per cent of the normal voltage when full generator load was applied. In addition, the lines were not supplied from what might be considered an infinite system, but instead, from a single generator. The subtransient reactance of this generator was approximately 11 per cent. The importance of this fact and its effect on the magnitude of harmonic currents will be shown later. The generator was operated at approximately full kilovolt-amperes for four temperature runs, with the temperature of the stator winding being measured by detector, and the temperature of the rotor winding being measured by the increase in resistance. Oscillograms of the wave form were taken during each run, in order to obtain the magnitude of the harmonic currents to be used in subsequent calculations. After each test, the rotor was inspected to determine whether burning had occurred, either between the wedges and the teeth, or at the joints between the retaining rings and the rotor body. There was, however, no evidence of burning on any of the runs, indicating that excessive heating did not occur under the loading conditions of the test. Table II shows the results of the tests. In order to obtain the additional temperature rise of the stator and rotor windings, preliminary temperature runs were made at zero load and full voltage; full kilovolt-amperes and 90 per cent power factor; and full kilovolt-amperes and zero per cent power factor. Using the results of these tests, it was possible to determine what the temperatures would have been if the harmonic currents had not been included in the load current. The additional temperature rise shown in the tables is the difference between the measured temperature rise and the normal temperature rise for that particular load.

Rotor Heating

In order to calculate the heating of the rotor, the loss can be determined by the use of the negative-sequence resistance of

Table I. Harmonics Present in Stator and Rotor

	Stator Harmonics		Resultant Rotor Harmonics		
Order of Harmonic	6-Phase Connection	12-Phase Connection	6-Phase Connection	12-Phase Connection	Rotation of Harmonics
1	1	1			Forward
3					
5	5		6		Backward
7	7		6		Forward
9					
11	11	11	12	12	Backward
13	13	13	12	12	Forward
15					
17	17		18		Backward
19	19		18		Forward
21					
23	23	23	24	24	Backward
25	25	25	24	24	Forward

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The authors acknowledge the assistance of J. H. Cox and L. A. Kilgore.

Table IIA. Generator Test Results—Temperature Runs

Rectifier Connected for Double Three-Phase Operation

Phase Delay (Degrees)	Voltage	Current	Power Factor	Stator Temperature Rise (Degrees Centigrade)		Rotor Temperature Rise (Degrees Centigrade)		Ambient Temperature (Degrees Centigrade)
				Total	Additional	Total	Additional	
0	2300	297	90.	40.	2.6	79.1	19.1	28.8
14.1	2300	302	89.3	40.8	2.8	86.	22.	29.7
24.7	2300	296	83.5	42.5	5.1	93.1	22.1	25.4
30.4	2300	295	81.5	44.	6.6	95.5	25.5	28.8

Table IIB. Generator Test Results—Per Cent Harmonic Currents in Supply Circuit

Rectifier Connected for Double Three-Phase Operation

Order of Harmonic	Phase Delay (Degrees)			
	0	14.1	24.7	30.4
1	99.0	99.5	98.26	98.3
3	0.10	1.76	0.99	1.80
5	11.80	13.50	15.30	15.30
7	6.72	7.24	5.60	6.63
9	0.11	1.29	0.48	2.05
11	2.41	4.10	5.77	5.38
13	1.75	2.92	3.05	3.89
17	1.03	1.10	2.94	2.61
19	1.18	0.53	1.59	2.28
23	0.33	1.17	1.59	1.15
25	0.76	0.89	0.86	1.02

the rotor. A description of the test method for obtaining this value is given in the appendix. A minor correction in the value of resistance must be made, since the loss varies as the 1.8 power of the negative-sequence current for turbine generators.[4] Since the fifth and seventh harmonics result in two sixth-harmonic waves rotating in opposite directions on the surface of the rotor, the current to be used in calculating the sixth-harmonic loss can be taken as the sum of the fifth and seventh harmonics. This figure of loss must be multiplied by three, for the three phases, and by two, because at 120 cycles only one-half the rotor loss is $I_2^2R_2$, the other half being supplied through the shaft. The sum of the harmonic currents gives the maximum loss for the complete periphery of the rotor, but the actual loss is an average between the points of maximum and minimum loss. Thus, the maximum loss must be reduced by a factor, K_a, in order to obtain the average loss. The factor K_a may be obtained from Figure 5, once the ratio of the two harmonic currents is known. The derivation of the curve in Figure 5 is given in the appendix.

In obtaining the temperature rise, a factor, K_v, of 0.11 watt per degree centigrade per square inch of rotor surface, neglecting the surface of the retaining rings, was used for this particular peripheral speed. An air velocity of one-half the peripheral speed of the rotor was assumed. Since the temperature rise was measured on the rotor winding instead of the rotor surface, it was necessary to take into account the portion of the rotor winding which was affected by the additional loss on the rotor surface. The construction of the rotor for this machine was such that the ventilation of the portion of the coils beneath the retaining rings was very good, and it was assumed that all the extra heat in these sections of the winding was dissipated to the air passing over the end turns of the rotor winding, leaving only the slot portion of the winding to be affected by the additional loss on the surface of the rotor. On this rotor, the slot portion of the winding was 54 per cent of the mean turn, and this value was used for the winding ventilation factor, K_w. This factor may vary widely, depending on the physical proportions of the rotor, as well as the amount of ventilation on the end turns of the rotor winding.

The temperature rise of the rotor winding caused by the harmonic currents may be calculated for the various even-numbered harmonics from the following equation, using the currents of order $n-1$ and $n+1$, where n is the order of the harmonic on the surface of the rotor.

$$\text{Temperature rise} = 2\times3\times\frac{K_w}{K_v\times S}\times \Sigma K_a(I_{n-1}+I_{n+1})^2(R_2-R_1)\sqrt{\frac{f}{120}}$$

where

K_a = average loss factor
K_w = winding ventilation factor
K_v = heat-dissipation factor
S = rotor surface ($\pi\times$rotor diameter$\times$core length)

Table III compares the calculated and test values for the four temperature runs.

Figure 1. Oscillogram showing wave form in generator field and armature circuits

Double three-phase ignitron operating with 30.4 degrees phase delay. 1,250-kva turbogenerator, 2,300 volts, three phase, 60 cycles, 3,600 rpm

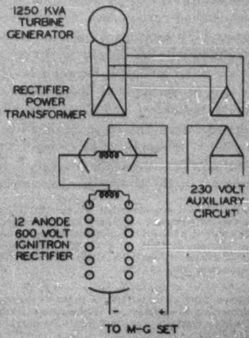

1250 KVA TURBINE GENERATOR
RECTIFIER POWER TRANSFORMER
230 VOLT AUXILIARY CIRCUIT
12 ANODE 600 VOLT IGNITRON RECTIFIER
-
+
TO M-G SET

Figure 2. Schematic diagram of test circuit

Table III. Additional Temperature Rise of Rotor Winding

Reduction Factors Applying to This Machine Connected to Double Three-Phase Ignitron

Phase Delay (Degrees)	Additional Rise (Degrees Centigrade)		Load Reduction Factor	
	Test	Calculated	Test	Calculated
0	19.1	19.3	1.0	1.0
14.1	22	27.6	0.995	0.95
24.7	22.1	32.3	0.92	0.85
30.4	25.5	32.2	0.9	0.85

The reduction in load to maintain not more than 85 degrees centigrade rotor temperature rise was calculated for each load condition and is given in Table III as a reduction factor. To determine this value, the additional temperature rise of the rotor caused by the harmonic currents was varied as the square of the load current, and the rise caused by the field current was calculated from the saturation curves of the machine. The two components were added by trial until the proper figure was obtained. It should be noted that these reduction factors apply only to this particular machine, and they cannot be generalized to cover all machines.

Figure 3 (above). 1,250-kva turbine generator on test

Stator Heating

The calculation of the heating of the stator winding is complicated further by the fact that the temperatures by detector are affected not only by the I^2R loss as determined by the eddy factor, but also by the heating of the stator parts, and by the extra loss on the surface of the rotor. It has been suggested that the load on the stator should be reduced according to the square root of the ratio of the normal loss to the loss with rectifier load as calculated by the use of the eddy factor alone, and neglecting any effect of core loss or rotor loss.[1] This method has been followed in the calculations for Table IV. These calculations have been made for the average loss, using the average eddy factor and for the maximum loss in the top strand. The effective eddy factor, $K_{\text{eff.}}$, equals $\Sigma I^2K_e/\Sigma I^2$, and the reduction, R, in load equals $\sqrt{K_e}$ (fundamental)$/K_{\text{eff}}$. The reduction factors, R and R', for the average and top strands, respectively, may be compared with the reduction factors from the measured additional temperature rise by inspection of Table IV.

Table IV. Additional Temperature Rise of Stator Winding

Reduction Factors Applying to This Machine Connected to Double Three-Phase Ignitron

Phase Delay (Degrees)	Additional Rise From Test (Degrees Centigrade)	Reduction Factor From Test	Calculated Reduction Factor	
			Average	Top Strand
0	2.6	0.966	0.963	0.95
14.1	2.8	0.965	0.94	0.917
24.7	5.1	0.937	0.915	0.895
30.4	6.6	0.922	0.907	0.887

The stator temperature rise on the normal full-load test was 38 degrees centigrade, which is considerably below the maximum value of 60 degrees centigrade rise. While this stator had considerable temperature margin, it was thought worth while to prepare a series of load reduction factors based on maintaining the full-load temperature rise of 38 degrees centigrade with various rectifier loads. It was assumed that the permissible load varied as the square root of the ratio of the temperature rise under normal load to the temperature rise with rectifier load. The reduction factors for this machine alone are given in Table IV.

12-Phase Operation

The foregoing tests and calculations were all on the basis of the rectifier operating with a double three-phase connection. If the connection had been 12 phase, the magnitude of the 5th, 7th, 17th, and 19th harmonics would have been reduced, with some improvement in power factor. In order to compare the performance with double 3-phase and 12-phase connections, the calculations have been made, eliminating the 5th,

Figure 4 (right). 12-anode ignitron on test

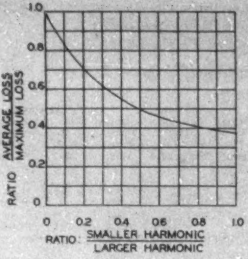


Figure 5. Values of rotor loss factor K_a

7th, 17th, and 19th harmonics, and assuming the magnitudes of the others were unchanged. The calculated reduction factor for the stator winding was 0.96 compared with 0.907 for the condition of 30.4 degrees phase delay. The calculated additional temperature rise for the rotor winding was approximately six degrees for the 30.4-degree delay condition compared to 25.5 degrees for the double three-phase connection Considering the improvement in power factor and the lower temperature rise, the rotor heating for the assumed conditions will not affect the permissible loading.

Conclusions

From these tests and calculations, and from previous studies of rectifier operation, the following conclusions may be drawn:

1. A rectifier load is the source of harmonic currents in the supply system. These harmonic currents cause extra heating in the stator and rotor of a generator, and, in some cases, may require a reduction in rating of supplying generators.

2. The order of the harmonic currents is affected by the number of phases of rectifier operation, and their magnitude is affected by the voltage reduction or phase delay, and the impedance of the complete system.[5,6,7] The lowest-order harmonic current which exists in the supply circuit is $P-1$, where P is the number of phases of rectifier operation. With a rectifier operating with a 12-phase connection, the additional heating of the generator will be slight, and with a number of phases higher than 12, the additional heating will be negligible. The magnitude of the harmonic currents is reduced with a reduction in degrees ignitor delay and an increase in system impedance.

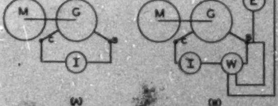


Figure 6. Generator connections for obtaining negative-sequence resistance

3. For a given load, the rotor heating is more pronounced than the stator heating, but operation at a relatively high power factor will introduce some margin in the rotor heating. Under certain conditions of operation the stator heating becomes the limiting factor.

4. It is not possible to state any definite rule for the reduction in rectifier load necessary in order to keep generator temperatures within recognized limits, because the extra heating is affected by the physical proportions of the machine, as well as the loading conditions. It is possible, however, to determine the magnitude of the harmonic currents,[5,6] once the loading conditions are known. The extra heating and any necessary reduction in load can be determined by the generator designer, using the methods outlined in this discussion.

Appendix A

Method of Obtaining Negative-Sequence Resistance

The negative-sequence resistance may be determined by either one of two methods.[4,8]

For Figure 6a, drive the generator at synchronous speed.

$$R_2=\frac{3(P-P_{F\&W})}{2I^2}$$

where

P = shaft input in watts
$P_{F\&W}$ = friction and windage loss in watts
I = phase current

For Figure 6b, drive the generator at synchronous speed.

$$\text{Power factor}=\frac{P}{E\times I}=\cos\theta$$

$$Z_2=3\frac{E}{I}$$

$$R_2=\frac{E}{3}\frac{\sin}{I}\theta$$

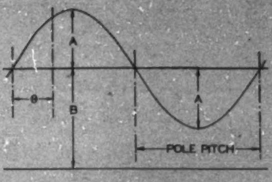
where

P = power in watts
E = line-to-line voltage
I = phase current

It should be noted that both of these methods require a sustained, single-phase short circuit, and care should be taken to see that the rotor is not overheated.

Appendix B

Since the fifth harmonic current rotates against the direction of rotation of the rotor, it generates a sixth harmonic current in the rotor surface. The seventh harmonic current rotates with the rotor, also generating a sixth harmonic current in the rotor surface, but the direction of rotation is opposite to that caused by the fifth harmonic current. Thus, there are two points of maximum loss and two points of minimum loss, with a sinusoidal distribution between. This relationship holds for any pair of harmonic currents which generate any particular frequency in the rotor surface. The distribution is shown in Figure 7, where A is the smaller of the two harmonic currents and B is the larger.



"A" IS THE SMALLER OF TWO HARMONIC CURRENTS

Figure 7. Resultant current distribution around rotor periphery

Let average loss around periphery equal L and angle around periphery equal θ, then

$$L=\frac{1}{2\pi}\int_0^{2\pi}(B+A\sin\theta)^2d\theta$$

$$=\frac{1}{2\pi}\left[B^2\theta-2AB\cos\theta+A^2\left(\frac{\theta}{2}-\frac{\sin 2\theta}{4}\right)\right]_0^{2\pi}$$

$$=B^2+\frac{A^2}{2}$$

Figure 5 gives the ratio of the average loss to the maximum loss for any ratio of the two harmonic currents.

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Rotating Regulator for Arc Furnaces

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Synopsis: Increased production of steel alloy for the war effort has brought about a wider use of electric-arc furnaces, and with this improvements have been made in automatic regulation of electrode position.

The Rototrol regulator described in this paper is of the rotating type which controls electrode position as determined by response to current in the electrode and voltage between the electrode and the furnace shell. Features and performance of a typical installation are illustrated.

AUTOMATIC adjustment of electrode position has always been used as a means of controlling the power input to steel-melting arc furnaces. This is necessary because the charge melts and flows away from the electrode so that the electrode must be lowered to maintain the arc. The scrap being melted frequently falls against the electrode, making withdrawal necessary to remove the short circuit. Furthermore, adjustment is necessary to allow for natural electrode consumption.

In addition to this the arc is a pure resistance load, and changing its length by adjustment of the electrode position gives a simple and satisfactory method of controlling the current.

The early arc-furnace regulator was a simple current-operated device. The electrode current in each phase was supplied from a current transformer to the coil of the regulator element. The pull of this coil was then balanced against a spring, and the operation of the regulator contacts caused the hoist motor to run in either direction to raise or lower the electrode as the current varied from some predetermined value. Figure 1 shows a typical arc furnace for steel melting. The hoist motors as shown are connected through a wire cable and drum sheave so as to lower and raise the electrode masts from which the electrodes are supported. This mechanical system is varied somewhat with different designs of furnace, but the electrical problem is simply one of automatic control, suitable for reversing and braking the electrode motor.

Current regulation was generally unsatisfactory when precise control was necessary, chiefly because the restraining force of the spring had a constant value. The variations of the current in the circuit are rapid at times, and the mechanical movement of the electrode is slow. Therefore, the position of the electrode responded not to instantaneous but to an average value of current variation. Furthermore, a three-phase system of currents in an electric furnace is complex. The furnace charge is a ground which connects through three continually shifting phase arcs to the potential supplied by the transformer. It is clear that the currents in the regulator elements are not independent but rather are interdependent; the value in any phase at a given time is determined by conditions in the other two phases, as well as by the length of the arc in that particular phase.

As is well known, the voltage drop across the arc will increase as the current is decreased and vice versa. In practice this characteristic can be modified by inserting additional reactance in the circuit. In fact, a sufficient value of reactance must be present in the circuit to maintain stability, so that the combination of arc resistance and circuit impedance will have a positive impedance characteristic over the operating zone. These theoretical requirements of the arc-furnace circuit have been frequently discussed in other articles on the subject and are well known. It should not be forgotten, however, that a considerable part of the circuit reactance in the case of the larger furnaces is in the secondary leads, and, therefore, such furnaces require no additional reactance for stable operation.

It is readily understood that, when the varying pull of the coil or other regulating device which is proportional to current in the electrode, is matched against a restraining force that is proportional to the voltage drop across the arc, the utmost sensitivity to changes in arc length is obtained in the regulator. While this sensitivity may not be so important when the furnace is reducing the cold scrap to a molten bath, it is of great importance when this molten bath is being refined.

The development of the art over a period of years showed that this principle of regulation was correct. It was natural that further development should look for improvements of the controlling scheme for the motor which for many years had been a constant-potential d-c, reversing control combined with dynamic braking.

Quite independently of the arc-furnace industry, however, new methods of regulation had been introduced in other industrial applications where regulated control of motors to adjust for speed, tension, rate of acceleration, and so forth, were desired. These methods for the

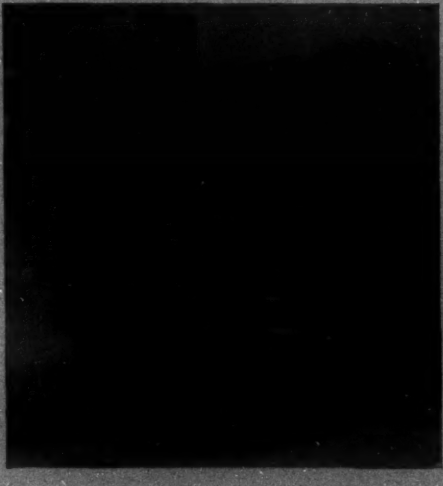

Figure 1. Representative arc furnace for steel melting

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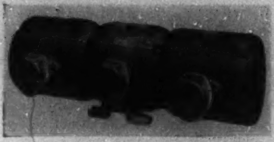

Figure 2. Typical unit including Rototrol, a-c motor, and d-c generator

most part were based on a different means of operating the motor in response to the regulating element. Instead of using a constant-potential control, adjustable-voltage control of the motors was used and has been found very satisfactory. An independent source of d-c voltage is required and is usually supplied by a motor generator set. If the exciter for this generator is supplied with suitable control fields, a complete regulating system is formed in which the regulating element is a part of the power supply for the electrode motors.

The Rototrol regulator was first applied to an automatic welding head in 1928. A few years later it was applied to the large elevators in Radio City and, since then, has found wide use in many industrial applications. It is the heart of the adjustable-voltage planer drive and has been proved in service over a period of several years. Other applications include paper-mill drives, electric shovels, mine sweepers, skip hoists, bomb spinners, and machine tools, some of which provide speed ranges as high as 120 to 1.

The rotating-type arc-furnace regulator was thus a natural development prompted by the use of similar regulators in other applications. It has been favorably received chiefly because the direct tie between the generator and the motor eliminates all contactors in the motor circuits.

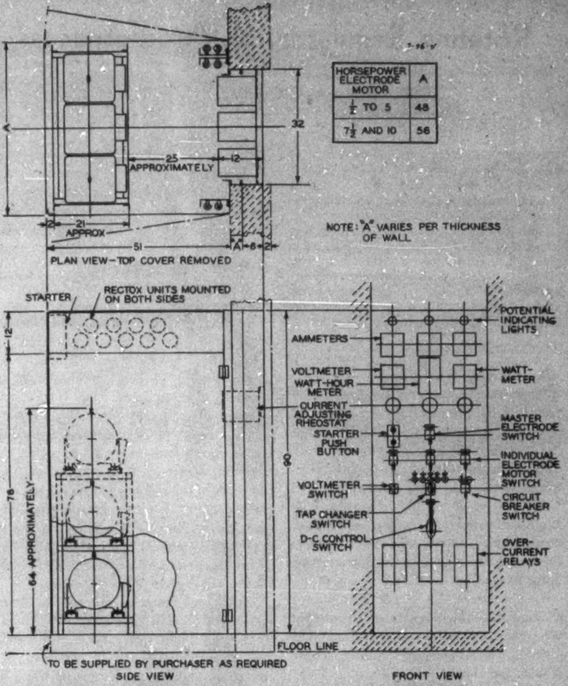
HORSEPOWER ELECTRODE MOTOR
A
½ TO 5
48
7½ AND 10
56
32
25 APPROXIMATELY
12
2
21 APPROX
51
A
6
2
PLAN VIEW—TOP COVER REMOVED
NOTE: "A" VARIES PER THICKNESS OF WALL
STARTER
RECTOX UNITS MOUNTED ON BOTH SIDES
12
78
64 APPROXIMATELY
90
FLOOR LINE
TO BE SUPPLIED BY PURCHASER AS REQUIRED
SIDE VIEW
AMMETERS
VOLTMETER
WATT-HOUR METER
CURRENT ADJUSTING RHEOSTAT
STARTER PUSH BUTTON
VOLTMETER SWITCH
TAP CHANGER SWITCH
D-C CONTROL SWITCH
POTENTIAL INDICATING LIGHTS
WATT-METER
MASTER ELECTRODE SWITCH
INDIVIDUAL ELECTRODE MOTOR SWITCH
CIRCUIT BREAKER SWITCH
OVER-CURRENT RELAYS
FRONT VIEW

Figure 4. Metal-enclosed Rototrol arc-furnace control and regulator unit

The Rototrol is essentially a small d-c machine similar in both construction and theory of operation to the standard d-c generator of like size. It is driven by a standard squirrel-cage induction motor as part of the motor generator set, Figure 2. The magnetic circuit is excited by a number of field windings, and the machine functions entirely through the interaction of these fields. The field coils constitute the input of the device, while the output is obtained from the armature circuit. The principal field windings include:

1. A control field which serves as a reference against which the quantity to be regulated is compared.
2. A control field which measures the quantity to be regulated.
3. A self-energizing field connected either in series or in shunt with the armature.

For a high degree of accuracy, the various control fields should have complete control over the regulating element without supplying any of the magnetomotive force necessary to generate power in its own output circuit. The self-energizing field circuit is designed so that its resistance line will be coincident with the air-gap line of the no-load saturation curve. This results in a high degree of amplification since an extremely small change of power in the control fields is ample to change the output over its full range.

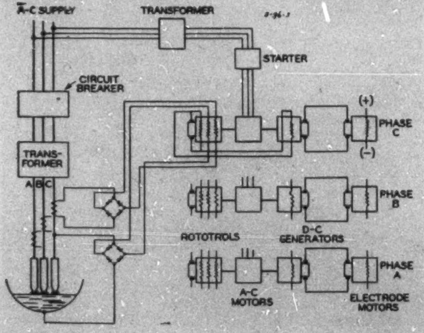
A-C SUPPLY
TRANSFORMER
8-96-3
STARTER
CIRCUIT BREAKER
TRANS-FORMER
A B C
(+)
PHASE C
(−)
PHASE B
ROTOTROLS
D-C GENERATORS
PHASE A
A-C MOTORS
ELECTRODE MOTORS

Figure 3. Elementary diagram of arc-furnace regulating system

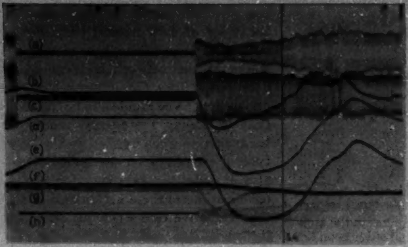

Figure 5. Oscillogram showing electrical performance of regulator during the melt-down period

(a). Electrode current to Rototrol control field
(b). Rototrol armature volts
(c). A-c volts to Rototrol control field
(d). Motor armature volts
(e). Motor speed
(f). Electrode position indicator
(g). Motor armature current
(h). Electrode current

This output is applied to the field of the constant-speed direct-connected generator, which supplies power to the electrode motors.

The speed of response of the system comprising the arc-furnace regulating element and the power-supplying generator must be carefully co-ordinated with the acceleration characteristics of the electrode motor in order to obtain the maximum rate of acceleration permissible without exceeding the commutating ability of the electrode motor. Acceleration of the electrode motor is influenced by the system inertia, the friction load, and the torque developed by the motor. For instance, a three-horsepower equipment with a system WR^2 at the motor shaft of 1.5 pound-feet squared, a friction load of two-thirds full-load torque and a maximum motor torque of 1.5 times rated will accelerate to rated speed in about one half of a second.

The speed of response of the regulating element depends on the ratio of inductance to resistance for each field, the mutual induction between the fields, and the relative ratios of turns and time constants of the various fields. It is particularly important that time-constant and relative-turn ratio of the self-energizing field be correctly proportioned. Assuming a three-field machine, the differential equation of load voltage e will be:

$$e=k_2i_2-k_1i_1+k_3i_3$$
$$=i_3r_3+L_3\frac{di_3}{dt}+M_{23}\frac{di_2}{dt}+M_{13}\frac{di_1}{dt}$$

where

k_1, k_2, k_3 are machine constants with various field turns.
i_1, i_2, i_3 are the respective field currents.
r_3 is the self-energizing field resistance.
L_3 is the self-energizing field inductance.
N_{23}, M_{13} are the mutual inductances between fields 2 and 3, and between fields 1 and 3.

By combining this with the equations for other field voltages, the differential equation of voltage build-up in the output circuit can be obtained. Solution of equations of this type are of the forms:

$$e=A_1\epsilon m_1t+A_2\epsilon m_2t+A_3\epsilon m_3t+B$$

where the A's, m's, and B are constants depending on L, R, and M.

By proper proportion of the field circuits, any reasonable speed of response as required for electrode control can be obtained. Optimum performance is obtained when the speed of response of the electrical system is co-ordinated with acceleration characteristics of the mechanical system.

Figure 3 shows an elementary diagram of the regulating system. The current control field of the regulating element is energized from a dry-type rectifier which is in turn connected to a current transformer in the electrode circuit. The potential control field is energized in a similar manner from the voltage between each electrode and the shell of the furnace. In practice, when the breaker in closed voltage is applied to the potential control field which causes the generator voltage to build up in the direction to lower the electrodes. When the first electrode strikes the metal, the voltage drops to zero causing the motor to stop. When a second electrode strikes the metal, a current flows causing the current control field to become energized which acts to raise the electrode thus establishing an arc. As the arc is lengthened, its voltage increases and the current decreases until a balance is established between the potential and current control fields. The third electrode is controlled in a similar manner.

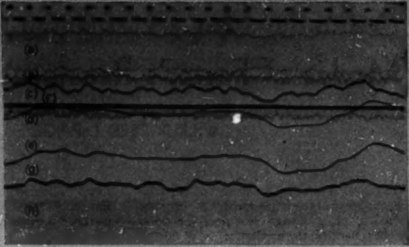

Figure 6. Oscillogram showing electrical performance of regulator during the refining period

(a). Electrode current to Rototrol control field
(b). Rototrol armature volts
(c). A-c volts to Rototrol control field
(d). Motor armature volts
(e). Motor speed
(f). Electrode position indicator
(g). Motor armature current
(h). Electrode current

As the charge melts and flows away, as the electrodes burn off, and as the scrap falls against the electrodes, the balance between the control fields is continually being disturbed. The Rototrol changes the generator voltage to position the electrodes to maintain the balance between arc voltage and current. This arrangement of the control field circuits corresponds to the accepted principle of regulation used successfully for many years in the balanced-beam type of regulator.

Provision is made for controlling the electrodes manually, either individually or in a group. This facilitates handling of the electrodes during charging, pouring, changing electrodes and so forth.

The wide range of current adjustment required is obtained by means of a rheostat shunting the current transformer. This method is used, since it maintains the same sensitivity for all current settings.

Figure 4 shows the arrangement of equipment in a single metal-enclosed unit. The front panel is arranged for setting into the wall of the furnace vault in the usual manner. Hinged side panels are used on the rear of the unit in order to provide maximum accessibility to the rotating equipment as well as to the panel mounted apparatus. A bolted rear plate is provided so that any one or all of the motor generator sets can be readily removed for inspection or maintenance. Enclosed machines with ball bearings are

A Method for Determining the Normal Modes of Foster's Reactance Networks

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Synopsis: A method is presented for finding the zeros of the impedance or admittance function of a Foster reactance network. More generally, the method is one of finding the zeros of a rational function having alternate zeros and poles, all of which are simple, if the poles are known; or of finding the poles if the zeros are known. The method consists of writing the function as a sum of partial fractions and replacing all but two of the fractions, depending on the zero to be found, by a constant and solving the resulting quadratic equation. In this way an upper and lower bound of a zero may be found, and, if need be, the method may be applied repeatedly to give a pair of bounds which will be as close to the required zero as necessary. The method has particular value if the rational function is the impedance or admittance function of one of Foster's reactance networks, since these functions are obtained directly from the networks as sums of partial fractions of the form $A_n/(B_n^2-\omega^2)$. Hence, the zeros may be obtained without reducing the function to the polynomial form. The new method may not always be shorter in application than other known methods, but its advantage lies in the facility with which an upper and lower bound of the required zero may be found and in the possibility of dealing with the partial-fraction form of the impedance or admittance function.

Statement of the Problem

THE two generalized networks of Figure 1 are Foster's canonic forms, which are of particular importance because either one is a possible representation of any reactance network.[1] They are forms in which the normal modes of oscillation are placed in evidence: for Figure 1a they coincide with the network meshes,[2] and for Figure 1b, which is the dual[3] of Figure 1a, the normal modes coincide with the network nodes. This means that if Figure 1a is excited by a voltage, or Figure 1b by a current, the response frequencies are merely those of the isolated sections. However, if the excitations of these networks are, respectively, current and voltage, they no longer have nodes or meshes which coincide with the normal modes. That is, the value of each natural frequency of oscillation depends on all the circuit constants of the network and cannot be determined from the properties of any one section. The problem to be treated is that of finding the normal modes for these circuits when exposed to the latter type of excitation. These solutions will also include the solutions for certain combinations of these networks, and also the dissipative case where all coils have the same R/L ratio and all capacitors have the same G/C ratio. The method will be illustrated in terms of the voltage excitation of Figure 1b, but with the application of the principles of duality[3] it applies also to the current excitation of Figure 1a. There are known methods for finding these normal modes, but under the special conditions of the circuits considered here the method to be introduced will provide good approximations with a minimum of effort.

Determination of the Normal Modes

On a mesh basis none of the possible meshes of Figure 1b satisfy the necessary conditions to make them coincide with the normal modes,[2] and if there are no such meshes the normal modes, regardless of the actual point of insertion of the disturbing voltage, are the values of p for which

$$Z(p)=0 \qquad (1)$$

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used exclusively to eliminate periodic oiling, also to keep abrasive dust out of bearings. Individual motor generator sets having rugged construction and ample safety factors are employed. Should a failure of any unpredictable kind occur, the heat can be continued with two electrodes on automatic control and a spare set placed in service quickly.

The equipment is made so far as possible in one unit so that the various parts can be protected from dirt and physical damage. This also permits factory assembly and complete test, up to the operation of the electrode motors. A minimum of external connections and erection time is thus required.

Figure 5 is an oscillogram showing typical operation of the furnace during the melt-down period. This demonstrates the high speed of the electrodes. Unsustained sudden variations in current cause no extra action or undue wear on the equipment, but sustained changes cause immediate response. It will be noted that the electrode can be withdrawn at relatively high speed when a cave-in takes place. In case of abnormal unbalance between current and voltage in the arc, the equipment is able to perform its function with the minimum number of outages.

The start of the film shows a loss of voltage and current. The motor comes to rest in about one half of a second and remains at rest until the arc is re-established. In this case, the current was low and the Rototrol voltage immediately started rising in a direction to cause the electrode to be lowered. The motor started one seventh of a second after the change was initiated. This delay was due to the electrical and mechanical inertia of the system. The motor was accelerated rapidly to full speed, and, as the current approached the regulated value, the motor speed was diminished. At point 5-*A* there was a marked rise in current and the electrode motor was decelerated to zero speed and accelerated to one-third speed in the raise direction. It should be appreciated that during the melt-down period, electrical conditions in the furnace change rapidly, necessitating more or less continual electrode adjustment. This film covers a period of about ten seconds.

Figure 6 is an oscillogram showing the refining period. Here, relatively small changes in current take place, and consequently only small movement of electrodes is required. Because of the inertia of the electrical system, sudden unsustained variations do not cause movement of the electrodes.

Operating experience has shown that the Rototrol regulator combines the necessary degree of sensitivity, amplification, speeds of response, and stability required for arc-furnace operation. This regulator is particularly successful during melt-down periods when cave-ins frequently occur because the high speed withdrawal of the electrodes prevents breaker tripping because of sustained overload.

where p is the usual generalized angular velocity and $Z(p)$ is the impedance function of the network.[4]

For the network in question,

$$Z(p)=p\left(L_0+\sum_{n=1}^{N}\frac{A_n}{B_n^2+p^2}\right) \quad (2)$$

where

$$\left.\begin{aligned} A_n&=1/C_n && \text{(a)}\\ B_n&=1/\sqrt{L_nC_n} && \text{(b)}\end{aligned}\right\} \quad (3)$$

When there is a series capacitor, as shown in Figure 1b, it is a special case of the parallel combination for which the corresponding $B=0$, and is therefore included in the summation.

For the reactance network the values of p which satisfy equation 1 are pure imaginaries, and accordingly we are interested only in purely imaginary values of p. Therefore, it is convenient to let

$$p=j\omega \quad (4)$$

and

$$Z(p)=Z(j\omega)=j\omega\left(L_0+\sum_{n=1}^{N}\frac{A_n}{B_n^2-\omega^2}\right) \quad (5)$$

It follows from the separation property of zeros and poles[1] of equation 5 that there will be a root of equation 1 between any two adjacent values of B_n. Let the values of B_n be arranged as an increasing sequence, with increasing subscripts, and choose the subscripts on the roots so that

$$B_1<\omega_1<B_2<\omega_2<\ldots<B_{k-1}<\omega_k<B_k<\ldots<\omega_N \quad (6)$$

All B's will be different because it is assumed that Figure 1b is reduced to its simplest form. The solution is obtained by solving

$$L_0+\phi(\omega)=0 \quad (7)$$

where

$$\phi(\omega)=\sum_{n=1}^{N}\frac{A_n}{B_n^2-\omega^2} \quad (8)$$

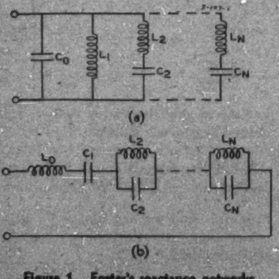

Figure 1. Foster's reactance networks

which contains all the roots except the trivial root $\omega=0$ which occurs if $B_1\neq0$ and which has been factored out.

This is made clearer by the typical plot of $\phi(\omega)$, as shown in Figure 2. The function closely resembles a reactance function, since the latter is $\omega\phi(\omega)$. The intersections of these curves with the horizontal line $-L_0$ are the required roots. They are located symmetrically about the vertical axis, and therefore, it is necessary to consider only the positive roots.

There are two procedures to be followed, depending on whether $1\leqq k\leqq N-1$ or $k=N$.

CASE 1. $1\leqq k\leqq N-1$

The root ω_k lies in the interval

$$B_k<\omega_k<B_{k+1} \quad (9)$$

and with this knowledge we can separate out the two terms of $\phi(\omega)$ which have poles nearest to ω_k and approximate the remaining terms by a term which will be a constant. Equation 7 becomes

$$\frac{A_k}{B_k^2-\omega^2}+\frac{A_{k+1}}{B_{k+1}^2-\omega^2}\equiv\psi(k,\omega)=W(k) \quad (10)$$

which serves to define $\psi(k,\omega)$, and where W is a constant. The solution of equation 10 is

$$\left.\begin{aligned}
&\left.\begin{aligned}\omega^2&=\frac{1}{2W}[F(k)+G(k)], && W\neq0\\ &=\frac{B_{k+1}^2A_k+B_k^2A_{k+1}}{A_{k+1}+A_k}, && W=0\end{aligned}\right\}1\leq k\leq N-1 && \begin{aligned}&\text{(a)}\\&\text{(b)}\end{aligned}\\
&\text{where}\\
&F(k)=W(B_{k+1}^2+B_k^2)-(A_{k+1}+A_k) && \text{(c)}\\
&\text{and}\\
&G(k)=\sqrt{(A_{k+1}+A_k)^2-2W(B_{k+1}^2-B_k^2)^2(A_{k+1}-A_k)+W^2(B_{k+1}^2-B_k^2)^2} && \text{(d)}
\end{aligned}\right| \quad (11)$$

The form of $\psi(k,\omega)$ is shown in Figure 3, and it is seen from this that there may be an extraneous root outside of the interval B_k to B_{k+1}. However, equation 11 is written in such a form as to always give a root within the required interval. In speaking of a root of equation 10 it will always be understood that it is the root given by equation 11. It is evident from the form of $\psi(k,\omega)$ that there will always be a root, regardless of the value of W. The problem is now one of finding suitable values of W. To do this we write equation 7 as

$$\left.\begin{aligned}&\psi(k,\omega)=S(k,\omega) && \text{(a)}\\ &\text{where}\\ &S(k,\omega)=-L_0-\sum_{\substack{n=1\\ n\neq k,\,k+1}}^{N}\frac{A_n}{B_n^2-\omega^2} && \text{(b)}\end{aligned}\right\} \quad (12)$$

$S(k,\omega)$ is shown in Figure 3, and it is apparent that in the interval between B_k and B_{k+1}, $\psi(k,\omega)$ is continuous and always increasing, and that $S(k,\omega)$ is continuous, always decreasing, and is bounded. Thus, if ω_p is any value, $B_k\leqq\omega_p\leqq B_{k+1}$, and we define

$$W=S(k,\omega_p) \quad (13)$$

there will be a solution of equation 10 which we will call ω_q and which will have the following properties:

$$\text{if}\ \left\{\begin{matrix}\omega_p>\omega_k\\ \omega_p=\omega_k\\ \omega_p<\omega_k\end{matrix}\right\}\ \text{then}\ \left\{\begin{matrix}\omega_q<\omega_k\\ \omega_q=\omega_k\\ \omega_q>\omega_k\end{matrix}\right\} \quad (14)$$

The properties of $\psi(k,\omega)$ and $S(k,\omega)$ also show that if $B_k\leqq\omega_p'<\omega_p\leqq B_{k+1}$, and ω_q' is the root corresponding to ω_p', as given by equations 11 and 13, then

$$B_k<\omega_q<\omega_q'<B_{k+1} \quad (15)$$

In view of these relations we will find it expedient to define

$$\left.\begin{aligned}W_1(k)&=S(k,B_k) && \text{(a)}\\ W_2(k)&=S(k,B_{k+1}) && \text{(b)}\end{aligned}\right\} \quad (16)$$

and let the corresponding roots of equation 10 be $\bar{\omega}_k$ and $\underline{\omega}_k$, respectively. From relations 9, 14, and 15 it follows that

$$B_k<\underline{\omega}_k<\omega_k<\bar{\omega}_k<B_{k+1} \quad (17)$$

and we have determined upper and lower bounds of ω_k which are closer than B_k and B_{k+1}. If this approximation is not sufficient, one may use $\underline{\omega}_k$ and $\bar{\omega}_k$ in place of B_k and B_{k+1}, respectively, and determine two new roots, $\bar{\omega}_k'$ and $\underline{\omega}_k'$.

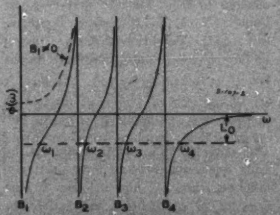

Figure 2. Plot of $\phi(\omega)$ versus ω to show the locations of the roots. Shown for $N=4$

This process may be repeated as many times as required. To be more specific, define

$$\left.\begin{array}{ll} W_1^{(s)}(k)=S[k,\underline{\omega}_k^{(s-1)}] & \text{(a)} \\ W_2^{(s)}(k)=S[k,\bar{\omega}_k^{(s-1)}] & \text{(b)} \end{array}\right\} s\geqq 1 \qquad (18)$$

and let $\bar{\omega}_k^{(s)}$ and $\underline{\omega}_k^{(s)}$ be the roots of equation 10 using equations 18*a* and *b*, respectively, for W. Consider the case $s=1$. From relations 15 and 17 it follows that $\bar{\omega}_k'<\bar{\omega}_k$ and $\underline{\omega}_k'>\underline{\omega}_k$, and from relations 14 and 17 we obtain the relations $\bar{\omega}_k'>\omega_k$ and $\underline{\omega}_k'<\omega_k$. Hence,

$$B_k<\underline{\omega}_k<\underline{\omega}_k'<\omega_k<\bar{\omega}_k'<\bar{\omega}_k<B_{k+1} \qquad (19)$$

It is obvious that this reasoning may be continued, leading to the pair of sequences

$$\left.\begin{array}{l} \bar{\omega}_k>\bar{\omega}_k'>\bar{\omega}_k''>\ldots>\bar{\omega}_k^{(s-1)}>\bar{\omega}_k^{(s)}>\ldots>\omega_k \\ \underline{\omega}_k<\underline{\omega}_k'<\underline{\omega}_k''<\ldots<\underline{\omega}_k^{(s-1)}<\underline{\omega}_k^{(s)}<\ldots<\omega_k \end{array}\right\} \qquad (20)$$

Observe that relation 20 contains twice as many values as required to obtain a given approximation. For example, if $\bar{\omega}_k$ is found one can immediately find $\underline{\omega}_k'$, and then $\bar{\omega}_k''$, and so forth. Similarly, $\underline{\omega}_k$ could be used as the starting point, and thus, either of the following pairs of sequences may be used:

$$\left.\begin{array}{l} \left.\begin{array}{l} \bar{\omega}_k>\bar{\omega}_k''>\ldots>\bar{\omega}_k^{(s-2)}>\bar{\omega}_k^{(s)}> \\ \qquad\qquad\ldots>\omega_k \\ \underline{\omega}_k'<\underline{\omega}_k'''<\ldots<\underline{\omega}_k^{(s-1)}<\underline{\omega}_k^{(s+1)}< \\ \qquad\qquad\ldots<\omega_k \end{array}\right\} \text{(a)} \\ \text{or} \\ \left.\begin{array}{l} \bar{\omega}_k'>\bar{\omega}_k'''>\ldots>\bar{\omega}_k^{(s-1)}>\bar{\omega}_k^{(s+1)}> \\ \qquad\qquad\ldots>\omega_k \\ \underline{\omega}_k<\underline{\omega}_k''<\ldots<\underline{\omega}_k^{(s-2)}<\underline{\omega}_k^{(s)}< \\ \qquad\qquad\ldots<\omega_k \end{array}\right\} \text{(b)} \end{array}\right\} \qquad (21)$$

CASE 2. $k=N$

This case requires special treatment because there is no pole of $\phi(\omega)$ beyond B_n, and ω_N is in the range

$$B_N<\omega_N<\infty \qquad (22)$$

In contrast to the previous practice, it is not possible to keep two terms, having poles on opposite sides of ω_N, in their exact form and replace the remainder of $\phi(\omega)$ by an approximation. One could, therefore, keep only one term, $A_N\div(B_N^2-\omega^2)$, as the exact term. However, since the solution can be given explicitly if two exact terms are used, a more accurate approximation will be obtained by also using the term $A_{N-1}/(B_{N-1}^2-\omega^2)$.

One is lead to attempt to approximate the other terms by a constant, as was done in case 1. That is, we will seek a suitable value of W to make the solution of

$$\psi(N-1,\omega)=W(N) \qquad (23)$$

approximate ω_N. $\psi(N-1,\omega)$ is shown in Figure 4 from which it is seen that for equation 23 to have a solution it is necessary that $W<0$. Assuming this to be true the solution is

$$\left.\begin{array}{ll} \omega^2=\dfrac{1}{2W}[F(N)-G(N)],\quad W<0 & \text{(a)} \\ \text{where} & \\ F(N)=W(B_N^2+B_{N-1}^2)- & \\ \qquad\qquad (A_N+A_{N-1}) & \text{(b)} \\ \text{and} & \\ G(N)=\sqrt{\begin{array}{l}(A_N+A_{N-1})^2-2W(B_N^2- \\ B_{N-1}^2)^2(A_N-A_{N-1})+ \\ \qquad W^2(B_N^2-B_{N-1}^2)^2\end{array}} & \text{(c)} \end{array}\right\} \qquad (24)$$

This is written in such a way as to give the solution which is greater than B_N. The root given by equation 24 will henceforth be spoken of as the root of equation 23. Continuing, as for case 1, we write equation 7 as

$$\psi(N-1,\omega)=S(N-1,\omega) \qquad (25)$$

and examine $S(N-1,\omega)$ in the interval of ω for which it may serve as a value of W. For $\omega>B_N$, $S(N-1,\omega)$ is continuous and always decreasing, with increasing ω, and becomes less than zero at some point, ω_r, as indicated in Figure 4. This point may lie on either side of B_N. If ω_s is the larger of ω_r and B_N, and if W is defined as

$$W=S(N-1,\omega_t) \qquad (26)$$

where $\omega_s<\omega_t<\infty$, then there will be a solution of equation 23 for this value of W. Let this solution be ω_u. From the properties of $\psi(N-1,\omega)$ and $S(N-1,\omega)$ one may conclude that

$$\text{if}\ \left\{\begin{array}{l}\omega_t>\omega_N\\ \omega_t=\omega_N\\ \omega_t<\omega_N\end{array}\right\}\ \text{then}\ \left\{\begin{array}{l}\omega_u<\omega_N\\ \omega_u=\omega_N\\ \omega_u>\omega_N\end{array}\right\} \qquad (27)$$

and if $\omega_s\leqq\omega_t'<\omega_t\leqq\infty$ and ω_u' is the root corresponding to ω_t', as given by equations 26 and 24, then

$$\omega_s<\omega_u<\omega_u'<\infty \qquad (28)$$

The procedure to be used in obtaining a solution depends on the position of ω_r relative to B_N. For the first case, assume $S(N-1,B_N)<0$, which is equivalent to the condition $\omega_r<B_N$. This is similar to case 1 and accordingly we define

$$\left.\begin{array}{ll} W_1(N)=S(N-1,B_N) & \text{(a)} \\ W_2(N)=S(N-1,\infty)=-L_0 & \text{(b)} \end{array}\right\} \qquad (29)$$

The corresponding roots obtained by using equations 29*a* and *b*, respectively, will be $\bar{\omega}_N$ and $\underline{\omega}_N$, and in view of relations 22, 27, and 28 they will satisfy the inequality

$$B_N<\underline{\omega}_N<\omega_N<\bar{\omega}_N \qquad (30)$$

Closer approximations may be obtained, exactly as in case 1, by defining

$$\left.\begin{array}{ll} W_1^{(s)}(N)=S[N-1,\underline{\omega}_N^{(s-1)}] & \text{(a)} \\ W_2^{(s)}(N)=S[N-1,\bar{\omega}_N^{(s-1)}] & \text{(b)} \end{array}\right\} s\geqq 1 \qquad (31)$$

and $\bar{\omega}_N^{(s)}$ and $\underline{\omega}_N^{(s)}$ as the roots of equation 23 using, respectively, equations 31*a* and *b* for W. For $s=1$, in consequence of relations 28 and 30, we have $\bar{\omega}_N'<\bar{\omega}_N$ and $\underline{\omega}_N'>\underline{\omega}_N$ and from relations 27 and 30, $\bar{\omega}_N'>\omega_N$ and $\underline{\omega}_N'<\omega_N$. Therefore we may write the inequality

$$B_N<\underline{\omega}_N<\underline{\omega}_N'<\omega_N<\bar{\omega}_N'<\bar{\omega}_N \qquad (32)$$

and by extending this repeatedly there results the pair of sequences,

$$\left.\begin{array}{l} \bar{\omega}_N>\bar{\omega}_N'>\bar{\omega}_N''>\ldots>\bar{\omega}_N^{(s-1)}>\bar{\omega}_N^{(s)}> \\ \qquad\qquad\ldots>\omega_N \\ \underline{\omega}_N<\underline{\omega}_N'<\underline{\omega}_N''<\ldots<\underline{\omega}_N^{(s-1)}<\underline{\omega}_N^{(s)}< \\ \qquad\qquad\ldots<\omega_N \end{array}\right\} \qquad (33)$$

from which either of the two following pairs may be selected for computation:

$$\left.\begin{array}{l} \left.\begin{array}{l} \bar{\omega}_N>\bar{\omega}_N''> \\ \ldots>\bar{\omega}_N^{(s-2)}>\bar{\omega}_N^{(s)}>\ldots>\omega_N \\ \underline{\omega}_N'<\underline{\omega}_N'''< \\ \quad\ldots<\underline{\omega}_N^{(s-1)}<\underline{\omega}_N^{(s+1)}< \\ \qquad\qquad\ldots<\omega_N \end{array}\right\} \text{(a)} \\ \left.\begin{array}{l} \bar{\omega}_N'>\bar{\omega}_N'''> \\ \quad\ldots>\bar{\omega}_N^{(s-1)}>\bar{\omega}_N^{(s+1)}> \\ \qquad\qquad\ldots>\omega_N \\ \underline{\omega}_N<\underline{\omega}_N''< \\ \quad\ldots<\underline{\omega}_N^{(s-2)}<\underline{\omega}_N^{(s)}<\ldots<\omega_N \end{array}\right\} \text{(b)} \end{array}\right\} \qquad (34)$$

With the introduction of the sequences 34*b* there is some relaxing of the original condition, that $W_1(N)<0$. To use these sequences it is necessary only to have $W_1'(N)<0$, which is equivalent to $\underline{\omega}_N>\omega_r$.

If $W_1'(N)\geqq 0$, an upper bound of ω_N may be found by another method which consists of obtaining a root of

$$\psi(N-1,\omega)=-L_0-\sum_{n=1}^{N-2}\frac{A_n}{B_{N-1}^2-\omega^2} \qquad (35)$$

It is easy to see that equation 35 has a root, by writing it as

$$\left.\begin{array}{ll} \dfrac{A_{N-1}'}{B_{N-1}^2-\omega^2}+\dfrac{A_N}{B_N^2-\omega^2}=-L_0 & \text{(a)} \\ \text{where} & \\ A_{N-1}'=\displaystyle\sum_{n=1}^{N-1}A_n & \text{(b)} \end{array}\right\} \qquad (36)$$

Let this root be designated by $\bar{\omega}_N^*$, and observe that, if $\omega>B_N$, then

$$-\sum_{n=1}^{N-2}\frac{A_n}{B_{N-1}^2-\omega^2}>-\sum_{n=1}^{N-2}\frac{A_n}{B_n^2-\omega^2} \qquad (37)$$

From this, and the fact that $\psi(N-1,\omega)$ is increasing with increasing ω, it may be concluded that

$$\bar{\omega}_N^*>\omega_N \qquad (38)$$

Thus $\bar{\omega}_N^*$, an upper bound of ω_N, has been found by a method which is applicable in all cases.

It is of interest to note, parenthetically, that if $W_1'(N)<0$ then $\bar{\omega}_N^*$ and $\bar{\omega}_N'$ can both be found, and if there is much difference between them it may be worth the extra computation required to know which is the closer to ω_N and therefore which one may be most profitably used as the basis for future computations of the sequences 34. It is possible to know which is the better approximation by computing $\bar{\omega}_N^*$ and then either also computing ω_N' and comparing directly with $\bar{\omega}_N^*$, or comparing $S(N-1, \varphi_N)$ with

$$-L_0-\sum_{n=1}^{N-1} A_n/(B_{N-1}^2-\bar{\omega}_N^{*2})$$

If the former is larger, then $\bar{\omega}_N'>\bar{\omega}_N^*$, and vice versa.

To return to the case where $W_1'(N)\geqq 0$, $\bar{\omega}_N^*$ having been found, it may be used to define

$$W_2^*(N)=S(N-1,\bar{\omega}_N^*) \tag{39}$$

If the root of equation 23, when $W_2^*(N)$ is used, is φ_N^*, it follows from relations 27 and 38 that $\varphi_N^*<\omega_N$. Also, $S(N-1, \bar{\omega}_N^*)>S(N-1,\infty)$ and therefore, from the increasing property of $\psi(N-1,\omega)$,

$$\varphi_N<\varphi_N^*<\omega_N \tag{40}$$

Although $\varphi_N<\omega_r$ it is possible that $\varphi_N^*>\omega_r$, in which event φ_N^* is a possible starting point for the sequences 34, as a replacement for either B_N or φ_N. Whether this condition is fulfilled may be checked by observing if $S(N-1, \omega_N^*)$ is less than zero, this being the necessary and sufficient condition to make $\varphi_N^*>\omega_r$.

If this attempt at finding an improved upper bound, by first seeking a lower bound which is greater than ω_r, is not successful, the following procedure may be used. Arbitrarily choose a value, ω_N^*, such that

$$\left.\begin{array}{ll}\varphi_N^*<\omega_N^*<\bar{\omega}_N^* & \text{(a)}\\ \text{and} & \\ S(N-1,\omega_N^*)<0 & \text{(b)}\end{array}\right\} \tag{41}$$

If ω_N^* is less than ω_N, it may be used as B_N or φ_N in forming the sequences 34. To find whether this is true use $S(N-1, \omega_N^*)$ as W in equation 23 and call the resulting root $\omega_N^{*\prime}$. From relation 27, if

$$\left.\begin{array}{ll}\omega_N^{*\prime}>\omega_N^* & \text{(a)}\\ \text{then} & \\ \omega_N^*<\omega_N & \text{(b)}\end{array}\right\} \tag{42}$$

Also, from relation 27, if

$$\left.\begin{array}{ll}\omega_N^{*\prime}<\omega_N^* & \text{(a)}\\ \text{then} & \\ \omega_N^{*\prime}<\omega_N & \text{(b)}\\ \text{and if} & \\ S(N-1,\omega_N^{*\prime})<0 & \text{(c)}\end{array}\right\} \tag{43}$$

$\omega_N^{*\prime}$ may be used as B_N or ω_N in relation 34. Thus, there are two possibilities: if relation 42*a* is true the search for a lower bound is completed, and the same conclusion follows if relation 43*a* and *c* are both true. However, if relation 43*a* is true but 43*c* is not true then another, smaller, value will have to be assumed for ω_N^*.

Application to Other Circuits

In Figure 5 two circuits are shown, to which this method of determining the normal modes is applicable. Consider Figure 5b, with a current excitation, a voltage excitation being no different than the previous problem and of no further interest. The normal modes will be the values of p which make

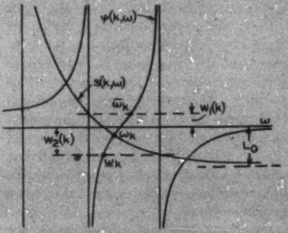

Figure 3. Properties of the functions involved in finding all the roots except the largest

$$Y(p)=0 \tag{44}$$

From the form of the circuit it is apparent that

$$Y(p)=\frac{Z_A(p)+Z_B(p)}{Z_A(p)Z_B(p)} \tag{45}$$

and hence, if p_k is a root of

$$Z_A(p)+Z_B(p)=0 \tag{46}$$

it is also a root of equation 44 unless $Z_A(p_k)=Z_B(p_k)=0$. The roots of equation 46 can be found by the method which has been presented. Likewise, for Figure 5a the normal modes are the roots of

$$Y_A(p)+Y_B(p)=0 \tag{47}$$

and, as has been indicated, the formal solution of equation 47 is the same as the solution of equation 46.

All of these networks can be treated equally well if all coils have the same R/L ratio and all capacitors the same G/C ratio. The general case of this is treated by Guillemin[5] who shows that if

$$\alpha=R/2L \quad \text{and} \quad \beta=G/2C \tag{48}$$

then, for roots of $Z(p)=0$, we obtain

$$\left.\begin{array}{ll}p_k=-(\alpha+\beta)\pm j\omega_k & \text{(a)}\\ \text{where} & \\ \omega_k=\sqrt{g_k^2-(\alpha-\beta)^2} & \text{(b)}\end{array}\right\} \tag{49}$$

and where g_k is the value that p_k would have if there were no dissipation.

Application to Other Rational Functions

An impedance or admittance function of a reactance network is a special rational function which has everywhere a positive slope and, consequently, alternating poles and zeros all of which are simple. The poles come in pairs located symmetrically about the point $\omega=0$, and the residue at one of the poles is the negative of the residue at its mate. This makes it possible, for such a function, to combine the terms representing the two poles of a pair into one term and only treat the function for $\omega>0$. The fact that the zeros come in pairs, located symmetrically about the point $\omega=0$, is indicated by equations 10 and 24 because there the zeros are given in terms of ω^2. In the general case of a rational function having alternate poles and zeros, all of which are simple, the same procedure may be used to find upper and lower bounds of the zeros, but without the possibility of combining terms. If the poles and residues are known the approximate zero will be a solution of an equation of the form

$$\frac{A_k}{B_k-\omega}+\frac{A_{k+1}}{B_{k+1}-\omega}=W \tag{50}$$

where W is defined in similarity with equations 12 and 13 but where the summation must be over all the poles except B_k and B_{k+1}. Equation 50 is a quadratic in ω, and its solution is given by equations 11, but with ω^2 replaced by ω. The treatment for the greatest and smallest zeros will be similar to that given under case 2, with the aforementioned modifications.

It should be pointed out that it is necessary to know only the locations of the poles since the residues may be found by explicit methods.[4] Also, if the locations of the zeros of a function are known, the poles may be found by applying the method to the reciprocal of the function.

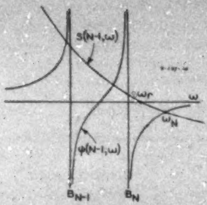

Figure 4. Properties of the functions involved in finding the largest root

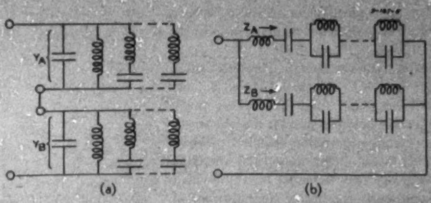

Figure 5. Combinations of the reactance networks to which the method is applicable

Therefore, this method may be used to find the zeros or poles, provided that the poles or zeros, respectively, are known.

Conclusions

In finding the zeros of a rational function by the method presented, one finds a sequence of approximate values, each approximate value being obtained from the previous one. The work involved in finding an approximate value is about the same as in substituting a trial value in the original function, because the evaluation of W requires the substitution of the previous approximate value in all but two of the partial fraction terms which constitute the function. Therefore, probably the greatest advantage of the new method is in the possibility of obtaining, with only two sets of computations, a pair of reasonably close approximate values between which the exact zero is known to be located. In many cases the sequences will converge so rapidly that only two terms need be computed, even for good accuracy. If it is necessary to compute more terms of the sequences to improve the accuracy, this method may have less advantage over the trial method, especially if the initial trials should be close to the zeros to be found. However, the new method does not depend on initial chance assumptions, and it is probable that it will at least yield a solution as quickly, on the average, as the trial method, and perhaps much more quickly. There may be some doubt in the reader's mind as to whether these comments apply to the extreme zeros because of the possible lengthened procedure in this case, which may even require a trial substitution. However, the result of the substitution of this trial value is used directly to find a closer approximation, and also, since a lower and upper bound may always be found by explicit means, the range of choice of the trial value is restricted to the interval between these bounds. This possible increased difficulty in finding these zeros is not without its counterpart in the trial method, because the range of choice of trial values extends to infinity, and hence the probability of a good choice is more remote than for the other more restricted zeros.

Of course, the trial method is not the only alternative for finding the zeros of a rational function. They may be obtained by converting the function to the polynomial form and using a method which is applicable to a polynomial, such as Horner's or Graffe's method, or a method suggested by Higgins.[7] However, if the rational function is the impedance or admittance function for the circuits considered here, it will automatically come in the partial-fraction form, and, if there is a large number of terms in the partial-fraction expression, there may be considerable work required to reduce it to the polynomial form. In this case the new method may be preferable, depending on how many of the total number of roots are to be found. This is because the reduction to the polynomial form need be carried out only once, and the work required to find N zeros is therefore less than N times the work required to find the first zero; whereas in the partial-fraction method the work required to find each zero is approximately the same. Hence, the smaller the number of zeros to be found the greater will be the advantage of the partial-fraction method.

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Load Pickup by a Group of Ignitron Rectifiers

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Synopsis: One of the advantages of the application of mercury-arc rectifiers to large electrochemical loads, such as aluminum cell lines, is that, if all the rectifiers are excited simultaneously, they will pick up and share the load together. This eliminates need for large d-c cell-line circuit breakers.

The conventional method of starting simultaneously several ignitron mercury-arc rectifiers, widely used in large electrochemical plants, has been to energize the individual ignitor firing circuits from a master starting switch. Differences in closing time of the control contactors were reflected in proportional irregularity of load pickup between the ignitron rectifiers.

Recent tests, described in this paper, show that the grids usually employed in the ignitrons for anode shielding can be used as an alternative method to energize cell lines. If all the grids are biased negatively, they will prevent current flowing from the rectifiers while the firing circuits are all energized and the switches closed in the power circuits. Then, by operating one switch or a plurality of accurately timed switches, all grids can be made positive at the same instant with the result that all the rectifiers pick up load at the same instant and share it equally.

MANY of the phenomena of the behavior of large rectifier installations, during the instant of applying or dropping electrochemical loads, have been little understood or investigated by engineers. Certain field tests were made in large aluminum reduction plants, the results of which add to the engineering knowledge relating to this subject. Technical advance in the ever-expanding electrochemical field, even though it involves simply a better understanding of the operation of the electrical apparatus in the plant, is important to the engineer. An analysis of the problem and a report of the field tests are contained in this paper.

Among the big five electrochemicals, aluminum, magnesium, chlorine, copper, and zinc, some of the pioneering work and operating experience in connection with aluminum plants stands out because of the tremendous increase in tonnage of aluminum which industry has demanded, particularly for the airplane program. The electrical apparatus required in the reduction of alumina to pure aluminum in the electrolytic cell lines is, however, typical in a great many respects of what is required for the other electrochemicals.

Arrangement of Ignitron Rectifiers

For purposes of this paper, it is necessary to review briefly the heavy electrical equipment involved in a typical large rectifier installation, such as is used for the reduction of aluminum.[1–4] Figure 1 shows a typical one-line power diagram for an aluminum installation. Conversion apparatus for one cell line, for instance, is rated 38,700 kw, 645 volts, 60,000 amperes direct current. The individual electrolytic cells are all connected in series, and each carries the full cell-line current, which under normal conditions may range as high as 50,000 to 55,000 amperes. Since it is impractical to build a power rectifier in a single unit of such large capacity, the conversion apparatus must be broken up into several units. So far, in the United States it has been found impractical to build rectifier transformers in the 600-volt d-c output level larger than 10,000 amperes direct current. Likewise, the largest single rectifier assembly in the 600-volt d-c level is approximately 5,000 to 6,000 amperes direct current. For these reasons, the typical 60,000-ampere rectifier installation is, therefore, arranged as shown in Figure 1 with six separate transformers, each feeding two ignitron-rectifier assemblies.

The fact that single large rectifier installations of this sort must be broken up into several units, however, has its advantages. Since there usually is provided a small amount of extra rectifier and transformer capacity, the scheme is flexible from the maintenance standpoint. For instance, it is usually possible to maintain full current input and, consequently, full production when one unit consisting of a transformer and two rectifier assemblies are out of service for maintenance. For the latter reason, and in order to provide protection from faults and arc-back, power circuit breakers and isolating disconnect switches always are provided.

Earlier large electrochemical cell lines, which were placed in operation before the successful development of mercury-arc rectifiers, were fed from several motor-generators or synchronous converters.[5–7] Since the largest ratings in rotating conversion apparatus compare with the present maximum ratings of single assemblies of power-rectifier equipment, earlier arrangements for connecting the several units to the main d-c bus were very similar to current practice.

Problem of Energizing Cell Line

The method used for energizing large electrolytic cell lines from a battery of several generators or rotary converters has a bearing on the subject of this paper. It was first necessary to bring the generators or converters up to speed. Next, their d-c output voltages were equalized. Since it always has been impossible to build high accuracy in the closing time of d-c air circuit breakers, it was necessary to connect all the separate converting units to the main d-c substation bus before the cell-line load was connected to the bus. This requirement existed because the first machine, whose d-c breaker closed, tended to pick up the complete cell-line load. Since the cell-line load is many times the individual rating of the conversion units, its d-c air circuit breaker would immediately trip on overload. The d-c circuit breakers on the rest of the machines also would clear instantly as they

Table I

Location	Test Numbers	Time After Master Starting Switch Was Closed Before the First Rectifier Started	Maximum Difference in Pickup Time Between First and Last Rectifier Starting
Plant *A*	1	4 cycles	$1^3/_4$ cycles
Plant *A*	2	4 cycles	$2^1/_8$ cycles
Plant *A*	3	$3^1/_4$ cycles	$1^3/_8$ cycles
Plant *A*	4	$3^1/_2$ cycles	$^3/_4$ cycle
Plant *A*	5	$4^1/_4$ cycles	$1^1/_4$ cycles
Plant *B*	1	$4^1/_2$ cycles	$1^1/_2$ cycles
Plant *C*	1	6 cycles	$1^3/_4$ cycles

In the interval between making plant *A* test 2 and plant *A* test 3, the contactor gap spacings on all 12 ignitor-firing-circuit contactors were readjusted to determine the effects of different contactor closing time on the performance of the various rectifiers. This resulted in changing three factors. First, the time was shortened for picking up the first rectifier. Second, the maximum difference in pickup time between first and last rectifier to start was decreased. Finally, the identity of the first rectifier to pick up load was changed.

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closed in succession. For these reasons the method has never been employed.

As an alternative method, a main d-c circuit breaker, capable of closing in on and carrying the full-load line current, was always employed with rotating converting equipment. This latter breaker was closed only after all the individual units had been connected to the station bus and the full capacity required to carry the load continuously was available.

Obviously, the elimination of this expensive circuit breaker designed to carry and rupture these large and highly inductive loads is desirable.

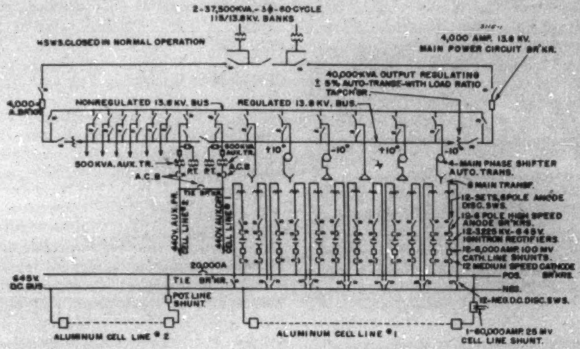

2-37,500KVA.-3Φ-60CYCLE
115/13.8KV. BANKS
4SWS.CLOSED IN NORMAL OPERATION
4,000 AMP. 13.8 KV.
MAIN POWER CIRCUIT BR'KR.
40,000KVA. OUTPUT REGULATING
± 5% AUTO-TRANSF.-WITH LOAD RATIO
TAPCH'GR.
4,000-A.BR'KR.
NONREGULATED 13.8KV. BUS
REGULATED 13.8KV. BUS.
500KVA.
AUX. TR.
+10°
-10°
+10°
-10°
500KVA. AUX. TR.
P.T.
P.T.
A.C.B
A.C.B
4-MAIN PHASE SHIFTER
AUTO. TRANS.
8 MAIN TRANSF.
TIE BR'KR.
440V. AUX. PR. CELL LINE #2
440V. AUX. CELL LINE #1
12-SETS,6POLE ANODE DISC. SWS.
12-6 POLE HIGH SPEED ANODE BR'KRS.
12-3225 KV.-645V. IGNITRON RECTIFIERS
12-6,000 AMP. 100 MV CATH. LINE SHUNTS.
12 MEDIUM SPEED CATHODE BR'KRS.
20,000A
TIE BR'KR.
645V. DC. BUS
POS.
NEG.
POT. LINE SHUNT.
12-NEG. D.C. DISC. SWS.
ALUMINUM CELL LINE # 2
ALUMINUM CELL LINE #1
1-60,000AMP. 25 MV
CELL LINE SHUNT.

Figure 1. Typical one-line power diagram for cell lines, Aluminum Company of America

One of the advantages of mercury-arc rectifiers over rotating equipment is their ability to pick up electronically such large loads after all the power switching has been completed. There are two general ways of accomplishing this:

1. By controlling the arc in the rectifier with grids.
2. By controlling it with ignitors.

Load Application by Ignitor Control

A widely used method of energizing large cell-line loads supplied by ignitron rectifiers has been to close the a-c and d-c power circuit breakers before exciting the ignitors in the cathode mercury. This means that no current flows from the rectifier, even though all the power circuits are closed, because the ignitors have not caused cathode spots. Magnetic-impulse firing circuits are used widely in such electrochemical service.[8,9] Most magnetic-impulse firing circuits commercially available start generating impulse voltages almost instantly after the contactors in their supply lines are closed and suitable a-c voltage is applied. Advantage has been taken of this performance of the ignitor firing circuits for starting all the rectifiers in a substation at once in order to pick up the large cell-line current.

Where as many as 12 ignitron rectifiers feed a common d-c bus, a master a-c excitation circuit has been used. This has consisted of a 12-pole relay or the equivalent. Each of the 12 poles is connected to the closing coil of a three-pole contactor in the a-c supply circuit to the firing equipment of each of the rectifiers. A single-pole master control switch located in the coil circuit of the 12-pole relays can, therefore, be closed to pick up all of the 12 separate a-c contactors in the supply circuits to the firing equipments.

The procedure, therefore, in picking up ignitron-fed cell-line loads after outages has been rather simple. First, the master excitation switch is opened, which releases all the a-c contactors in the individual firing-circuit supplies. Under this condition, none of the ignitors in any of the rectifiers can create cathode spots to cause current to flow. Next, all of the a-c and d-c power circuit breakers and switches are closed. So far no current flows. The final step then is to close the master a-c excitation circuit energizing all of the separate ignitor firing circuits.

Limitations of This Widely Used Starting Method

The master a-c excitation method of picking up large electrochemical loads fed from ignitron rectifiers has been used for years. As operating experience was gained, it appeared, however, that there were certain limitations. Outages have occurred occasionally because of loss of main a-c supply voltage. Sometimes, when the cell lines were re-energized, one or more of the cathode breakers would trip out from overcurrent. There appeared to be two possible causes for this action:

1. Standard commercial a-c contactors (three-pole) were used in the supply circuits to the individual ignitor firing equipments. While the pickup time of these contactors for most applications is satisfactorily uniform, it was realized that some disparity must exist between the various contactors in closing action. Since cell lines have inductance, it had been anticipated by the application engineers first dealing with the problem that the rate of rise of the cell-line current would be low enough to permit such inequalities in contactor closing time as might appear. Whether this assumption was correct now seemed open to some doubt until tests could be made to substantiate it under field conditions.
2. Resistance characteristics of ignitor firing points are different when the ignitors and liquid mercury in the cathode pools are cold from what they are during operation when the ignitors and mercury are hot. It appeared possible that when the ignitors were cold, such as is the case after a prolonged shutdown, there might be some delay in creating cathode spots in the ignitron rectifiers after the ignitor firing circuits were energized, until the ignitors warmed up and their resistance characteristics improved.

Grid Starting Method

A careful study of the performance of the rectifiers and ignitor firing circuits, was, of course, indicated under the circumstances. It was obvious that satisfactory data could be obtained only under actual field conditions. In anticipation that one or the other, or both, of the foregoing limitations would prove true, an alternative method for starting large ignitron installations feeding electrochemical loads was designed.

Use of Anode Baffle for Grid Control

All large pumped ignitron rectifiers employ a device which in principle is somewhat analogous to the grid in a multianode-type rectifier, but which usually has been called an anode baffle or shield when used in ignitrons. Figure 2 shows the cross section of a single-anode ignitron assembly. Please note the structure surrounding the anode which is called the deionizing anode baffle. This baffle, which is constructed of graphite and insulated from the metal vacuum-tight envelope, is energized through the grid seal. Its function in the ignitron-type rectifier has been to shield the main anode, thereby preventing or minimizing the occurrence of arc-backs. The fact that it was available, however, suggested that it might be used as another means of controlling the arc.

Factory tests were made to determine whether, with the right negative-bias voltage supplied to these grids, as they should be called when used for such purposes, sufficient control could be obtained to prevent current flowing from the anodes even though the ignitors were energized and firing. These tests indicated that there was enough grid action in the present-type ignitron-insulated anode baffle to prevent current flowing, when the baffles were biased negative with respect to the cathode by 300 volts, under expected conditions of operating temperature and positive main-anode voltages likely to be encountered in this field of application. Satisfactory blocking control could be obtained at this negative-bias level with as many as 500 ohms in series with the biasing circuit. This meant that a practical scheme for grid blocking the main arc in the rectifier could be developed.

Therefore, a cell-line starting circuit based on grid-blocking action in ignitron rectifiers was built. Figure 3 shows the schematic arrangement of a typical grid-bias starting circuit.

Procedure for Energizing Cell Line by Grid Blocking

A procedure for re-energizing a large ignitron rectifier installation after an outage using the Figure 3 circuit is as follows:

1. The 350-volt d-c bias rectifier is energized by closing the fused knife switch and contactor *A* in its supply circuit. In normal operation, the station operator energizes it by closing switch C_1 which is located on his master operating panel.

2. When operator's switch C_2 is closed, all *31S* relays close their contacts, and all the *31SX* relays open their contacts. This action then applies negative 350-volt d-c bias between the cathodes of all the rectifiers and the grids. It is now impossible for any of the rectifiers to carry current so long as this negative bias is maintained.

3. All power circuit breakers and disconnect switches are next closed connecting the rectifiers to both a-c supply voltage and the main d-c station bus.

4. The operator's master a-c excitation switch is then closed, which energizes all the individual firing circuits and causes all the ignitors to start firing. However, no current flows from the rectifiers, because the negative bias is maintaining a blocking action.

5. Finally, the short-circuiting switch *D* is closed, instantly removing the negative bias from all the grids in all the ignitron rectifiers. There remains the 110-volt positive a-c excitation from the baffle transformers, which had been canceled up to this time by the 350-volt negative bias. All 12 ignitron rectifiers, therefore, are permitted to carry current at the same instant.

6. Operator's switches C_1 and C_2 can now be opened. This de-energizes the 350-volt negative-biasing circuit, disconnecting it completely from the main power circuits. It also operates all relays *31S* and *31SX*, transferring the neutrals of the baffle transformers from the master biasing circuit back to the cathode terminals of the rectifiers.

Field Tests

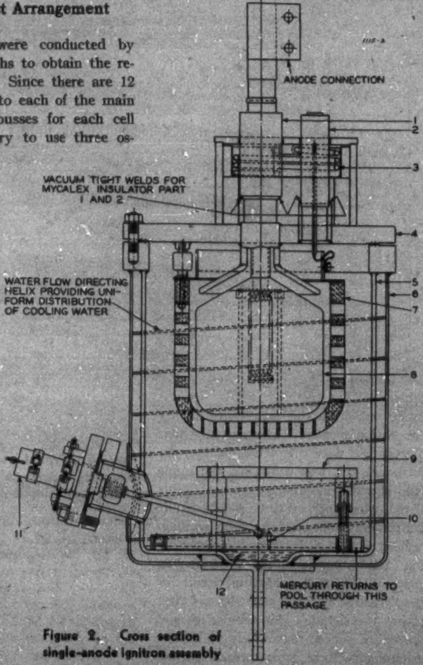
In order to make a thorough study of the problem, it was decided to make extensive tests in the field. Aluminum Company of America operates several reduction plants. Three western plants, all employing rectifiers, were selected. These plants will be designated as plants *A*, *B*, and *C*, for purposes of reference. Initial tests were made on one manufacturer's rectifiers at plant *A*. Confirming tests were made on rectifiers of other manufacture and type at plants *B* and *C* to make them fully comprehensive in nature.

Oscillographic-Test Arrangement

The field tests were conducted by means of oscillographs to obtain the required information. Since there are 12 rectifiers connected to each of the main 60,000-ampere d-c busses for each cell line, it was necessary to use three oscillographs in order to have available enough vibrators to record the behavior of the cathode currents in all 12 rectifiers. By using three oscillographs, 20 vibrators were available.

The three oscillographs were placed on a single operator's table. One vibrator in each oscillograph was connected to a common timing wave, so that the instant of starting the 12 rectifiers would be commonly indicated on the three oscillograms taken for each test. Then four supersensitive vibrators of oscillograph 1 were connected through shunt leads to the ammeter shunts in the cathode lines of rectifiers *1A*, *1B*, *2A*, and *2B*. Oscillograph 2 had four supersensitive vibrators connected to the four ammeter shunts in cathode lines of *3A*, *3B*, *4A*, and *4B*, and likewise oscillograph 3 was connected with ammeter shunts in cathode lines of *5A*, *5B*, *6A*, and *6B*. In each instance, main cell-line current was recorded on one of the films.

Control of the starting operation for all 12 rectifiers was transferred from the operator's main panel in the substation to control switches on the oscillographer's


ANODE CONNECTION
1
2
3
VACUUM TIGHT WELDS FOR MYCALEX INSULATOR PART 1 AND 2
4
WATER FLOW DIRECTING HELIX PROVIDING UNIFORM DISTRIBUTION OF COOLING WATER
5
6
7
8
9
10
11
12
MERCURY RETURNS TO POOL THROUGH THIS PASSAGE

1. Mycalex anode insulator
2. Mycalex insulator (lead to insulated baffle)
3. Anode heater
4. Vacuum-chamber cover
5. Vacuum chamber
6. Water jacket
7. Energized (deionizing) anode baffle
8. Graphite anode
9. Mercury splash baffle
10. Ignitor tip
11. Mycalex insulator for ignitor and relieving anode leads
12. Mercury pool (cathode)

Figure 2. Cross section of single-anode ignitron assembly

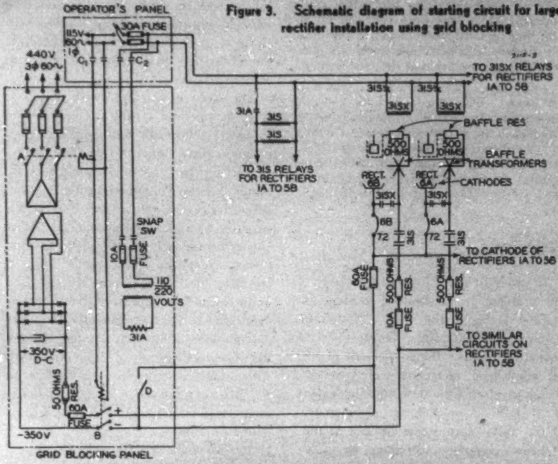

OPERATOR'S PANEL
30A FUSE
115V
60~
1ϕ
440V
3ϕ 60~
C_1
C_2
TO 31SX RELAYS FOR RECTIFIERS 1A TO 5B
31SX
31SX
31SX
31SX
31A
31S
31S
TO 31S RELAYS FOR RECTIFIERS 1A TO 5B
BAFFLE RES
500 OHMS
500 OHMS
BAFFLE TRANSFORMERS
RECT. 6B
RECT. 6A
CATHODES
31SX
31SX
6B
6A
72
72
31S
31S
TO CATHODE OF RECTIFIERS 1A TO 5B
A
SNAP SW
10A
FUSE
110
220
VOLTS
31A
60A FUSE
500 OHMS RES.
10A FUSE
500 OHMS RES.
FUSE
TO SIMILAR CIRCUITS ON RECTIFIERS 1A TO 5B
350V D-C
50 OHMS RES.
60A FUSE
D
+
−
−350V
B
GRID BLOCKING PANEL

Figure 3. Schematic diagram of starting circuit for large rectifier installation using grid blocking

Figure 4 (below). Typical oscillogram starting cell line by master a-c excitation

On this test the oscillograms for the cathode currents of the other eight rectifiers showed the following results:

1*A* started 0.72 cycle late

1*B* started 0.60 cycle late

2*B* started 0.67 cycle late

3*A* started 0.64 cycle late

5*A* started 0.33 cycle late

5*B* started 0.33 cycle late

6*A* started 0.72 cycle late

6*B* started 0 cycle late same instant as 3*B*. Cathode current for 6*B* overswung same as 3*B*. Otherwise behavior of cathode current for these rectifiers was same as shown for 2*A*, 4*A*, and 4*B*

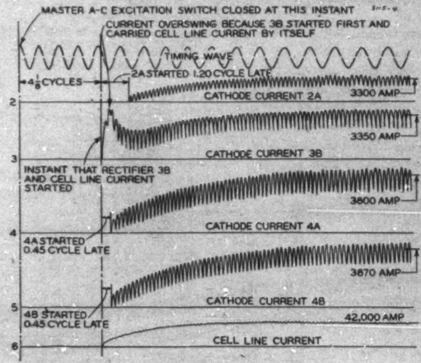


table. All 12 rectifiers would be first taken out of service simultaneously by the station operator. Then, after switching changes were completed quickly, the oscillographer, who controlled the single final starting operation, gave an audible signal, whereupon all three oscillographic instruments were started. The oscillographer, who gave the signal for starting the three oscillographic instruments, operated the master starting circuit for the 12 rectifiers a fraction of a second after giving the signal. This test setup and procedure worked remarkably well. The instruments were synchronized so accurately by their operators that the starting transients occurred on the oscillographic film for all tests less than 30 cycles after the starting signal. At the three plants a total of 13 such tests was made.

Results of Starting Tests Using Ignitor Control

Five tests were made at plant *A*, two at plant *B*, and one at plant *C* to record the behavior of the 12 separate rectifiers using the master a-c ignitor-excitation method. Figure 4 is typical of the oscillographic results of these tests. The number of cathode-current records shown in Figure 4 is limited to only 4 of the 12 rectifiers. This was necessary, because an illustration showing the behavior of all 12 rectifiers would be excessively large for publication. However, these four records typify the results.

The results of several of the starting tests using the master a-c ignitor-excitation method to demonstrate the effects or disparity in starting time of the individual rectifier units are given in Table I.

A special test was made at plant *B*, using the master a-c ignitor-excitation method to determine what effect cold ignitors and cathode mercury would have on starting. The volt-amperes output characteristic of the firing circuit used on these rectifiers was lower than on the rectifiers where the other starting tests with master a-c excitation were made. This volt-ampere characteristic was, however, sufficiently high to give satisfactory operation of the ignitors under continuous normal operating conditions after the ignitors and cathode mercury were warm. Results of this special test were that some of the rectifiers delayed as long as 20 to 30 cycles before the ignitors began to fire. Needless to say, with the

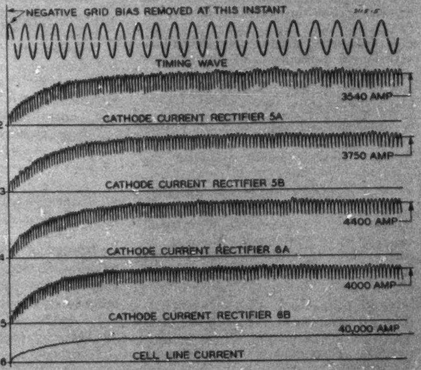


Figure 5. Typical oscillogram starting cell line by grid blocking

Cathode currents for rectifiers 1*A*, 1*B*, 2*A*, 2*B*, 3*A*, 3*B*, 4*A*, and 4*B*, started at same instant and had same appearance as cathode currents for 5*A*, 5*B*, 6*A*, and 6*B* shown on this oscillogram various individual rectifiers trying to pick up a cell line at various intervals over such a long period as 20 cycles, each of them tripped out on d-c overcurrent in turn as they started, and the rectifiers failed to energize the cell line.

Results of Starting Tests Using Grid Method

There were five cell-line starting tests made using the Figure 3 grid-blocking methods, two at plant *A* and three at plant *B*. The results of the tests are typically illustrated by Figure 5. For the same reasons as explained for Figure 4, only 4 records of cathode current are shown instead of 12.

During the tests with the grid blocking, the power circuits were all energized and the ignitors caused to fire for a short interval prior to the time when the negative grid bias was removed to pick up the cell-line load. There was no failure of blocking action at any time.

Conclusions

Based on the results of the various tests described, the following conclusions were reached:

1. While the cell-line current requires five to six cycles to reach its steady-state value, its initial rate of rise, while slow referred to the total load, is relatively high referred to the rating of a single rectifier. For this reason, unless all the rectifiers start carrying current at the same instant, when a cell line is energized, the first rectifiers to carry current accumulate load so rapidly that overload can exist in a cycle or less. The three-pole contactors used for the supplies to the individual ignitor-firing circuits were of a standard industrial-control type. A disparity as high as $2^{1}/_{4}$ cycles existed in the closing times of these contactors, as proved by the various starting tests. In view of the high rate of rise of cell-line current, if the conventional master a-c ignitor-excitation method of cell-line starting is to be used successfully (excluding effects of cold ignitor points and cold mercury), only contactors having a maximum difference in closing time not greater than one-half cycle should be used.

2. It was proved that the resistance characteristics of cold ignitors may be such that they require substantially higher volt-amperes to cause them to ignite cathode spots, when cold, after a service outage, than under normal operating conditions. Therefore, to insure satisfactory cell-line starting with ignitron rectifiers, the volt-ampere characteristics of the firing circuit must match these extra requirements if the master a-c excitation method is to be employed.

3. The new starting method for large rectifier installations employing the grid-blocking action available in ignitron rectifiers was demonstrated to be practical. The equipment used for making the starting tests by the new grid-blocking method was semipermanently connected in one of the ignitron-rectifier cell-line installations at plant *B*. At least six emergency outages have occurred subsequent to the tests described in the paper. In each instance the new grid-starting method was employed for re-energizing the cell line. All reports show that the scheme was completely successful in all cases. It is, therefore, believed that the grid-blocking method offers a better solution for starting large Ignitron installations of this sort. This conclusion may be reached, because the new method overcomes the difficulty with cold ignitors by permitting the station operators to run them for a sufficient period to warm them up and restore easy ignition characteristics; and it also eliminates the problem of obtaining control contactors having precision closing time, the outcome of which, at best, may be doubtful.

Addendum

During the tests, in addition to solving the problems outlined in the paper, several other interesting and valuable data were obtained. The effect of cell-line inductance in the operation of large electrochemical plants often has been speculated upon by engineers dealing with the problem. Records were obtained of the cell-line-current rate of rise and are shown in Figure 4 and Figure 5.

Cell-line inductance as measured by the starting transients in cell-line current, shown in Figure 4 and Figure 5, has been recognized as a factor affecting the performance of main cell-line breakers in the older installations using rotating apparatus for conversion purposes, and also the cathode breakers, which are used to trip the cell-line currents when shutting down installations employing mercury-arc rectifiers. While it is well known that large air circuit breakers trip with more uniform opening time than they close, nevertheless, the contacts are bound to start parting on some of the cathode breakers earlier than others during planned shutdowns.

Large electrochemical rectifier installations are shut down by operating a master switch which gives simultaneous tripping impulses to all the cathode breakers. When the faster cathode breakers start parting contacts, there is a rapid extinction of current from their associated rectifiers. The remaining rectifiers, whose cathode breakers are slower to open, therefore, accumulate the portion of the load dropped by the earlier rectifiers.

Two important effects, therefore, combine to impose substantially higher interrupting duty on the slower cathode breakers. First, overcurrent exists on one or more of the rectifiers. The magni-

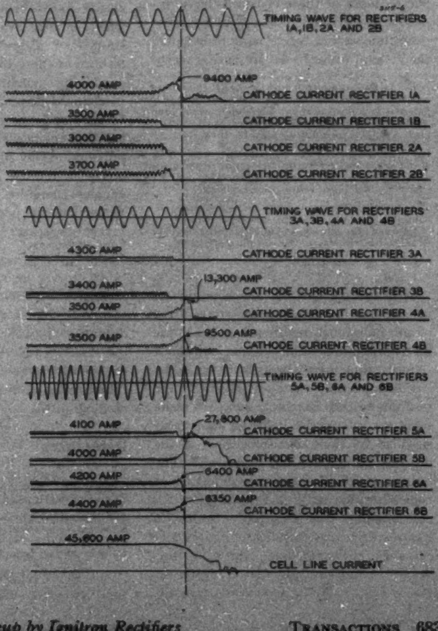


Figure 6. Typical oscillogram showing cathode current in all 12 rectifiers feeding one cell line and total cell-line current when cell line is de-energized by tripping cathode breakers by master tripping switch

The Quadrature Synchronous Reactance of Salient-Pole Synchronous Machines

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Synopsis: Advances in the theory of synchronous machines have created a number of reactance constants, and improvements in theory have led to continual refinements in methods of calculation and test. This paper presents an improvement in the calculation of the quadrature synchronous reactance, compares calculated with test results, and discusses four methods of test, one of which is an improvement in simplicity and ease of accomplishment over methods of test presented previously.

THE wide application of synchronous machines to many methods of power generation and utilization has caused the development and refinement of synchronous-machine theory as a tool to enable the accurate prediction of the behavior of these machines. This theory has created a number of characteristic constants, or coefficients, in terms of which the operation of the synchronous machine is defined. Papers dealing with one or more of these constants have appeared in the pages of the technical press from time to time, each paper broadening the general concepts or presenting methods of calculation or test which more sharply define particular constants.*

Past attention to individual reactance constants has been devoted chiefly to the armature leakage reactance[7] x_l, to the direct-axis quantities[2,6] x_d, x_d', x_d'', and to the negative- and zero-sequence reactances[2,6] x_2 and x_0. Less attention has been paid to the quadrature synchronous reactance[1,2] x_q either in the development of its full theory or in the presentation of test data. The situation is a natural one, as this reactance is not of major importance in either the short-circuit or starting characteristics of machines, and it is these characteristics which have received the most attention. Further, both the classic slip test[6] for x_q and reluctance power test[1] present difficulties in their execution which have discouraged the accumulation of data.

The quadrature synchronous reactance is of prime importance in determining the steady-state torque-angle characteristic which in turn evaluates the spring constant P_r. Accurate knowledge of either or both of these things is necessary when combining synchronous machines with mechanical drives having pulsating shaft torques such as are found in Diesel or gasoline engines and reciprocating compressors. The present tremendous increase in the use of Diesel and gasoline engines therefore makes an analysis of x_q particularly pertinent.

This paper presents refinements in the theory and calculation of x_q and advances two new methods of test for this quantity. It also presents a discussion of the two previously published test methods and compares the results which have been obtained by calculation and by the four test methods.

General Theory, Methods, and Test Results

The quadrature synchronous reactance x_q of salient-pole synchronous machines can be thought of as being made up of two major parts. One of the parts of this quantity is armature leakage reactance x_l the theory of which has been fully presented previously. The second part of this quantity is the reactance of armature reaction. This second quantity x_{aq} exists as a result of the generation of fundamental air-gap flux when armature current flows and the subsequent generation by that flux of a component of armature voltage which opposes an increase in the armature current. The magnitude of the flux and therefore the back voltage depend in part upon the permeance of the air gap opposite the armature magnetomotive-force wave.

In synchronous machines there are two axes of air-gap symmetry, the direct and quadrature axes, and hence two values of gap permeance. Each of these has been determined accurately over the range of variables encountered in modern machine design for unslotted air gaps, but the effect of slots in either or both the armature and field surfaces, while it has been recognized, has not been presented previously in the technical press. Usually the reactance of armature reaction constitutes the major part of x_q, and to predetermine this quantity accurately, its major component must be predetermined

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* See "References."

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tude of this current may reach as high as 600 per cent of the full-load current rating of an individual rectifier. Second, the high inductance of the cell-line load tends to maintain current through the breakers.

The opportunity was seized, during the tests described in the paper, to make some records of the behavior of current supplied by the various rectifiers to the d-c bus during cell-line shutdown. The results of these tests are shown in a typical fashion by Figure 6.

Rectifier cathode-current traces in Figures 4, 5, and 6 indicate a high 360-cycle d-c current ripple. Actually, this ripple is substantially lower. It was necessary to use standard d-c shunts instead of oscillograph shunts. With standard shunts the effect of shunt inductance, as well as the effect of fields on shunt leads, is responsible for this apparently large ripple. This must be taken into account in analyzing the oscillograms.

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accurately. In Appendix B a method of calculating the quadrature reactance of armature reaction is presented. This method is based on obtaining the unslotted quadrature-axis air-gap permeance in the conventional manner and then applying to it selected fringing coefficients to account for the slots in the magnetic surfaces. After a value for the reactance of armature reaction has been obtained, the armature leakage reactance is added to it to obtain x_q. The results obtained by calculation are substantiated, as shown in Table I, by several different methods of test.

Unlike the method used for calculating x_q, the test for it must measure the entire quantity, both the armature leakage reactance and the reactance of armature reaction, as it is impossible in test to separate x_q into its analytical components and measure each individually. This restriction places on all test methods the necessity of measuring terminal conditions at an instant when these conditions can be used to evaluate x_q. This instant always occurs just prior to a state of instability, and consequently accurate measurements are not easily made.

In the first method of test, the slip test of Appendix C, an oscillograph is used to record the terminal voltage and armature current as the rotor is forced to slip past the axis of armature magnetomotive force. Unfortunately, the very slip which makes the test possible introduces its own errors. If the slip is large, over $1^1/_2$ per cent, currents are often induced in the usually present amortisseur windings, and the armature current is influenced by them, so that it may no longer reflect the true rotor permeance. While smaller values of slip can be obtained, they require accurate control of the terminal voltage and ability to adjust it, usually over the range from 20 per cent to 30 per cent of normal voltage. This control and adjustment is seldom available anywhere except on the manufacturer's premises, and it is not always easy to obtain it there for every machine.

The reluctance-torque method, described in Appendix D, is based on the determination of the maximum power the machine will carry when operating as a reluctance motor. The motor, with field open-circuited, can be pulled out of step either by increasing the shaft load with terminal voltage held constant or by reducing the terminal voltage with the shaft load held constant. Since the measurements are always made at the moment of instability, it is important in both of the preceding methods that the shaft load contain no torque pulsations. If the adjustable-shaft-load method is used, it is important that the load be capable of fine adjustment, so that the approach to the point of instability can be made accurately. If the test is taken at no load, the line voltage is reduced until the machine falls out of step because of the friction and windage load. This requires the same type of terminal voltage control necessary in the slip test and hence presents the same difficulties. In addition, it also requires accurate measurement of the friction and windage load.

Table I. Quadrature Synchronous Reactance x_q by Calculation and Test

Type of Machine	Kilovolt-amperes	Volts	Frequency (Cycles Per Second)	Rpm	x_q				
						Tests			
					Calculation	Slip	Reluctance Torque	Negative Excitation	Maximum Lagging Current
Motor	1,405	4,160	60	1,800	0.98	1.03	0.98	1.00	0.96
Motor	300	440	60	1,200	1.18		0.97	1.26	1.16
Motor	380	440	60	1,200	1.14	1.09	1.11	1.10	1.14
Industrial-type generator	31.3	208	60	900	0.43	0.42	0.41	0.39	0.43
Motor	900	4,150	60	900	0.98	0.91	1.03	0.98	0.98
Motor	2,700	6,600	60	900	0.98	1.04	1.02	1.00	1.00
Motor	1,160	2,200	60	900	0.74	0.77		0.77	0.72
Motor	1,250	6,600	60	900	0.53	0.54	0.59	0.58	0.57
Motor	1,358	4,100	60	900	0.83	0.83	0.80	0.82	0.82
Motor	2,200	2,200	60	720	0.72	0.71	0.68	0.70	0.70
Motor	1,175	2,200	60	600	0.75	0.77	0.83	0.81	0.82
Motor	5,900	6,600	60	514	1.02	0.96	1.02	0.97	0.98
Motor	2,100	6,660	25	500	0.92	0.92		0.92	0.93
Motor	1,465	2,200	60	450	0.82	0.82		0.84	0.82
Motor	690	4,000	60	400	0.84	0.86	0.85	0.87	0.85
Motor	7,320	6,900	60	400	0.43	0.43			
Industrial-type generator	187	400	50	375	0.53	0.63	0.57	0.63	0.66
Industrial-type generator	500	4,160	60	360	0.60	0.58	0.59	0.57	
Industrial-type generator	625	4,160	60	360	0.65	0.67	0.65	0.67	0.66
Industrial-type generator	375	2,400	60	327	0.75	0.73	0.71		
Motor	205	2,200	60	300	0.99	1.00	0.89	0.95	0.93
Motor	1,380	4,000	60	277	0.77			0.70	0.74
Waterwheel-type generator	6,250	4,150	60	240	0.55	0.57	0.59		
Motor	780	2,300	60	164	1.08	1.09	0.98	1.10	1.08
Waterwheel-type generator	24,000	13,200	60	164	0.58	0.58			0.59

The negative-excitation test, described in Appendix E, is based on the determination of the amount of field excitation required to overcome the reluctance torque of the machine. The test is taken with the machine operating as a synchronous motor with a free shaft and at rated line voltage, thus eliminating the necessity for a variable voltage supply. It is important in this test that the shaft load be free of torque harmonics and of as small a magnitude as it is possible to obtain. As this small load determines the point of instability, it is important that it be known to calculate x_q.

The fourth and most satisfactory test method, the maximum-lagging-current test, described in Appendix F, uses the same method of test as in the negative-excitation method of Appendix E. In this case it is not necessary to measure shaft power, other than to be sure that it is small, as line voltage and current readings are sufficient. Because the line current is large in size and easy to measure, the test is simpler in its technique than previous methods. Further, a variable line voltage is not required, because, like the negative-excitation test, results can be obtained at rated voltage. The value of x_q is simply the ratio of voltage and maximum stable current.

Thus, in the course of developing various methods of test, the objections have one by one been removed. The necessity of having an oscillograph was first removed, then the requirement of a controlled low-voltage source, and finally the low power readings. The results obtained by the various methods of test are shown in Table I. It was not possible to test each machine included in the table by every test method, and the blanks indicate lack of test data on the particular machine. The agreement is, in general, excellent. No attempt has been made to establish divergences between test and calculation or between test methods. These are apparent from a study of the table itself.

Conclusions

The paper has presented improvements in the method of calculating x_q which enable the accurate prediction of this quantity. It has reviewed two well-known test methods and presented two new ones, one of which, the maximum-lagging-current test, has great advantages in simplicity of technique and ease of accomplishment. These advantages should lead to a wide acceptance of the maximum lagging-current test as a recognized test procedure.

Appendix A. Nomenclature

Electrical quantities, voltage, current, power, reactance, and resistance, are in the unit system and are represented by symbols which have already been generally recognized. Other quantities are, when necessary, defined adjacent to their initial use in the text, and all definitions are consolidated in this appendix.

A = square wave value of armature-reaction ampere turns per pole

$$A_1 = \frac{\text{maximum ordinate of fundamental flux-density wave}}{\text{maximum ordinate of actual flux-density wave}}$$

in the air gap of a synchronous machine when excited only from the rotor (saturation is neglected)

$$A_{q1} = \frac{\text{maximum ordinate of fundamental flux-density wave}}{\text{maximum ordinate of actual flux-density wave that would be produced by the same magnetomotive force acting on a uniform gap equal to } g_{\min}}$$

in the air gap of a synchronous machine when excited by sine wave of armature magnetomotive force in the quadrature axis (saturation is neglected)

$C_{\sigma_{fdf}}$ = total fringing coefficient applied to g'_{fdf} to obtain g_{fdf}

$C_{\sigma_{fqa}}$ = total fringing coefficient applied to g'_{fqa} to obtain g_{fqa}

e = per unit armature terminal voltage

E = particular value of per unit armature terminal voltage

e_d = per unit direct-axis field excitation, neglecting saturation

E_d = particular value of per unit direct-axis field excitation, neglecting saturation

F = field ampere turns in the air gap required to produce normal voltage in the armature on open circuit

$g_{\min}$ = iron-to-iron air gap at the pole center when opposite an armature tooth

g'_{fdf} = apparent air gap seen by the maximum ordinate of the fundamental direct-axis field flux-density wave, neglecting fringing phenomena

g_{fdf} = air gap seen by the maximum ordinate of the fundamental direct-axis field flux-density wave including fringing phenomena

g'_{fqa} = apparent air gap seen by the maximum ordinate of the fundamental quadrature-axis armature flux-density wave, neglecting fringing phenomena

g_{fqa} = air gap seen by the maximum ordinate of the fundamental quadrature-axis armature flux-density wave, including fringing phenomena

I_1 = particular value of per unit line current

P = per unit value of shaft power (unit power is numerically equal to base kilovolt-amperes)

P_1 = particular value of per unit shaft power

P_r = synchronizing coefficient determined by dividing the shaft power in kilowatts by the corresponding angular displacement in electrical radians of the rotor from its zero shaft power position

Q = per unit value of reactive power (Unit reactive power is base kilovolt-amperes)

Q_1 = particular value of per unit reactive power

r_a = per unit armature resistance

x_l = per unit armature leakage reactance

x_d = per unit direct-axis synchronous reactance

x_q = per unit quadrature-axis synchronous reactance

x_{aq} = per unit quadrature-axis reactance of armature reaction

x = per unit external reactance

δ = load angle in electrical degrees

Γ, T, Ω = quantities defined in equations 20, 22 and 25, respectively

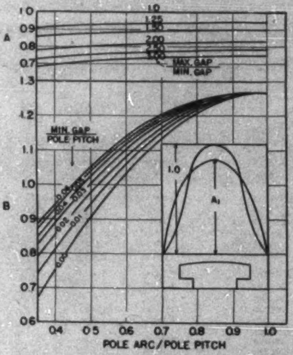

Figure 1. Ratio of the maximum amplitudes of the fundamental component and the actual flux-density waves in the air gap of a synchronous machine excited only from the rotor

Maximum value of actual flux-density wave = unity

Maximum value of fundamental flux-density wave, $A_1 = A \times B$

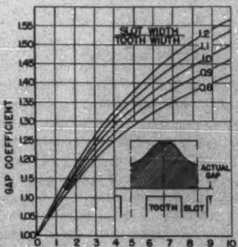

Figure 2. Air-gap fringing coefficient for the flux around a tooth of finite width and a slot of infinite depth as obtained by elliptic function analysis

Use the magnetic gap in place of the actual gap when the two differ

Appendix B. The Calculation of x_q

The quadrature-axis synchronous reactance x_q is defined as the ratio of the fundamental component of reactive armature voltage, due to the fundamental quadrature-axis component of armature current to this component of current under steady-state conditions and at rated frequency. Usually the reactance is taken as the value corresponding to rated armature current.

Alger[7] divided this quantity into two parts, the reactance of armature reaction

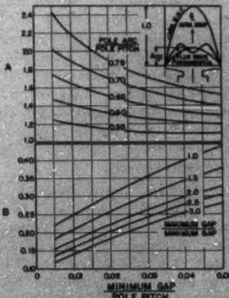

Figure 3. Fundamental of the air-gap flux-density wave when a salient-pole synchronous machine is excited by only a sine-wave armature magnetomotive force whose axis is in quadrature with the pole center

Maximum value of armature magnetomotive force = unity. Fundamental $A_{q1} = A \times B$

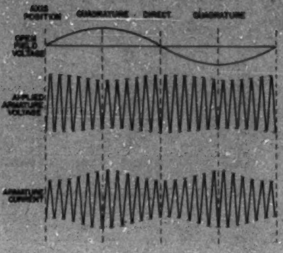

Figure 4. Appearance of oscillogram, slip test for x_q

In practice slip must be the minimum possible and only a small fraction of that illustrated

$$x_q = \frac{\text{per unit applied armature voltage}}{\text{per unit armature current}}$$

where both values are read from the quadrature-axis position

x_{aq}, and the armature leakage reactance x_l, and produced methods for the calculation of x_l. Wieseman,[3] by means of curves derived from flux plots, evaluated coefficients A_1 and A_{q1} which, for smooth air gaps, made possible the determination of the ratio of fundamental air-gap flux due to field magnetomotive force. Linville[4] showed how to use these coefficients to calculate x_{aq}. Both Wieseman's presentation and Linville's employment of it were, as noted, based on smooth gaps. Kilgore[5] pointed out that an "effective" gap, not the actual measured gap at the pole center, must be used when slotted surfaces are considered, but a method of obtaining that effective gap and the employment of the result has not been presented. This, a step needed for the accurate calculation of x_q, is presented here.

The problem can be approached most clearly by first considering the phenomena existing in the direct-axis air gap when the machine is excited only by the field winding. Next, the effect of quadrature excitation from the armature will be introduced, and finally the two effects will be considered jointly to produce an equation for x_{aq}.

Assume, for the moment, that sufficient field excitation is applied to a smooth air gap to produce an actual flux-density wave whose maximum ordinate, at the pole center, is unity. Then from Figure 1, the amplitude of the maximum ordinate of the fundamental of that wave is A_1. The air gap seen by the maximum ordinate of the actual flux-density wave is the iron-to-iron gap at the pole center $g_{\min}$. Since both the actual and fundamental flux-density waves are created by the same magnetomotive force, their different amplitudes indicate that the magnetic gaps seen by the actual and fundamental components are different. Since the ratio of the amplitude of the flux-density waves is A_1, and the gap seen by the maximum ordinate of the actual wave is $g_{\min}$, then the gap seen by the maximum ordinate of the fundamental wave is

$$g'_{fdf} = \frac{g_{\min}}{A_1} \qquad (1)$$

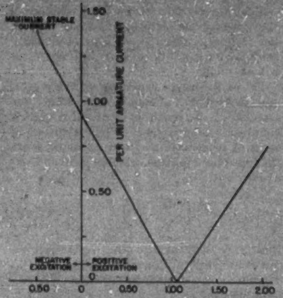

Figure 5. Per unit armature current versus excitation at constant terminal voltage

Carter has derived the relationship between the magnetic gaps of two parallel smooth surfaces and one smooth and one slotted surface. His fringing coefficients, which also can be thought of as "equivalent gap ratios" are well known. Figure 2 shows this coefficient for stator slots over a range of parameters encountered in synchronous machines. Similar characteristics can be obtained for stator air ducts, rotor slots, and other discontinuities in the magnetic surfaces. When these fringing coefficients are applied to the fundamental gap, g'_{fdf}, the relationship between the measured iron-to-iron minimum gap and the magnetic minimum gap for the fundamental component of the actual flux wave can be approximated closely.

To use this approximation, the actual iron-to-iron minimum gap, $g_{\min}$, is the starting point. g'_{fdf}, the effective smooth gap for the direct-axis fundamental flux-density wave due to rotor excitation, is next found from equation 1. Then using g'_{fdf}, the fringing coefficient for the armature slots is found from Figure 2, and similarly, fringing coefficients for other magnetic discontinuities in the air gap are obtained. If the product of the several fringing coefficients for this condition is $C_{g_{fdf}}$, then the magnetic gap seen by the fundamental component of the direct-axis field-excited flux-density wave is

$$g_{fdf} = C_{g_{fdf}} g'_{fdf}$$

$$= C_{g_{fdf}} \frac{g_{\min}}{A_1} \qquad (2)$$

Figure 3 shows the ratio between the maximum per unit amplitudes of the fundamental component of the flux-density wave and the fundamental magnetomotive-force wave in the quadrature axis when the machine is excited only by a sine wave of armature magnetomotive force in the quadrature axis. If this identical unit magnetomotive-force wave that is applied to the complex smooth gap configuration of Figure 3 were applied to a constant-length smooth gap equal to $g_{\min}$, then the fundamental component of the flux-density wave would be unity. Therefore, the ratio A_{q1} is not only a ratio between a magnetomotive-force wave and a flux-density wave, but may also be thought of as the ratio between the fundamental component of quadrature-axis armature flux and unit fundamental armature flux, both of which are created by unit fundamental armature magnetomotive force. Hence, like A_1, it also is an inverse ratio of magnetic gap and the gap seen by the maximum ordinate of the fundamental quadrature-axis armature flux is

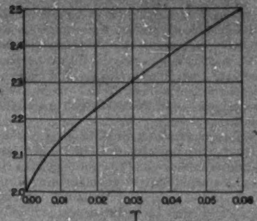

Figure 6. Plot of equation 22

$$g'_{fqa} = \frac{g_{\min}}{A_{q1}} \qquad (3)$$

If similar approximations are applied to g'_{fqa} as were applied to g'_{fdf} to get the appropriate fringing coefficients, the magnetic gap seen by the fundamental component of quadrature axis armature flux is

$$g_{fqa} = C_{g_{fqa}} g'_{fqa}$$

$$= C_{g_{fqa}} \frac{g_{\min}}{A_{q1}} \qquad (4)$$

Under the assumption of smooth air-gap surfaces and with the application of identical fringing coefficients to all methods of excitation, the relationships determining x_{aq} have been shown to be[4]

$$x_{aq} = \frac{4}{\pi} \frac{A}{F} \frac{A_{q1}}{A_1} \qquad (5)$$

When the fringing coefficients are recognized to be different and dependent upon the method of excitation, as set forth here, this equation becomes

$$x_{aq} = \frac{4}{\pi} \frac{A}{F} \frac{A_{q1}}{A_1} \frac{C_{g_{fdf}}}{C_{g_{fqa}}} \qquad (6)$$

Also there is the relationship

$$x_q = x_l + x_{aq} \qquad (7)$$

Therefore

$$x_q = x_l + \frac{4}{\pi} \frac{A}{F} \frac{A_{q1}}{A_1} \frac{C_{g_{fdf}}}{C_{g_{fqa}}} \qquad (8)$$

Appendix C. The Slip Test For x_q

The slip test for x_q has already been presented adequately in the technical press,[6] and so only a brief description is necessary. The machine to be tested is either coupled to a second machine or run with a free shaft and positive-sequence voltage applied to the terminals. The field winding is open-circuited. The terminal voltage or shaft torque is then adjusted so that the reluctance torque of the machine is overcome, causing the field to slip past the armature magnetomotive-force wave at a low value of slip. An oscillographic record of the armature current, terminal voltage, and induced field voltage, is then taken. Figure 4 illustrates the appearance of such an oscillogram.

The quadrature synchronous reactance equals the minimum ratio of the per unit armature voltage and current read from the oscillogram. The correct values of armature voltage and current are obtained at those instances when the induced field voltage is at a positive or negative maximum.

The slip method of measurement, using an oscillograph, has the advantage of measuring most clearly those quantities on which the definition of x_q is based and of providing a permanent record of the quantities read. It also has disadvantages. Since the terminal voltage applied must usually be approximately 20 per cent to 30 per cent of the rated voltage and must be adjusted accurately, a controllable low-voltage power supply is required. It is important that the slip be very small; otherwise in machines having amortisseur windings, currents will be induced in these windings, and the reactances read from the oscillogram will be low. Even under the most strictly controlled conditions the instantaneous slip of the rotor is usually a minimum when the stator magnetomotive force is passing through the quadrature axis, and hence a difference between the accuracies of x_d and x_q, as read by the oscillogram, is inherent in the method.

For these reasons the slip method is generally limited to testing in the manufacturer's plant and is not broadly suitable for field work.

Appendix D. Reluctance Torque Test For x_q

Kingsley[1] first suggested the reluctance-torque test for x_q. Neglecting losses, the power-angle equation of an ideal synchronous machine connected to an infinite bus is given by[10]

$$P=\frac{ee_d}{x_d}\sin\delta+\frac{e^2(x_d-x_q)}{2x_dx_q}\sin 2\delta \qquad (9)$$

If the machine is operated as a reluctance motor, $e_d=0$, and maximum synchronous power will occur when $\sin 2\delta=1.0$ (that is, $\delta=45$ degrees). Any change tending to increase δ will cause loss of synchronism.

By lowering the terminal voltage or increasing the shaft load, the machine may be made to pull out of synchronism. Knowing x_d from other tests, and determining E and P_1 at the point at which loss of synchronism occurs, x_q can be found from the relation

$$x_q=\frac{E^2x_d}{E^2+2P_1x_d} \qquad (10)$$

It can be shown that the effect of armature resistance is approximately taken into account by the equation

$$x_q=\frac{E^2(x_d+2r_a)}{E^2+2P_1x_d} \qquad (11)$$

If P_1 is only the friction and windage of the machine, the drop-out voltage will be low (15 per cent to 25 per cent of normal), saturation will not be present, and core losses will be small.

Errors in the measurement of the power, and particularly the pull-out voltage, are possible as the machine is unstable as this condition is approached. The magnitude of error in determining x_q can be found from the following equation, which is derived from equation 10:

$$\frac{dx_q}{x_q}=\left(1-\frac{x_q}{x_d}\right)\left(2\frac{de}{e}-\frac{dP}{P}\right) \qquad (12)$$

Reactance external to the test machine causes pull-out at some angle less than 45 degrees, and the calculated value of x_q is too high.

If x is the external reactance, the load angle at pull-out is found to be

$$\delta=\text{arc tan}\,\frac{x_q(x_d+x)}{x_d(x_q+x)} \qquad (13)$$

The value of x_q is then given by

$$x_q=\frac{E^2(x_d\sin 2\delta+2r_a)}{E^2\sin 2\delta+2P_1x_d} \qquad (14)$$

If the effects of armature resistance and external reactance are neglected, the error is given, very closely, by the relation

$$\frac{\Delta x_q}{x_q}\cong\left(1-\frac{x_q}{x_d}\right)(1-\sin 2\delta) \qquad (15)$$

The reluctance-torque test for x_q has one advantage over the slip test, namely, that satisfactory readings are possible without the use of an oscillograph. It has many of the disadvantages of the slip test. Again a controllable low-voltage source is usually required, and tests are generally possible only on the manufacturer's premises. In addition, shaft power must be known accurately.

Appendix E. Negative-Excitation Test for x_q

The negative-excitation test for x_q derives its name from the reversed polarity which is imposed on the field winding when the instruments are read and the necessity of recording this value. The machine to be tested is run as a synchronous motor, with a free shaft and with constant applied rated terminal voltage. The field current is initially adjusted to its value at the low point in the V-curve characteristic. It is then decreased to zero, causing an increase in line current, and, after reaching zero field current, the polarity of the applied excitation is reversed and field current increased in the negative direction, causing a continuing increase in the armature current. See Figure 5. By applying small increments in field current as the maximum stable negative field current is approached, the highest per unit value E_d of field current that can be obtained is found. Per unit shaft power, P_1, at this point is also determined. If the per unit direct-axis synchronous reactance x_d is known from other tests, x_q can then be found from the two readings taken.

Neglecting losses and applying negative excitation, the power-angle equation 9 becomes

$$P=\frac{e(-e_d)}{x_d}\sin\delta+\frac{e^2(x_d-x_q)}{2x_dx_q}\sin 2\delta \qquad (16)$$

When the machine is operating at its maximum stable point, there is

$$\frac{dP}{d\delta}=0 \qquad (17)$$

and

$$\frac{ee_d}{x_d}\cos\delta=\frac{e^2(x_d-x_q)}{x_dx_q}\cos 2\delta \qquad (18)$$

from which

$$\delta=\text{arc cos}\,\frac{1.0+\sqrt{1+2\Gamma^2}}{2\Gamma} \qquad (19)$$

where

$$\Gamma=\frac{2e}{e_d}\left(\frac{x_d}{x_q}-1.0\right) \qquad (20)$$

Combining and simplifying equations 16, 19 and 20 there results

$$P=\frac{ee_d}{x_d}\Upsilon \qquad (21)$$

where

$$\Upsilon=\sqrt{\frac{(\sqrt{1.0+2\Gamma^2}-3.0)^3}{32(\sqrt{1.0+2\Gamma^2}-1.0)}} \qquad (22)$$

Equation 22 is plotted in Figure 6.

If test values of $P(P=P_1)$, $e_d(e_d=E_d)$, $e(e=E)$, and x_d are substituted in equation 21, there is

$$\Upsilon=\frac{P_1x_d}{EE_d}$$

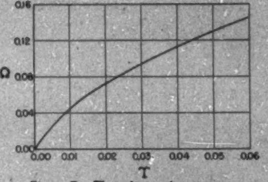

Figure 7. Plot of error function Ω

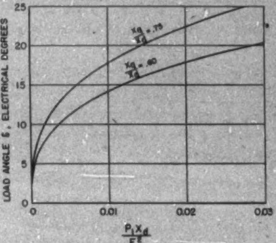

Figure 8. Plot of equation 31

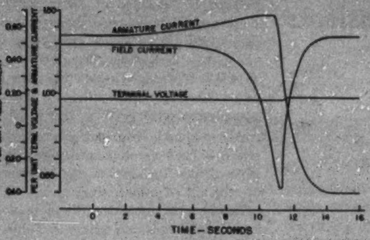

Figure 9. Terminal characteristics during a negative-excitation slip cycle

and the quantity Υ can be evaluated by test. Figure 6 can then be used to evaluate Γ, and from equation 20

$$x_q=\frac{x_d}{\frac{\Gamma E_d}{2E}+1.0} \tag{23}$$

Test errors may occur in the determination of E_d and P_1. It can be shown that, if such errors do occur, the effect on x_q can be evaluated from the relationship

$$\frac{dx_q}{x_q}=-\left(1.0-\frac{x_q}{x_d}\right)\left(\Omega\frac{dP_1}{P_1}+(1.0-\Omega)\frac{dE_d}{E_d}\right) \tag{24}$$

where

$$\Omega=\frac{\sqrt{1.0+2\Gamma^2}-3.0}{\sqrt{1.0+2\Gamma^2}+1.0} \tag{25}$$

The error function Ω is plotted against Υ in Figure 7 for the range of Υ usually encountered. A study of this reveals that the value of x_q so determined is more affected by a test error in E_d than by a corresponding one in P_1.

The negative-excitation test for x_q, has an important advantage over both the slip and reluctance-torque methods of test in that a controllable low-line-voltage source is not required. Tests can be made on the user's premises where rated line voltage is frequently the only source of power, but, as in the reluctance-torque test, shaft power must be known, and this introduces the problem of measuring it.

Appendix F. Maximum-Lagging-Current Test for x_q

The maximum-lagging-current test for x_q is made in exactly the same manner as the negative-excitation test discussed in Appendix E. With the machine operating with a free shaft and with constant terminal voltage applied, the maximum stable per unit line current I_1, corresponding to the maximum stable negative excitation E_d, is found. x_q can then be found from these line-voltage and line-current values.

Neglecting losses and applying negative excitation as in Appendix E, the magnitude of negative excitation at the maximum stable operating point is, by solving equation 18, found to be

$$e_d=\frac{e(x_d-x_q)}{x_q}\,\frac{\cos 2\delta}{\cos\delta} \tag{26}$$

The general reactive-power-angle equation of a synchronous machine connected to an infinite bus is

$$Q=\frac{ee_d}{x_d}\cos\delta-\frac{e^2}{x_d}\cos^2\delta-\frac{e^2}{x_q}\sin^2\delta \tag{27}$$

When negative excitation, as given in equation 26, is applied to equation 27, the maximum stable reactive power is

$$Q_1=-\frac{e^2}{x_q}\left[1-\left(1-\frac{x_q}{x_d}\right)\sin^2\delta\right] \tag{28}$$

In an ideal machine operating at no load with no losses, the load angle δ and the active power P are both zero when maximum reactive power is reached.

For these conditions

$$Q_1=EI_1=-\frac{E^2}{x_q} \tag{29}$$

and in magnitude

$$x_q=\frac{E}{I_1} \tag{30}$$

In an actual machine, power must be developed to overcome friction and windage, and the load angle at the maximum stable point cannot be zero. However, neglecting armature losses, it can be shown as follows that for the usual range of machine constants and friction and windage losses, these effects produce negligible errors in equation 30.

If equation 26 is substituted in equation 9, the power P_1 with negative excitation and at the maximum stable point is

$$P_1=\frac{E^2(x_d-x_q)}{x_dx_q}\sin^2\delta\tan\delta \tag{31}$$

Figure 8 is a plot of equation 31 showing the relationship between δ and P_1x_d/E^2 over a range of machine constants. It can be seen from Figure 8 that at normal voltage and with usual values of x_d and P_1, δ may be as large as 20 degrees but probably will not exceed 15 degrees. If $\delta=20$ degrees, and $x_q/x_d=0.6$, equation 28 gives a value for Q_1 which is 95.7 per cent of the maximum possible value given by equation 29. Hence, x_q, as determined by equation 30, would be approximately five per cent high. The inclusion of armature losses will decrease this figure slightly. It is felt that this figure of five per cent is a reasonable maximum variation between x_q determined by this method and the correct value and that in most cases the error will be less than five per cent. Table I justifies this conclusion.

The terminal characteristics during a typical negative-excitation slip cycle have been recorded on oscillograms. Figure 9 shows the envelopes of the terminal voltage and armature current and a trace of the field current during one of these cycles. Previous to time $t=0$, the machine was operating at the maximum-stable-armature-current point of Figure 5. At $t=0$ the field voltage was slightly increased, and the machine became unstable and started to slip. As the slip increased, the armature current increased, reaching a maximum unstable value at $t=11$ seconds. As the unstable region was passed, the armature current fell until at $t=16$ seconds it was operating stably on the positive excitation side of the V curve of Figure 5. This phenomenon of rise and fall in current, when passing through the slip cycle, is readily apparent on test meters. The maximum stable current, not the maximum current during slipping, is used to determine x_q.

The maximum-lagging-current test for x_q has a great advantage over all other test methods in that the instruments required are perfectly standard and widely available. The test is made at rated voltage, and shaft power is not required to calculate the results. It is by far the simplest of the methods presented.

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Practical Calculation of Electrical Transients on Power Systems

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Synopsis: Solution of many power-circuit problems depends upon the magnitude, duration, and frequency of occurrence of transient voltages and currents produced by circuit changes. Circuit-opening or recovery-voltage transients are fundamental in the operation of interrupting devices and may be important from the standpoint of the dielectric strength of circuit elements. Circuit-closing transients may be important from the standpoint of inrush currents and occasionally from the standpoint of overvoltages. Both types of transients may be increased greatly if restriking and repetitive interruptions take place.

Transient voltages and currents resulting from circuit changes can be calculated by the classical method with the aid of differential equations or by the Heaviside method, particularly with the aid of the expansion theorem. Transients on complicated power systems are most conveniently determined by the miniature-system method introduced by Evans and Monteith and described in their 1937 AIEE paper.[4] Analytical methods are useful for simple problems and are essential for those beyond the range of the a-c network calculator or when it is not available. Analytical methods are also necessary for determining the proper form of the equivalent networks for the representation of power systems on the a-c network calculator. This circumstance provided the occasion for the particular investigation for which the present paper is a partial report.

This paper is devoted to the practical calculation of power-system transients and presents concepts, methods, and approximations selected with that objective in view. The paper describes a method of calculation particularly adapted for engineering work.

The scope of the present paper is limited to networks consisting of constant inductance and constant capacitance elements without loss. Consideration of such networks facilitates the presentation of the subject, and gives an adequate view of the phenomena involved. The presentation is from the standpoint of the electrical engineer who is accustomed to making steady-state calculations but who has not given the special study to the transient problem required by the mathematical approach generally used.

THE classical method of determining the transients produced by the application of voltage or current to a network is carried out by setting up a set of linear differential equations. The general solution of the set of equations for the case of a suddenly applied voltage may be written as follows:

$$i(t) = A_0(t) + A_1 \sin \omega_1 t + A_2 \sin \omega_2 t \ldots A_n \sin \omega_n t \quad (1)$$

In this equation, $\omega_1, \omega_2 \ldots \omega_n$ are the modes of oscillation of the network ($2\pi \times$ natural frequencies); $A_1, A_2 \ldots A_n$ are the constants that define the amplitudes of the corresponding modes; and $A_0(t)$ is the term that defines the steady-state current and the nonoscillatory components of the transient current. The integration constants $A_1 \ldots A_n$ can be determined from the boundary conditions in the usual manner. A corresponding equation may be written for the voltages produced by the application of current to a network.

The general solution of the transient problems in networks is readily accomplished in a formal manner, but considerable work remains before the numerical answers are obtained. Determination of the modes of oscillation, although required by all methods (at least for principal terms), involves considerable labor when the equations are of a high degree. Determination of the integration constants, while straightforward from a mathematical point of view, frequently offers considerable difficulty to engineers not accustomed to transient calculations. Because of the limitations of the classical method, the analytical work presented in this paper has been based on the Heaviside expansion theorem. Before taking up the method of analysis described in this paper, it is desirable to discuss in some detail the general properties of no-loss networks.

Properties of Networks

The reactance and susceptance characteristics of networks are fundamental to the method of analysis described in this paper and so will be discussed first. The reactance properties are used when the method is employed in solving circuit-closing problems, whereas the susceptance properties are used in circuit-opening problems. Accordingly, the discussion of networks is arranged in two parts, one for circuit-closing and the other for circuit-opening problems.

All no-loss two-terminal networks consisting of fixed circuit elements are of four types, depending only on the reactances at zero and at infinite frequencies. The principal characteristics of these four types of networks are given in Table I. The network can be typed readily by considering the character of the paths between the driving-point terminals as shown in the table. For example, type I networks have no paths between terminals containing inductance or capacitance alone, that is, all paths contain both elements. Representative networks of the four types are illustrated in the table. Angular velocity-reactance curves corresponding to the representative networks are also given in Table I. These curves are obtained by calculating the steady-state reactance as a function of angular velocity in the usual manner.

The network-reactance curves are discontinuous functions that start from negative infinity and increase to zero or to positive infinity or that start at zero and increase to positive infinity. In other words, all reactance curves have positive slopes[3] throughout their continuous portions. The infinite-reactance points or points of discontinuity are termed "poles," and the zero-reactance points are termed "zeros." These definitions are in accordance with accepted terminology and are used in the subsequent discussion of network characteristics. The "zeros" and "poles" at zero and at infinite frequencies are called "external zeros" and "external poles" and the remaining ones are called "internal zeros" and "internal poles." As can be seen readily from the reactance curves of Table I, the external zeros and poles also define the network type; for example, the type I network has poles at zero and at infinite frequencies. The number of internal zeros that a network has is equal to the number of modes of oscillation; this number is also equal to the maximum number of meshes containing both independent inductive and capacitive elements.

Basic equivalent networks in a form convenient for circuit-closing transients are also included in Table I for each network type. In the basic networks there is one branch containing both L and C for each internal zero; in addition, there are other branches containing only L or only C as required to correspond to the

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external zeros. A branch containing L alone is required when the network has a zero at zero frequency and a branch containing C alone is required when the network has a zero at infinite frequency. Thus a network of type IV, having zeros at zero and at infinite frequencies, requires one branch with L alone and one branch with C alone.

All no-loss two-terminal networks can also be classified according to the network susceptance at zero and infinite frequencies, as shown in Table II. The representative networks in Table II are identical to the corresponding types in Table I. The angular velocity-susceptance curves are included in the table for each network type. Basic equivalent networks of the form convenient for circuit-opening transients are also included in Table II.

Method of Zeros and Slopes

As the method of analysis to be described depends upon the zeros and slopes of the reactance (or susceptance) curves for the networks, it is referred to as the method of "zeros and slopes." The method is described under two headings:

(*a*). Unit voltage applied.

(*b*). Unit current applied.

The method will be extended for the application of a-c voltages and currents in a later section.

(a). UNIT VOLTAGE APPLIED

The response of a network to a suddenly applied unit voltage is given by the Heaviside expansion theorem as follows:

$$i(t)=i_s(t)+\sum_{k=1}^{k=2n}\frac{e^{p_kt}}{p_kZ'(p_k)} \qquad (2)$$

In this form of the expansion theorem, the function $i_s(t)$ defines the steady-state network solution and that part of the transient solution determined by the external zeros. The term $Z'(p)$ is the first derivative of $Z(p)$ with respect to p, where $Z(p)$ is the impedance function of the network as viewed from the driving point. The summation indicates that there are as many terms in the transient solution (neglecting the $i_s(t)$ term) as there are roots of the equation $Z(p)=0$. The quantity p_k is a general expression for the roots of $Z(p)=0$.

It is shown in the appendix that when restricted to no-loss networks, equation 2 can be written in the following approximate form:

$$i(t)=i_s(t)+A_1\sin\omega_1t+A_2\sin\omega_2t+\ldots+A_n\sin\omega_nt \qquad (3)$$

where

$$A_1=\frac{2}{\omega_1\dfrac{\Delta X}{\Delta\omega_1}};\quad A_2=\frac{2}{\omega_2\dfrac{\Delta X}{\Delta\omega_2}};\quad\ldots A_n=\frac{2}{\omega_n\dfrac{\Delta X}{\Delta\omega_n}}$$

The $i_s(t)$ term defines the steady-state current and the components of the transient current corresponding to the external zeros. This term is given in Table III for each type of network. The term t/L_0 defines a current starting at zero and increasing directly with time to an infinite value at $t=\infty$. The term $\left[\dfrac{t}{C_0}\right]_{t=0}$ defines a current having an infinite magnitude at $t=0$ and equal to zero thereafter, such as instantly to bring the charge on the capacitance to the applied voltage. The quantity L_0 is defined as the equivalent inductance between driving-point terminals with all capacitances open-circuited, and C_0 is the equivalent capacitance with all inductances open-circuited.

In equation 3, ω_1, ω_2, ... ω_n are the values of angular velocity corresponding to zero reactance between the driving-point terminals, and $\dfrac{\Delta X}{\Delta\omega_1}$, $\dfrac{\Delta X}{\Delta\omega_2}$, ... $\dfrac{\Delta X}{\Delta\omega_n}$ are the slopes of the angular velocity-reactance curve at the corresponding values of angular velocity. This relation shows that the response of a no-loss network can be determined from the steady-state reactance characteristics of the network. An approximate solution can, therefore, be obtained by calculating the angular velocity-reactance curve and then determining the slopes of the curve at or near the zeros. This procedure is illustrated in Figure 1 for a relatively simple circuit. It should be noted that it is not necessary to obtain the complete curve but only those parts in the vicinity of the zeros. The number of points that must be calculated will depend upon the accuracy desired and on the ability of the operator to estimate the zeros. The accuracy of the solution will depend entirely on the accuracy with which the zeros and slopes are evaluated.

(b). UNIT CURRENT APPLIED

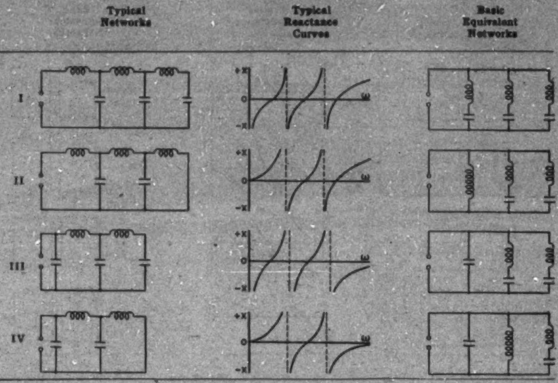
The problem of calculating the transient voltage following the opening of a circuit can be analyzed in terms of the original current and a suddenly applied equal and opposite current.* The superposition of these two currents reduces the total current to zero, which is equivalent to opening the circuit. Thus, the first step is to calculate the network response to unit current.

The response of a network to a suddenly applied unit current can also be written in a form similar to equation 3.

$$e(t)=e_s(t)+A_a\sin\omega_at+A_b\sin\omega_bt+\ldots+A_m\sin\omega_mt \qquad (4)$$

Table I. Classification of Networks for Circuit-Closing Transients

Type	X at $\omega=\infty$	X at $\omega=0$	Paths	
			C Alone	L Alone
I	∞	∞	None	None
II	∞	0	None	At least one
III	0	∞	At least one	None
IV	0	0	At least one	At least one



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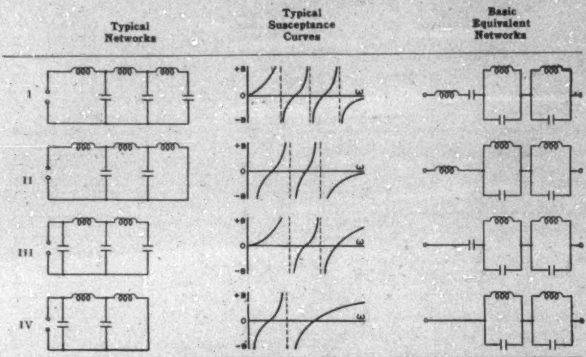
* Page 306 of reference 1.

Table II. Classification of Networks for Circuit-Opening Transients

Type	B at $\omega=\infty$	B at $\omega=0$	Paths: C Alone	Paths: L Alone
I	0	0	None	None
II	0	∞	None	At least one
III	∞	0	At least one	None
IV	∞	∞	At least one	At least one



where

$$A_a=\frac{2}{\omega_a\frac{\Delta B}{\Delta\omega_a}};\ A_b=\frac{2}{\omega_b\frac{\Delta B}{\Delta\omega_b}};\ \ldots A_m=\frac{2}{\omega_m\frac{\Delta B}{\Delta\omega_m}}$$

The $e_o(t)$ term defines the steady-state voltage and the components of the transient voltage corresponding to the external zeros. This term is given in Table III for each type of network. The term t/C_s defines a voltage starting at zero and increasing directly with time to an infinite value at $t=\infty$. The term $\left[\frac{L_s}{t}\right]_{t=0}$ defines a voltage having an infinite magnitude at $t=0$ and equal to zero thereafter. The quantity C_s is the equivalent capacitance between driving-point terminals with all inductances short-circuited, and L_s is the equivalent inductance with all capacitances short-circuited.

Table III

Network Type	$i_o(t)$	$e_o(t)$
I	0	$\left[\frac{L_s}{t}\right]_{t=0}+\frac{t}{C_s}$
II	$\frac{t}{L_o}$	$\left[\frac{L_s}{t}\right]_{t=0}$
III	$\left[\frac{C_o}{t}\right]_{t=0}$	$\frac{t}{C_s}$
IV	$\frac{t}{L_o}+\left[\frac{C_o}{t}\right]_{t=0}$	0

L_s = Equivalent L determined with all C's short-circuited

L_o = Equivalent L determined with all C's open-circuited

C_o = Equivalent C determined with all L's open-circuited

C_s = Equivalent C determined with all L's short-circuited

In equation (4), $\omega_a, \omega_b, \ldots \omega_m$ are the values of angular velocity resulting in zero susceptance between driving-point terminals, and $\frac{\Delta B}{\Delta\omega_a}, \frac{\Delta B}{\Delta\omega_b}, \ldots \frac{\Delta B}{\Delta\omega_m}$ are the slopes of the angular velocity-susceptance curve at the corresponding points. The response of a no-loss network to unit current can, therefore, be obtained in a manner similar to that illustrated for the response to unit voltage. However, in this case the steady-state susceptance characteristics of the network are used instead of the reactance characteristics.

Equivalent Networks

Every network can be reduced to an equivalent network[2,3] of either the shunt or series forms, illustrated in Figure 2. The basic equivalent network of the shunt form is advantageous for circuit-closing transients because of the lack of coupling between branches for this type of circuit change. Similarly, the basic equivalent network of the series form is advantageous for circuit-opening transients. These equivalent networks are helpful in visualizing the properties of networks and in interpreting the equations of transient response. Furthermore, the replacement of a given complicated network by the simplest equivalent network of a given form is advantageous under many circumstances for calculation by analytical or a-c network calculator methods.[4]

(a). Circuit-Closing Transients

The basic equivalent network in the shunt form of Table I can be derived from the network response to unit voltage as given by equation 3. For convenience this equation has been rewritten with the $i_o(t)$ term expanded as given in Table III:

$$i(t)=\frac{t}{L_o}+\left[\frac{C_o}{t}\right]_{t=0}+A_1\sin\omega_1t+ A_2\sin\omega_2t\ldots A_n\sin\omega_nt \quad \text{(5a)}$$

$$=\frac{t}{L_o}+\left[\frac{C_o}{t}\right]_{t=0}+\frac{2}{\omega_1\frac{\Delta X}{\Delta\omega_1}}\sin\omega_1t+ \frac{2}{\omega_2\frac{\Delta X}{\Delta\omega_2}}\sin\omega_2t\ldots\frac{2}{\omega_n\frac{\Delta X}{\Delta\omega_n}}\sin\omega_nt \quad \text{(5b)}$$

The basic equivalent network of the shunt form is illustrated in Figure 2*a*. Each branch of this equivalent network corresponds to one term of the network solution given by equation 5. The L_o and C_o branches correspond, respectively, to an equivalent inductance or an equivalent capacitance in parallel with the driving-point terminals, as defined in Table III. As the response of a simple series LC circuit to unit voltage is $\sqrt{\frac{C}{L}}\sin\omega t$, where $\omega=\frac{1}{\sqrt{LC}}$, equation 5 can be written in the following form:

$$i(t)=\frac{t}{L_o}+\left[\frac{C_o}{t}\right]_{t=0}+\sqrt{\frac{C_1}{L_1}}\sin\omega_1t+ \sqrt{\frac{C_2}{L_2}}\sin\omega_2t\ldots\sqrt{\frac{C_n}{L_n}}\sin\omega_nt \quad \text{(6)}$$

where

$$\omega_1=\frac{1}{\sqrt{L_1C_1}};\ \omega_2=\frac{1}{\sqrt{L_2C_2}};\ \ldots\ \omega_n=\frac{1}{\sqrt{L_nC_n}}$$

By equating the coefficients of equations 5*a* and 6, the remaining circuit elements of the equivalent network can be determined as follows:

$$\left.\begin{array}{l}
A_1=\sqrt{\frac{C_1}{L_1}};\quad A_2=\sqrt{\frac{C_2}{L_2}};\quad \ldots\ A_n=\sqrt{\frac{C_n}{L_n}}\\
\omega_1=\frac{1}{\sqrt{L_1C_1}};\ \omega_2=\frac{1}{\sqrt{L_2C_2}};\ \ldots\omega_n=\frac{1}{\sqrt{L_nC_n}}\\
C_1=\frac{A_1}{\omega_1};\quad C_2=\frac{A_2}{\omega_2};\quad \ldots\ C_n=\frac{A_n}{\omega_n}\\
L_1=\frac{1}{\omega_1A_1};\quad L_2=\frac{1}{\omega_2A_2};\quad \ldots\ L_n=\frac{1}{\omega_nA_n}
\end{array}\right\} \quad \text{(7)}$$

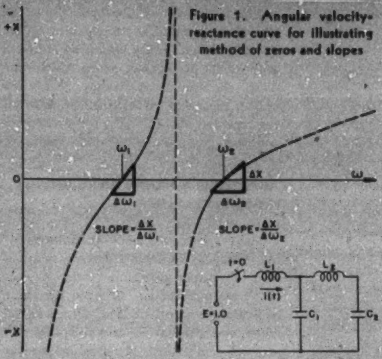


Figure 1. Angular velocity-reactance curve for illustrating method of zeros and slopes

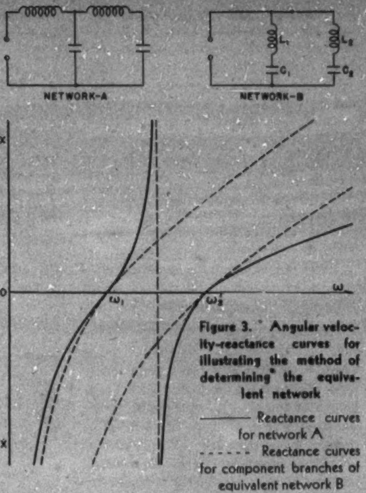
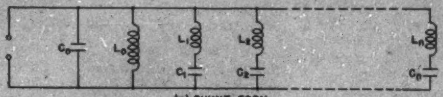


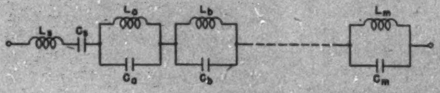
Figure 3. Angular velocity-reactance curves for illustrating the method of determining the equivalent network

——— Reactance curves for network A

- - - - - - Reactance curves for component branches of equivalent network B



(a) SHUNT FORM



(b) SERIES FORM

Figure 2 (left). Basic equivalent networks in the shunt and series forms

By equating the coefficients of equations 5*b* and 6, the circuit elements of the equivalent network can be stated directly in terms of the zeros and slopes:

$$\left.\begin{aligned}
&\sqrt{\frac{C_1}{L_1}}=\frac{2}{\omega_1\dfrac{\Delta X}{\Delta\omega_1}};\quad \sqrt{\frac{C_2}{L_2}}=\frac{2}{\omega_2\dfrac{\Delta X}{\Delta\omega_2}};\\
&\qquad\qquad \ldots\sqrt{\frac{C_n}{L_n}}=\frac{2}{\omega_n\dfrac{\Delta X}{\Delta\omega_n}}\\
&\omega_1=\frac{1}{\sqrt{L_1C_1}};\quad \omega_2=\frac{1}{\sqrt{L_2C_2}};\\
&\qquad\qquad \ldots\omega_n=\frac{1}{\sqrt{L_nC_n}}\\
&L_1=\frac{1}{2}\frac{\Delta X}{\Delta\omega_1};\quad L_2=\frac{1}{2}\frac{\Delta X}{\Delta\omega_2};\quad \ldots L_n=\frac{1}{2}\frac{\Delta X}{\Delta\omega_n}\\
&C_1=\frac{1}{\omega_1^2L_1};\quad C_2=\frac{1}{\omega_2^2L_2};\quad \ldots C_n=\frac{1}{\omega_n^2L_n}
\end{aligned}\right\}\tag{8}$$

These relations show that the circuit elements of the equivalent network can be determined directly from the angular velocity-reactance curves. This is graphically illustrated in Figure 3 for a simple two-mesh network. The reactance curves are shown for the original network and for each branch of the equivalent network. As the two networks are equivalent the reactance of network *A* is equal to the reactance of network *B*, which, of course, is equal to the parallel of the reactances of the individual branches. Each zero for the reactance of the total network occurs at the zero for a component branch and the corresponding slopes at these zeros are identical. This results from the fact that at the zero for one branch the reactance of the other branch becomes negligibly high in comparison with the branch under consideration. Thus, for each internal zero of the angular velocity-reactance curve there is an equivalent shunt branch containing *L* and *C*, the values of which are defined by the corresponding zero and slope. This same reasoning can be extended to any number of branches.

(b). Circuit-Opening Transients

The basic equivalent network in the series form of Table II can readily be derived from equation 4 for the response to unit current applied. For convenience this equation has been rewritten with the $e_a(t)$ term expanded as given in Table III:

$$e(t)=\frac{t}{C_s}+\left[\frac{L_s}{t}\right]_{t=}+A_a\sin\omega_a t+ A_b\sin\omega_b t\ldots+A_m\sin\omega_m t \tag{9a}$$

$$=\frac{t}{C_s}+\left[\frac{L_s}{t}\right]_{t=0}+\frac{2}{\omega_a\dfrac{\Delta B}{\Delta\omega_a}}\sin\omega_a t+ \frac{2}{\omega_b\dfrac{\Delta B}{\Delta\omega_b}}\sin\omega_b t\ldots\frac{2}{\omega_m\dfrac{\Delta B}{\Delta\omega_m}}\sin\omega_m t \tag{9b}$$

Table IV. Unit Voltage Applied

(1) ω	(2) ωL₂	(3) 1/ωC₂	(4) (2)+(3)	(5) 1/(4)	(6) ωC₁	(7) (5)+(6)	(8) 1/(7)	(9) ωL₁	X (8)+(9)
				x10⁻⁴	x10⁻⁴	x10⁻⁴			
2,701	2,701	−3,702.3	−1,001.3	9.987	27.01	36.997	−270.29	270.1	−0.19
2,702	2,702	−3,700.9	−998.9	10.011	27.02	37.031	−270.04	270.2	+0.16
3,701	3,701	−2,701.9	−999.1	10.009	37.01	27.001	−370.35	370.1	−0.25
3,702	3,702	−2,701.2	−1,000.8	9.992	37.02	27.028	−369.98	370.2	+0.22

$\omega_1=2{,}701$

$\Delta\omega_1=1.0$

$\Delta X=0.19+0.16=0.35$

$\dfrac{\Delta X}{\Delta\omega_1}=0.35$

$\dfrac{2}{\omega_1\dfrac{\Delta X}{\Delta\omega_1}}=0.00212$

$\omega_2=3{,}701$

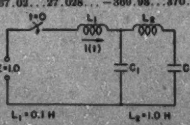
$\Delta\omega_2=1.0$

$\Delta X=0.47$

$\dfrac{\Delta X}{\Delta\omega_2}=0.47$

$\dfrac{2}{\omega_2\dfrac{\Delta X}{\Delta\omega_2}}=0.00115$

$i(t)=0.00212\sin 2{,}701t+0.00115\sin 3{,}701t$



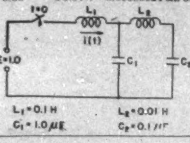
L₁=0.1 H
C₁=1.0 μF

L₂=1.0 H
C₂=0.1 μF

Table V. Unit Voltage Applied

ω	X	$\frac{\Delta X}{\Delta\omega}$
3,013	−0.17	
3,014	+0.03	0.20
33,100	+4,073	
33,200	+1,969	
33,179	−191	
33,180	+61	252

$i(t)=0.00$[illegible] sin $3.014t+0.00000024\sin 33{,}179t$



The basic equivalent network of the series form is illustrated in Figure 2*b*. Each branch of this equivalent network corresponds to one term of the network solution given by equation 9. The L_s and C_s branches correspond to an equivalent inductance or an equivalent capacitance in series with the driving point, as defined in Table III. As the response of a simple parallel *LC* circuit to unit current is equal to $\sqrt{\frac{L}{C}}\sin\omega t$, where $\omega=\frac{1}{\sqrt{LC}}$, equation 9 can be written in the following form:

$$e(t)=\frac{t}{C_s}+\left[\frac{L_s}{t}\right]_{t=0}+\sqrt{\frac{L_a}{C_a}}\sin\omega_a t+\sqrt{\frac{L_b}{C_b}}\sin\omega_b t\ldots\sqrt{\frac{L_m}{C_m}}\sin\omega_m t \quad (10)$$

where

$$\omega_a=\frac{1}{\sqrt{L_aC}};\ \omega_b=\frac{1}{\sqrt{L_bC_b}};\ldots\omega_m=\frac{1}{\sqrt{L_mC_m}}$$

By equating the coefficients of equations 9*a* and 10, the remaining circuit elements of the equivalent network can be determined as follows:

$$\left.\begin{aligned}
&A_a=\sqrt{\frac{L_a}{C_a}};\quad A_b=\sqrt{\frac{L_b}{C_b}};\quad\ldots A_m=\sqrt{\frac{L_m}{C_m}}\\
&\omega_a=\frac{1}{\sqrt{L_aC_a}};\quad \omega_b=\frac{1}{\sqrt{L_bC_b}};\\
&\qquad\qquad\ldots\omega_m=\frac{1}{\sqrt{L_mC_m}}\\
&C_a=\frac{1}{\omega_aA_a};\quad C_b=\frac{1}{\omega_bA_b};\quad\ldots C_m=\frac{1}{\omega_mA_m}\\
&L_a=\frac{A_a}{\omega_a};\quad L_b=\frac{A_b}{\omega_b};\quad\ldots L_m=\frac{A_m}{\omega_m}
\end{aligned}\right\} \quad (11)$$

By equating coefficients of equations 9*b* and 10, the circuit elements of equivalent networks can be stated directly in terms of the zeros and slopes:

$$\left.\begin{aligned}
&\sqrt{\frac{L_a}{C_a}}=\frac{2}{\omega_a\frac{\Delta B}{\Delta\omega_a}};\quad \sqrt{\frac{L_b}{C_b}}=\frac{2}{\omega_b\frac{\Delta B}{\Delta\omega_b}};\\
&\qquad\qquad\ldots\sqrt{\frac{L_m}{C_m}}=\frac{2}{\omega_m\frac{\Delta B}{\Delta\omega_m}}\\
&\omega_a=\frac{1}{\sqrt{L_aC_a}};\quad \omega_b=\frac{1}{\sqrt{L_bC_b}};\\
&\qquad\qquad\ldots\omega_m=\frac{1}{\sqrt{L_mC_m}}\\
&C_a=\frac{1}{2}\frac{\Delta B}{\Delta\omega_a};\quad C_b=\frac{1}{2}\frac{\Delta B}{\Delta\omega_b};\ldots C_m=\frac{1}{2}\frac{\Delta B}{\Delta\omega_m}\\
&L_a=\frac{1}{\omega_a^2C_a};\quad L_b=\frac{1}{\omega_b^2C_b};\quad\ldots L_m=\frac{1}{\omega_m^2C_m}
\end{aligned}\right\} \quad (12)$$

In the preceding discussion the basic equivalent networks have been derived from network-response equations based on the Heaviside expansion theorem. By postulating that every network can be reduced to one of the basic forms of equivalent circuits, it is possible to derive the network response equations 3 and 4 without the use of the expansion theorem. Thus, the equivalent circuits provide not only a means of visualizing the network response but also the basis for an alternative derivation of the response equations.

Table VI. Alternating Current Applied

(1) ω	(2) ωL_2	(3) $1/\omega C_2$	(4) (2)+(3)	(5) 1/(4)	(6) ωC_1	(7) $1/\omega L_1$	B (5)+(6)+(7)
				$\times 10^{-4}$	$\times 10^{-4}$	$\times 10^{-4}$	$\times 10^{-4}$
2,701	2,701	−3,702.30	−1,001.30	9.987	27.01	−37.023	−0.026
2,702	2,702	−3,700.96	−998.96	10.011	27.02	−37.009	+0.021
3,701	3,701	−2,701.97	999.03	−10.009	37.01	−27.019	−0.018
3,702	3,702	−2,701.24	1,000.80	−9.992	37.02	−27.012	+0.016
377	377	−26,500	−26,100	0.383	3.77	−265	−261

$\omega_a=2{,}701$ $\qquad$ $\omega_b=3{,}701$

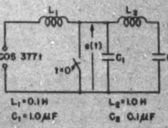
$\frac{\Delta B}{\Delta\omega_a}=0.047\times10^{-4}$ $\qquad$ $\frac{\Delta B}{\Delta\omega_b}=0.034\times10^{-4}$

$A_a=157$ $\qquad$ $A_b=159$

Fault current $=\frac{100}{j37.7}\cos 377t$

$$=2.65\cos\left(377t-\frac{\pi}{2}\right)$$

$I(t)=2.65\cos\left(377t-\frac{\pi}{2}\right)$



Steady-state component of $e(t)=\frac{2.65}{-j0.0261}\cos\left(377t-\frac{\pi}{2}\right)=102\cos 377t$

$e(t)=102\cos 377t-59\cos 2{,}701t-44\cos 3{,}701t$

Alternating Voltages and Currents Applied

The response of a network to alternating voltages or currents can be obtained from the response to unit voltage or current by superposition, or from the basic equivalent networks. The equations relating the alternating and unit-function responses have been derived and are included here for reference.

(a). ALTERNATING VOLTAGE APPLIED

Let

$$i(t)=\frac{t}{L_o}+\left[\frac{C_o}{t}\right]_{t=0}+\sum_{k=1}^{k=n}A_k\sin\omega_kt$$

be the network response to unit voltage. The response of the network to the sudden application of the voltage $E(t)=E\cos(\omega_ot+\phi)$ is then defined by the following equation:

$$\left.\begin{aligned}
i(t)=&\left[\frac{E}{\omega_oL_o}\sin(\omega_ot+\phi)-\frac{E}{\omega_oL_o}\sin\phi\right]+\\
&\left[-E\omega_oC_o\sin(\omega_ot+\phi)+\left(\frac{EC_o\cos\phi}{t}\right)_{t=0}\right]+\\
&\left[-E\sum_{k=1}^{k=n}\frac{\omega_o\omega_kA_k}{(\omega_k^2-\omega_o^2)}\sin(\omega_ot+\phi)+\right.\\
&\left.E\sum_{k=1}^{k=n}\frac{\omega_k^2A_k}{(\omega_k^2-\omega_o^2)}\left(\cos\phi\sin\omega_kt+\frac{\omega_o}{\omega_k}\sin\phi\cos\omega_kt\right)\right]
\end{aligned}\right\} \quad (13)$$

Each of the bracketed terms in the preceding equation is the a-c solution corresponding to one term in the unit voltage response equation or for one branch of the equivalent network. The first term in each bracket is a component of the steady-state response of the network, whereas the second term is in each case a transient response. In many cases less work is involved in determining the steady-state response directly from the network reactance at $\omega=\omega_o$. If this reactance is equal to $X(j\omega_o)$, then the steady-state response is equal to

$$\frac{E}{X(j\omega_o)}\cos(\omega_ot+\phi)$$

(b). ALTERNATING CURRENT APPLIED

Let

$$e(t)=\frac{t}{C_s}+\left[\frac{L_s}{t}\right]_{t=0}+\sum_{k=1}^{k=n}A_k\sin\omega_k t$$

be the response for unit current applied. The network response to the current $I(t)=I\cos(\omega_0 t+\phi)$ is then defined by the following relations:

$$\left.\begin{aligned}e(t)=&\left[\frac{I}{\omega_0C_s}\sin(\omega_0 t+\phi)-\frac{I}{\omega_0C_s}\sin\phi\right]+\\&\left[-I\omega_0L_s\sin(\omega_0 t+\phi)+\left(\frac{IL_s\cos\phi}{t}\right)_{t=0}\right]+\\&\left[-I\sum_{k=1}^{k=n}\frac{\omega_0\omega_kA_k}{(\omega_k^2-\omega_0^2)}\sin(\omega_0 t+\phi)+\right.\\&I\sum_{k=1}^{k=n}\frac{\omega_k^2A_k}{(\omega_k^2-\omega_0^2)}\left(\cos\phi\sin\omega_k t+\right.\\&\left.\left.\frac{\omega_0}{\omega_k}\sin\phi\cos\omega_k t\right)\right]\end{aligned}\right\}\qquad(14)$$

Each of the bracketed terms in the preceding equation is the a-c solution corresponding to one term of the response equation for unit current applied, or for one branch of the equivalent network. The first term in each bracket is the steady-state response, and the second term is the transient response. The steady-state response can also be obtained from the network admittance for $\omega=\omega_0$. If this admittance is represented by $B(j\omega_0)$, the steady-state response is equal to

$$\frac{I}{B(j\omega_0)}\cos(\omega_0 t+\phi)$$

One special case of the foregoing solution is of general interest, the case where the applied current is equal to $I\sin\omega_0 t=I\cos(\omega_0 t-\pi/2)$:

$$\left.\begin{aligned}e(t)=&\left[-\frac{E}{\omega_0C_s}\cos\omega_0 t+\frac{I}{\omega_0C_s}\right]+\\&\left[I\omega_0L_s\cos\omega_0 t\right]+\\&\left[I\sum_{k=1}^{k=n}\frac{\omega_0\omega_kA_k}{(\omega_k^2-\omega_0^2)}\cos\omega_0 t-\right.\\&\left.I\sum_{k=1}^{k=n}\frac{\omega_0\omega_kA_k}{(\omega_k^2-\omega_0^2)}\cos\omega_k t\right]\end{aligned}\right\}\qquad(15)$$

Illustrative Examples

The method of analysis just described is illustrated in Table IV for a relatively simple circuit. The problem in this case is to find an analytical expression for the current in the inductance L_1 when unit voltage is suddenly applied to the driving-point terminals. The zeros are first located approximately by rough slide-rule calculations, preferably using a plot of the reactance curve as a guide. The reactance of the circuit, as viewed from the driving point, is calculated by combining the reactance of the individual branches as illustrated in the table. It should be noted that it is not necessary to obtain the complete reactance curve but only those parts of the curve in the vicinity of zero reactance. Only two points near each zero are included in the illustration, these points having been arrived at after other preliminary points were obtained. The reactance curves need not be plotted except as a guide; all necessary information is included in the tabulated data. In this example $\Delta\omega$ was taken as one radian per second in order to obtain an accurate evaluation of $\Delta X/\Delta\omega$. The angular velocity increment to use in any particular case depends upon the network characteristics and upon the accuracy desired. It should be stressed that the reactance calculations should be carried out to a high degree of accuracy if a high degree of accuracy is desired in the final network response. However, if practical engineering accuracy is sufficient then less accuracy is required in calculating the reactances.

After the zeros and slopes are obtained the response is calculated by substituting these values in equation 3. As the network considered in this example is of type I, $i_e(t)$ is equal to zero and so the complete response is as given at the bottom of Table IV.

The accuracy of the method can readily be checked by noting the initial conditions. At $t=0$, the voltage across C_1 is zero, so the applied unit voltage must be consumed across L_1, that is, $L_1\dfrac{di(t)}{dt}$ should be equal to unity at $t=0$. For Table IV, the check gives

$$L_1\frac{di(t)}{dt}=0.1(2{,}701\times0.00212+3{,}701\times0.00115)=0.998\text{ at }t=0$$

which is accurate to within 0.2 of one per cent.

In the general case the calculated responses for networks of types I and II are checked by noting that $L_e\dfrac{di(t)}{dt}=1.0$ at $t=0$, assuming unit voltage applied, where L_e is the equivalent inductance between the driving-point terminals with all capacitances short-circuited.

In special cases some difficulty may be experienced in locating the zeros. For example, the reactance of the network in Table V was calculated at $\omega=33{,}100$ and 33,200 and the resulting reactances were both positive and decreasing in magnitude. These results incorrectly may be assumed to indicate that a zero would be present at some higher angular velocity, whereas the zero is actually between these values of angular velocity. When such cases are encountered much time is usually required to locate the zero. However, when the zeros are hard to locate, the slope of the reactance curve is large and as a result the transient current corresponding to the zero may be an unimportant component of the network response. In Table V the second component of current is only 0.00723 per cent of the first component and so is insignificant. This situation could have been predicted by checking the first component of current with the initial conditions. Neglecting the second term,

$$L_1\frac{di(t)}{dt}=0.1(0.00332)(3{,}014)=1.0\text{ at }t=0$$

which satisfies the boundary conditions. It is very helpful to make these checks for each component as soon as it is obtained, in order to obtain not only a check on the integrity of the calculation but also a continuous indication of the relative importance of the remaining terms of the solution.

The problem of calculating the transient voltage following the sudden opening of a switch can be treated in terms of a suddenly applied current. Considering the network in Table VI, the current flowing in the closed switch is equal to $+2.65\sin 377t$. The current in the switch can be reduced to zero, which is equivalent to opening the switch, by inserting a current $+2.65\sin 377t$ in a direction opposite to the initial current. In making the calculations with the method of zeros and slopes, the response to unit current is first obtained, as illustrated in Table VI, by determining the zeros and slopes of the susceptance curves. The response to the current $+2.65\sin 377t$ is then found by using the relations given in part *b* of the preceding section. The steady-state component of the recovery voltage was found by first calculating the network susceptance at $\omega=377$.

Extensions of the Method

In the paper the calculating procedure has been illustrated only for the zeros and slopes derived from the constants of the networks. However, it is possible and sometimes convenient to form the analytical expression for the impedance (or admittance) function and its derivative and to use the first or both of these to determine the zeros and slopes. The method of analysis and calculating procedure have been discussed only for problems involving driving-point functions. However, they are also applicable to problems involving transfer functions, for example, the determination of current in a particular branch of a network resulting from the application of potential to the driving-point terminals. The procedure is the same except that for the latter case the zeros and slopes are obtained from the transfer function instead of the driving-point function. Angular velocity-reactance (or susceptance) curves, derived from measurements on a miniature-system setup or even from an actual system instead of from calculated data as described in the paper, can be used for the determination of zeros and slopes.

Appendix

The response of a network to a suddenly applied unit voltage is given by the Heaviside expansion theorem as follows:

$$i(t)=i_0(t)+\sum_{k=1}^{k=2n}\frac{e^{p_kt}}{p_kZ'(p_k)} \tag{1}$$

In this form of the expansion theorem the function $i_0(t)$ defines the steady-state network solution and that part of the transient solution determined by the external zeros. Referring to the second term, $Z(p)$ is the impedance function of the network as viewed from the driving point and $Z'(p)$ is the first derivative of the impedance function with respect to p. The summation indicates that there are as many terms in the transient solution (neglecting the $i_0(t)$ term) as there are roots of the equation $Z(p)=0$. The quantity p_k is a general expression for the roots of $Z(p)=0$.

In a no-loss network the roots of $Z(p)=0$ are pure imaginaries appearing in conjugate pairs and so can be represented by $p=\pm j\omega$. It should also be noted that in no-loss network $Z'(j\omega_k)=Z'(-j\omega_k)$. Substituting these relations into the expansion theorem reduces equation 1 to

$$i(t)=i_0(t)+\sum_{k=1}^{k=n}\frac{e^{j\omega_kt}}{j\omega_kZ'(j\omega_k)}-\sum_{k=1}^{k=n}\frac{e^{-j\omega_kt}}{j\omega_kZ'(j\omega_k)} \tag{2}$$

This form of the theorem can then be converted to the trigonometric form, remembering that

$$\frac{e^{j\omega t}-e^{-j\omega t}}{2j}=\sin\omega t,$$

$$i(t)=i_0(t)+\sum_{k=1}^{k=n}\frac{2}{\omega_kZ'(j\omega_k)}\sin\omega_kt \tag{3}$$

In a no-loss network $Z(j\omega)$ is recognized as the alternating-current reactance of the network as measured at the driving point, and $\omega_k=\omega_1, \omega_2 \ldots . n$ are the angular velocities that give zero reactance. The term $Z'(j\omega)$ is, therefore, the slope of the reactance curve plotted as a function of $j\omega$, and $Z'(j\omega_k)$ is the slope at $\omega=\omega_k$. It becomes apparent, therefore, that the constants of integration, or the coefficients of the series of sine functions, depend only on the angular velocities resulting in zero reactance, and on the slope of the reactance curve at these corresponding velocities.

In making numerical computations, equation 3 is more useful in its approximate form,

$$i(t)=i_0(t)+\sum_{k=1}^{k=n}\frac{2}{\omega_k\dfrac{\Delta X}{\Delta\omega_k}}\sin\omega_kt \tag{4}$$

or with alternative notation,

$$i(t)=i_0(t)+A_1\sin\omega_1t+A_2\sin\omega_2t+\ldots A_n\sin\omega_nt \tag{5}$$

where

$$A_1=\frac{2}{\omega_1\Delta X/\Delta\omega_1};\quad A_2=\frac{2}{\omega_2\Delta X/\Delta\omega_2};\ldots$$

$$A_n=\frac{2}{\omega_n\Delta X/\Delta\omega_n}$$

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The Cause and Control of Some Types of Switching Surges

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AMONG the several causes other than lightning which can result in power-system overvoltages, switching surges rank high in importance. In the past the magnitudes of switching surges have been considered on a statistical basis, but the need for frequent switching operations naturally led to their study and to analyses of their causes and magnitudes.

Switching surges are transient overvoltages caused by closing or opening a circuit and can vary in magnitude and duration over wide ranges. As contrasted with lightning surges which are generally impulsive and unidirectional in character, switching surges are oscillatory in nature, of frequency anywhere from very high to fundamental or subharmonic frequency, and of duration from a small portion of a cycle to a relatively long time which may be governed by the time between the opening or closing of successive phases on a three-phase system. Where steepness of voltage wave front is a consideration, it should be recognized that surges caused by switching on certain circuits may have wave fronts steeper than the ordinary lightning surge.

Switching in circuits supplying ungrounded transformer banks can result in high voltages during the switching period if the interval between closing or opening the first and last phase in a three-phase system is long enough for the intermediate steady-state voltage conditions to be attained. This condition may obtain during single-phase switching on a three-phase system, or when one or two conductors are open by virtue of conductor breaks or blown fuses. The voltages obtained under these conditions have been studied and reported[1] for a wide range of constants. The principal constants which apply are the transformer magnetizing reactance, the system capacitance between the switch and the transformer, and the degree of transformer saturation. This paper in general, will, consider over-voltages other than these, which occur even with reasonably simultaneous action of the switches in the three poles. Primary consideration will be given to transient voltages caused by normal rather than fault switching operations.

Conclusions Drawn

Some of the conclusions which may be drawn from the analyses and discussion are as follows:

1. When a switch on an unfaulted power system is being closed, transient voltages as

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high as nearly twice normal line-to-neutral voltage may be obtained. This assumes that closing of the three poles of the switch is reasonably simultaneous, as would be true of the ordinary circuit breaker.

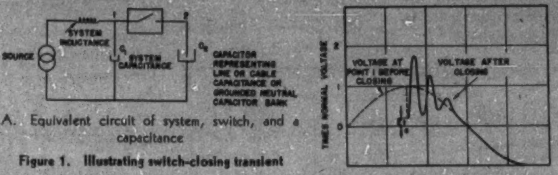
2. Transformer windings can be very effective in discharging the stored energy of a capacitive circuit being opened and thus reduce the possibility and magnitude of restriking overvoltages.

If the breaker is connected to the line, cable, or capacitor bank through a delta-wye-grounded transformer bank as shown in Figure 3B, the overvoltage under restriking conditions will be limited to less than twice normal line-to-ground voltage if the transformer has a kilovolt-ampere rating equal to or greater than the charging kilovolt-amperes of the capacitive circuit. Simultaneous clearing of the three phases is not required for this statement to hold true.

Where switching is done at line voltage, transformer windings switched with the line, as shown in Figure 3A, are also effective if the ratio of the capacitive reactance being switched to the transformer magnetizing reactance, both on a line-to-neutral basis (this may be considered the ratio of transformer magnetizing kilovolt-amperes to capacitance kilovolt-amperes), falls between the approximate limits of 0.0012 and 0.02 or 0.45 and ∞. In the region between 0.02 and 0.45, reasonably simultaneous opening of the three phases is essential to keeping the switching surge voltages low.

3. The prevention of overvoltages of harmful magnitudes, which may occur in certain limited fields on account of the forcing of the current zero by the circuit breaker on interrupting, may be accomplished by the use of shunt capacitors. The minimum capacitor kilovolt-ampere value for satisfactory performance depends upon the characteristics of the circuit, and the breaker, and the allowable overvoltage magnitude.

It is not intended to convey the impression that in every case of switching capacitive circuits or of switching magnetizing currents, special means need be used to prevent serious switching surge overvoltage. Although restriking may occur while opening capacitive circuits, such restriking will not necessarily produce overvoltages serious from the standpoint of circuit and apparatus insulation, particularly where adequate lightning-arrester protection is employed. This also is true of overvoltages produced by off-zero magnetizing-current interruption.


SOURCE
SYSTEM INDUCTANCE
1
2
C1
SYSTEM CAPACITANCE
C2
CAPACITOR REPRESENTING LINE OR CABLE CAPACITANCE OR GROUNDED NEUTRAL CAPACITOR BANK
TIMES NORMAL VOLTAGE
2
1
0
VOLTAGE AT POINT 1 BEFORE CLOSING
VOLTAGE AFTER CLOSING
a
b

A. Equivalent circuit of system, switch, and a capacitance

Figure 1. Illustrating switch-closing transient

B. Voltage transient on closing switch of Figure 1A at normal voltage crest

Switch-Closing Overvoltages

When an unenergized circuit is suddenly connected to an energized one, a voltage oscillation of twice (neglecting damping caused by losses) the voltage difference between them at the instant before closing occurs immediately following the closing operation. The reason for this is the presence of system capacitance, and it may be illustrated by Figure 1A which shows a one-line diagram of a system, a switch, and a capacitor. Assuming that the switch is closed at the peak of the generated voltage wave, *a*, in figure 1B, the voltage at point 1 instantaneously drops to a value given by

$$E_1=\frac{EC_1}{C_1+C_2}$$

and an oscillation of voltage of amplitude $E-E_1$ then takes place about the fundamental voltages as shown, rapidly decaying, in general, because of losses and other system damping such as loads. The oscillation frequency is determined by the system inductance and total capacitance.

In the limit, if C_1 is reduced to zero, E_1 becomes zero, the oscillation amplitude becomes E, and the total voltage on the capacitor reaches $2E$, if the switch is closed at the voltage peak and damping is neglected.

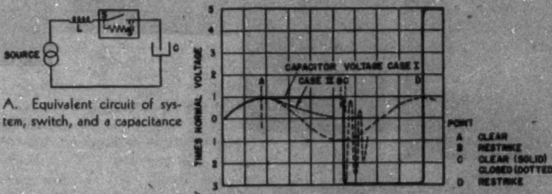
It has been shown[2] that, if the capacitance being switched, C_2, in Figure 1A, is replaced by a line of distributed inductance and capacitance, a similar voltage oscillation will appear all along the line. In the case of the line, however, the oscillation will appear at various points along the line at successive intervals of time and will require a time to reach the far end, depending primarily on the line length and its inductance and capacitance per unit of length. The frequency of the oscillation is also governed by these quantities.

Switch-Opening Overvoltages—Interrupter Restriking

Switch-opening transients are generally negligible if the interrupter opens the circuit at or very near current zero and does not restrike. Restriking takes place in any interrupter if the voltage across its contacts following an interruption recovers more rapidly than the dielectric strength of the insulating medium. When a fault on a solidly grounded system having small capacitance is being cleared by an interrupter, it may restrike several times following an attempted clearing at a current zero, until the contacts have gotten sufficiently far apart that they can withstand the recovery voltage transient of less than twice normal crest line-to-ground voltage, which will occur at each attempted clearing. This restriking is normal and expected and is not harmful either to the breaker or the system. (On a reactance-grounded system, restriking when clearing unbalanced faults can result in severe overvoltages if the ratios of zero-to-positive-sequence inductive reactance are high.[3])

However, under certain conditions, overvoltages of a serious nature may occur as a result of delayed restriking when opening the normally low balanced charging current of capacitance circuits, notably transmission lines and large banks of capacitors. The results of recent studies on capacitor[4] and transmission-line[5] switching, in which this phenomenon has been analyzed, have been reported. For the sake of completeness, a brief discussion of the mechanism by which these overvoltages are obtained follows.

Referring to Figure 2A which represents



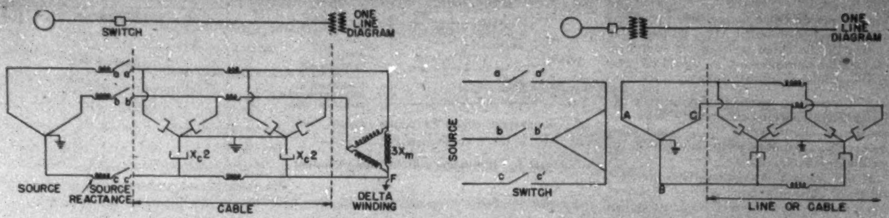
A. Equivalent circuit of system, switch, and a capacitance

Figure 2. Illustrating transient voltage resulting from restriking when opening a capacitive circuit

B. Voltage transient resulting from a restrike of switch in Figure 2A 180 electrical degrees after interruption at normal current zero (voltage crest)

Case I—Without resistor across switch

Case II—With resistor across switch, final interruption by opening switch *S'* of Figure 2A



A. Three-phase cable circuit with saturable delta winding switched together

B. Three-phase cable circuit with delta–wye-grounded transformer between switch and cable

Figure 3. Miniature-system (transient-analyzer) connection diagrams

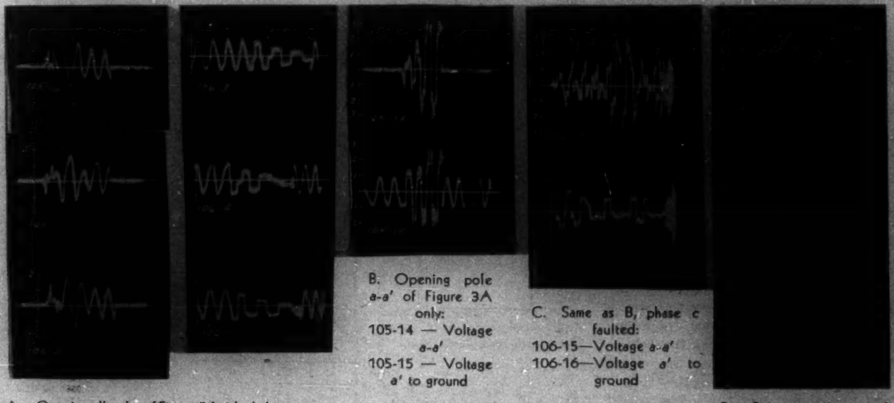
one phase of a system of inductance L supplying a capacitance load, C, let switch S be opened. At a current zero (voltage crest point A of Figure 2B), the current will be interrupted, leaving a full charge on the capacitor. The voltage across the interrupter contacts will grow from zero to twice normal in 180 electrical degrees, as shown by the difference of the stored capacitor charge and the system normal frequency voltage. If restriking occurs during this period, a voltage oscillation will result of magnitude equal to the switch voltage, about the fundamental, resulting in a total voltage of nearly three times normal (neglecting damping) if the restrike occurs when the switch volts are a maximum, as shown at point B. The oscillation will be at natural frequency, and at its first peak (point C), the arc current will be zero. If conditions within the interrupter are conducive to the arc's remaining extinguished at this point, the switch voltage will grow from twice to four times normal in the next 180 degrees, and a restrike at point D would result in a voltage of nearly five times normal. If the arc does not go out at C, the natural frequency voltage will gradually decay as shown by the dashed curve.

It should be remembered that whether the interrupter will restrike under this switching condition depends upon the magnitude of the capacitance current being interrupted and the interrupter characteristics. Tests[4] have established the amount of capacitance, in terms of kilovolt-amperes, that certain breakers in the voltage class 15 kv and below may interrupt with reasonable assurance against delayed restriking. Perhaps future tests will be desirable at higher voltages.

A study of Figure 2B indicates that whatever may be done to reduce the charge stored on the capacitor or line after the first clearing will help very materially, not only to prevent restriking when switching capacitive circuits, but also to reduce greatly the magnitude of the overvoltage obtaining if restriking does occur. The following paragraphs in this section will be devoted to discussing the various means which may be employed to bring about a reduction in the capacitor or line voltage within the half-cycle following the first interruption of the circuit.

1. Resistor Switching

If, when the switch S of Figure 2A is opened, a relatively high resistance is



A. Opening all poles of Figure 3A, ideal clearing:
105-16—Voltage a-a'
106-11—Voltage b-b'
106-2—Voltage c-c'
106-3—Voltage a' to ground
106-4—Voltage b' to ground
106-5—Voltage c' to ground

B. Opening pole a-a' of Figure 3A only:
105-14 — Voltage a-a'
105-15 — Voltage a' to ground

C. Same as B, phase c faulted:
106-15—Voltage a-a'
106-16—Voltage a' to ground

D. Opening pole a-a' of Figure 3B:
100-9 —Volts a-a'
100-10—Volts a' to ground
100-11—Volts C to ground

Figure 4. Transient-analyzer oscillograms

All calibrations in times normal line-to-neutral voltage inserted in the circuit by this action, as would be the case if the interrupter were shunted by a resistor as shown by the dotted lines, the capacitance is afforded a discharge path through sufficient resistance to permit considerable decay of its voltage as shown in Figure 2B, case II (dotted curve). This scheme not only reduces the possible overvoltages but also affords a relatively easy subsequent interruption of the circuit by switch S', because, since the resistor ohms are relatively high, the current will be nearly in phase with the voltage, the current will be small, and at current zero the circuit voltage will be near zero.

Reference 5 describes tests and analyses of an oil circuit breaker provided with such a resistor for reducing the overvoltage hazard when interrupting a long high-voltage line, and reference 2 similarly indicates performance of a breaker provided with resistors to assist in switching a large 13.8-kv capacitor bank. Reference 5 shows that it is possible to obtain a practical value of contact shunting resistance which will provide a very satisfactory means of solving the capacitance switching problem. It should not be inferred, however, that all existing breakers for switching long high-voltage lines need be provided with resistors.

2. Discharge Through Transformer Windings

In the case where, after the switch is opened, the capacitor or line is left with a transformer bank in shunt with it, the windings provide a discharge path for the energy stored in the capacitance. The effectiveness of the windings depends upon their size and whether they are delta—or grounded-wye—connected to the line.

(*a*). *Delta or Ungrounded-Wye Windings.* Refer to Figure 3A. This shows the connection diagram of a miniature-system representation of a cable having a total equivalent shunt capacitive reactance per phase of X_c ohms with the delta winding of an unloaded transformer connected to it. The transformer has a magnetizing reactance per phase of X_m on a line-to-neutral basis. As the first pole of the interrupter a-a' opens, the cable capacitance begins to discharge through the transformer, thus reducing the voltage across the switch initially, over the case of the cable or line alone. If the remaining poles of the interrupter subsequently clear "ideally"—each at its nearest available current zero, the switch voltage (across a-a') will never reach twice normal, and the tendency of the interrupter to restrike should be markedly reduced. Oscillograms 105-16 through 106-5 of Figure 4 show switch and cable voltages for the case of ideal clearing for a ratio of $X_c/X_m = 0.052$. If the interval of time between successive pole clearings is too large, however, the switch and cable voltages may, depending upon the ratio X_c/X_m, become much larger than twice normal, because of series resonance between the capacitance of the unopened conductors and the saturable transformer reactance. These are the overvoltages mentioned previously and reported in reference 1. The time required for the switch voltage to reach twice normal again depends on the ratio X_c/X_m and is summarized for the first pole to clear in curve form, Figure 5, together with the steady-state values that the switch voltage will attain for various values of the ratios. Oscillogram 105-14 shows the voltage across the first pole (a-a') opening with the others remaining closed for a ratio of $X_c/X_m = 0.052$. Oscillogram 105-15 gives the voltage of point a' to ground for this condition of interruption. It will be noted that it required about one cycle for voltage a-a' to reach twice normal line-to-neutral value, and, if reasonably simultaneous clearing of the other poles had occurred, pole a would have had a comparatively low voltage across it while opening. On the basis that similar results were obtained on the other poles, it was considered adequate to use the transient resulting from the first pole to open as a general criterion of the effectiveness of a delta winding to prevent the switch voltage from reaching at least twice normal in a short time. Also, only a general indication of results was necessary, since in general long times between successive pole openings cannot be tolerated.

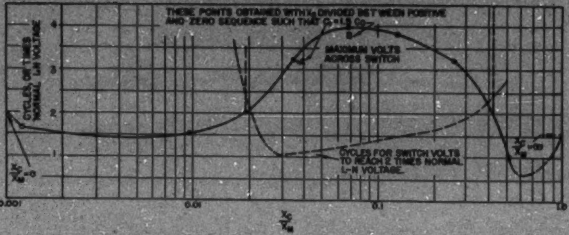


Figure 5. Summary of maximum switch voltages and time for switch voltage to reach twice normal line-to-neutral voltage when opening switch a-a' of Figure 3A for various values of X_c/X_m

It was interesting to note that, if a ground fault on one of the cable or capacitor phases as shown by F in figure 3A was being cleared, the transient voltages resulting from clearing the first of the unfaulted poles are somewhat reduced from the case of no fault. The reason for this is that a discharge path for the capacitance in addition to the delta winding exists by virtue of the fault. Oscillogram 106-15 shows the switch voltage (a-a') for $X_c/X_m = 0.052$ for the condition of phase a being cleared, with a fault on phase c, and 106-16 shows the voltage of a' to ground for this condition.

The data in Figure 5 is for the condition of equal positive- and zero-sequence capacitances. To show the effect of a ratio of positive-to-zero-sequence capacitance of 3/2 (approximately the value for an overhead line), tests were made on the miniature system which indicated the effect to be one of moving the curves of Figure 5 slightly to the right.

(*b*). *Grounded-Wye Windings.* If the line or grounded-neutral capacitor is left with a grounded winding instead of an ungrounded one, it will be somewhat more effective than an ungrounded one, because it is capable of discharging both zero- and positive-sequence capacitance. A special case where a delta–wye-grounded transformer is interposed between the interrupter and line with the line or capacitor connected to the wye-grounded winding was investigated. Figure 3B shows the miniature-system connections. The case of a transformer large enough to carry the line-charging or capacitor kilovolt-amperes being interrupted alone resulted in voltages of less than twice normal from line to ground even with a restrike applied at a time when the switch voltage following first clearing was a maximum. Oscillograms 100-9 to -11 show miniature-system voltages for this case. This indicates the benefits to be gained by "low-side switching" of lines, cables, or capacitors.

Switch-Opening Overvoltages—Interruption Off Current Zero

Another means by which an overvoltage may be obtained on opening a circuit is the studden stoppage of current flow before a normal-frequency current zero has arrived. Under this condition, the voltage obtainable is controlled by the circuit inductance and the rate of change of current caused by the interrupter thus forcing current zero. Overvoltages of this nature have been encountered in the process of interrupting transformer magnetizing currents which are small in magnitude, compared to the normal load current of both the transformer and the breaker. In particular, this problem has been encountered in connection with arc-furnace transformers which, because they are switched at no load frequently, are more apt to be subjected to this type of switching surge than other transformers, other things being equal.

Here again the tendency for the overvoltage to occur is dependent upon the characteristics of the interrupter at the magnitude of current to be switched. For a particular value of current a given interrupter might break the circuit at current peak (worst case), at or near zero, or anywhere in between.

Lightning arresters can be and have been used to protect transformers from this type of overvoltage, but it should be recognized that, if the interrupter cuts off at too high a current, the energy (LI^2) stored in the transformer may be greater than the arrester can dissipate without damage to itself.

An obvious means of preventing the possibility of this type of switching-surge overvoltage is to supply the magnetizing kilovolt-amperes of the transformer by means of capacitors, and switching the two together. The only no-load current to be switched if complete compensation is provided is loss current, which will be very small. It is not particularly important in this method whether either the transformer winding or the capacitor neutral is grounded.

Analysis (see appendix) and miniature-system tests indicate that it is not necessary to supply entirely the transformer magnetizing kilovolt-amperes in order to limit the transient voltage to acceptable values. In the appendix it is shown that for the circuit of Figure 6, representing a transformer winding protected by a capacitor, assuming interruption at current crest (worst case) gives a voltage oscillation across the transformer–capacitor combination whose equation is

$$E_p = -E\sqrt{\frac{X_c}{X_m}} \sin \left\{ \sqrt{\frac{X_c}{X_m}}t \right\}$$

where

E = normal line-to-neutral voltage

X_c = capacitor line-to-neutral ohms per phase at fundamental frequency

X_m = transformer magnetizing line-to-neutral ohms per phase at fundamental frequency and normal voltage

The expression $\sqrt{X_c/X_m}$ may be written

$$\sqrt{\frac{\text{transformer magnetizing kilovolt-amperes}}{\text{capacitor kilovolt-amperes}}}$$

and gives the crest value of the voltage oscillation following interruption and the order of its natural frequency. Thus a capacitor kilovolt-ampere of 25 per cent of the transformer magnetizing kilovolt-amperes will limit the overvoltage under the worst condition of interruption, at current crest, to twice normal, and the oscillation will be at second harmonic frequency.

Although this analysis is made on the basis of an equivalent single-phase circuit with saturation of the transformer neglected, miniature-system tests on a three-phase setup including saturation indicates that the results obtained apply practically whether the transformer and/or the protective capacitors are grounded or ungrounded.

Appendix—Magnetizing-Current Interruption Off Zero

In the equivalent circuit of Figure 6, in which a shunt capacitor is connected to the terminals of the transformer, let

Per unit transformer magnetizing kilovolt-amperes $= \text{kva}_m = \dfrac{1}{X_m}$

Per unit capacitor kilovolt-amperes $= \text{kva}_c = \dfrac{1}{X_c}$

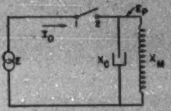


Figure 6. Equivalent circuit of a source, a switch, and an unloaded transformer shunted by a protective capacitor

The current flowing before the switch is opened is

$$I_0 = \frac{E(X_c - X_m)}{X_c X_m}$$

Assuming interruption at current crest (worst case), this may be represented mathematically by inserting the negative of this current through the switch, letting $t=0$ at current crest and the switch voltage will be $I_c \cos \omega t Z(p) = E_{12}$. The voltage across the transformer–capacitor combination is

$$\begin{aligned} E_p &= E + E_{12} \\ &= E \sin \omega t + I_0 \cos \omega t Z(p) \end{aligned}$$

in operational form this is, where

$$\omega_0^2 = \frac{1}{CL}$$

$$E_p = \frac{E\omega p}{p^2+\omega^2}1 + I_0\frac{\omega_0^2 p^2}{(p^2+\omega^2)} \times \frac{pL}{(p^2+\omega_0^2)}1$$

And the solution of this is

$$E_p = E \sin \omega t - \frac{I_0 L \omega_0^2 \omega}{(\omega_0^2 - \omega^2)}\left[\sin \omega t - \frac{\omega_0}{\omega} \sin \omega_0 t\right]$$

which reduces to

$$E_p = -E\sqrt{\frac{X_c}{X_m}} \sin \left(\sqrt{\frac{X_c}{X_m}}t \right)$$

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Automatic Voltage Compensator for Resistance Welding Control

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AMONG the more recent developments in a-c resistance welding control is the voltage compensator. This compensator is an auxiliary control used in conjunction with a standard type of phase-controlled welding panel, the circuit of which is shown in Figure 1. The function of the voltage compensator is to maintain the weld current substantially constant irrespective of line-voltage variations. The inability to obtain new feeders and the burden of added loads in war plants have in many cases made the power systems incapable of supplying several welding machines simultaneously without experiencing an appreciable drop in line voltage. Welding heat, which varies with current squared, will be reduced by 19 per cent for a ten per cent drop in line voltage; a line drop of 20 per cent will reduce the welding heat 36 per cent which is intolerable for consistent welding. These conditions can be remedied by the addition of a small inexpensive auxiliary control, such as the voltage compensator. The regulation of many overworked high reactance welding feeders can then be tolerated because consistent welds will be produced, whereas without proper compensation, poor welds would inevitably result. A compensator unit is shown in Figure 2 installed just in front of the current transformer in the bottom of a portable welding-machine control panel.

Requirements of the Compensator

First, let us consider where the compensator is required. Small voltage variations are permissible, since welding pressure, electrode shape, metallic finishes and other conditions may vary to some degree. Information in previous papers indicates that a five per cent voltage variation will not seriously reduce the weld strength of many types of steel.[1,2] Heavier line drops will probably require the voltage compensator. Its chief utility is on lines having more than one welding machine, since the line drop can be taken into account when adjusting current for one machine. Where line voltage drifts up and down, the compensator becomes advantageous with just one machine.

The next requirement is that the control operate entirely automatically. In operation, any gradual drift of the power-supply voltage will be continuously corrected, so that upon initiation of a spot weld, the initial cycles automatically start at the corrected heat setting. It is also important that the transient rate of response to sudden voltage changes should be as rapid as possible. This rapid response is very necessary in adapting such a control to seam and short-timed spot welding, where a slow response would result in relatively large variations between spots. It is, therefore, equally important that this voltage compensator be capable of nullifying the effects of transient as well as gradual power-supply fluctuations.

Other desirable features include simplicity, ease of connecting and adjustment, and, finally, low cost.

Phase-Controlled Welding Circuits

In order to understand how this voltage compensator works, it will be advantageous to review a standard phase-controlled welding circuit. Common to all electronic a-c welding control circuits are two power tubes, usually of the ignitron type, which are connected in an inverse parallel or back-to-back arrangement as shown in Figure 3. The function of these tubes is to serve as an electronic switch in controlling the power delivered to the welding machine. In this two-tube arrangement a-c power may be passed, since one tube may be rendered conductive in one direction, while the other tube, connected in reverse manner, permits conduction in the other direction. To render an ignitron conductive, a current must be passed through its ignitor or control element. This firing current is furnished by a thyratron-type tube which is capable of delivering high currents for short intervals. The welding control is designed to control these firing tubes, which in turn render the power tubes conductive.

When phase control is added to this electronic switch, the point at which this conduction is initiated is varied so that the power tubes carry current for a given preset portion of each half cycle. The particular phase-control circuit to which the line-voltage compensator was applied is shown in an elementary manner in Figure 1. For simplicity, all grid to cathode capacitors and transient arresting resistors have been left out. The net grid bias voltage on the firing tubes (see Figures 3 and 4) is the sum of four separate voltages:

1. An a-c "hold-off" bias of 300 volts supplied by transformer *T*1.
2. An a-c "turn-on" bias of 220 volts supplied by grid transformer *T*3.
3. A peaked triggering voltage of approximately 200 volts peak from transformer *T*2.
4. A self-rectified d-c voltage across capacitors *C*3 and *C*4 which appears there as a result of grid current flowing during the time the grid was positive.

Normally transformer *T*3 is de-energized, rendering the firing tubes nonconducting by virtue of the negative grid bias throughout the period of positive plate voltage as shown in Figure 4*A*. Upon energizing grid transformer *T*3 (Figure 4*B*) the net a-c bias is reduced such that the peak of firing voltage from transformer *T*2 exceeds the critical grid voltage of the firing tubes, permitting them to be conductive and thereby initiating the power tubes. For accurate and synchronous timing, transformer *T*3 is energized from a thyratron timing circuit, but the time could be controlled by a simple pushbutton hand switch. The phase setting of the *T*2 firing peak is determined by a resistance-capacitor bridge in which the resistance element *R*7 is adjustable for changing the phase of the peaked firing voltage. The components in series with the primary winding of the peaking transformer *T*2 form a filter to insure only one positive firing peak each half cycle. This circuit is resonant so that voltage and current are approximately in phase. However, the peaked voltage of *T*2*S* which is about 10 degrees wide occurs at current zero and is therefore 90 degrees out of phase with the phase-shifting voltage vector.

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The compensator may be adapted to any standard panel by three connections, 6, 7*B* (reconnection from 7), and 8 as shown in Figure 1.

Operation of the Compensator

The fundamental purpose of this compensator is to advance automatically the phase of the "peaker" firing voltages when the line voltage drops, and vice versa, to retard the phase should the line voltage increase. This is done by replacing the fixed transformer side of the resistance-capacitor phase-shift bridge with a voltage-dividing network which will vary in accordance with changes in line voltage. Referring to the elementary diagram of the compensator, Figure 5, the voltage divider consists of the primary winding of transformer *T*101 and resistor *R*101. Transformer *T*101 is made to act as a variable resistance by the action of the shorting tubes 102 and 103. The grid voltage of tubes 102 and 103 when varied causes the tubes to act as a variable resistance load in the secondary winding of transformer *T*101 and makes *T*101 appear substantially as a resistance in the voltage divider. The center point, 7*B*, of the voltage divider may be shifted back and forth by some magnitude e_e as shown in Figure 6. This causes a shift in the vector relation between vector 6–8 which corresponds to the phase of the line voltage, and vector 7*B*–9 which in turn determines the phase of the peaked firing voltage *T2S* of transformer *T*2 with respect to the line voltage. When the grids of tubes 102 and 103 are made less negative, tubes 102 and 103 appear as a lower resistance load and hence cause point 7*B* to move toward point 8, advancing the phase of the firing voltage vector *T2S* with relation to vector 6–8. In a reverse manner the phase of the firing peak may be retarded by making the grids of tubes 102 and 103 more negative.

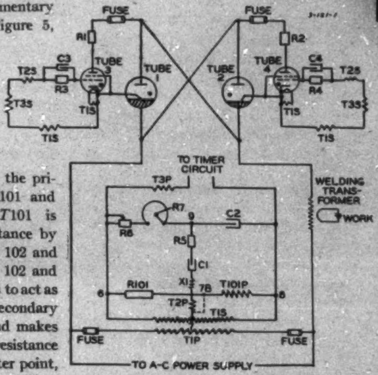


Figure 1. Simplified diagram of phase-controlled welding circuit

To obtain the operating grid voltage, transformer *T*102 is connected directly across the line supply and its output voltage rectified by tube 101. By making time constants of the filter on the output of the rectifier voltage relatively low, this d-c voltage can be made to follow closely all fluctuations in the line supply. D-c output of the rectifier provides a reference voltage across the voltage regulating bridge (tubes 104 and 105), which at normal line condition give zero voltage across points 20 and 21. A d-c signal voltage directly proportional to the line-voltage change appears across these bridge output terminals.

It is necessary next to investigate the amount of compensation or phase shift required to maintain the welding current constant for a given line-voltage drop at various power factors of the welding circuit and per cent current settings. The higher the power factor, the greater will be the phase shift required; and likewise, the larger the percentage of full-phase current flowing, the greater will be the shift required to maintain the current. Figure 7 illustrates the approximate phase shift requirements to correct for a 10 per cent line-voltage drop for various power factors and per cent current settings. It should be apparent that once full-phase firing has been reached, no additional compensation can be obtained. Hence, if the line voltage falls to 90 per cent of its normal value, a maximum (depending on power factor) of approximately 95 per cent current can normally be delivered to the welding machine. The compensated timer requires a five per cent, and in some cases ten per cent, zero current gap to allow for high heat compensation by phase advance. It is, however, usual practice to include this slight zero current gap at full heat in most welding machines so that any changes in secondary power factor will not permit heat-control settings to range above 100 per cent sine wave current. Unstable operation might result in operating practice unless this slight open current gap was adjusted for the ordinary welder control panel.

Figure 2. Panel with PT recorder and compensator installed

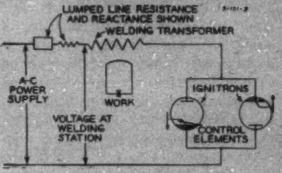

LUMPED LINE RESISTANCE AND REACTANCE SHOWN
WELDING TRANSFORMER
A-C POWER SUPPLY
WORK
VOLTAGE AT WELDING STATION
IGNITRONS
CONTROL ELEMENTS

Figure 3. Typical welding power circuit

Figure 8 shows how the setting of *P*101 is used to obtain a greater rate of compensation at higher than normal power factors. *P*101 is adjusted so that there is an unbalance as much as 20 per cent between the voltages 6–7*B* and 7*B*–8. When a voltage drop occurs, point 7*B* is moved toward 8 by the amount e_e. This produces an angular advance in the trigger firing voltage of a_1, a_2, and a_3 for high, intermediate, and low heat settings. For the range of 20 per cent to 60 per cent power factor, which covers most resistance welding setups, a_1 is substantially greater than a_2 and a_3 without too much unbalance between voltages 6–7*B* and 7*B*–8. Thus *P*101 is adjusted to obtain a curving characteristic of phase shift versus heat setting. *P*101 is used as a bridge sensitivity control and therefore acts to determine the slope of the characteristic curve. By correlating the two adjustments it is possible to obtain a compensator characteristic which closely approaches the requirements shown by Figure 7. That this has been accomplished is borne out by experimental results which follow.

Data on Operating Performance

Referring to Figure 7, it is seen that the compensator must respond over a

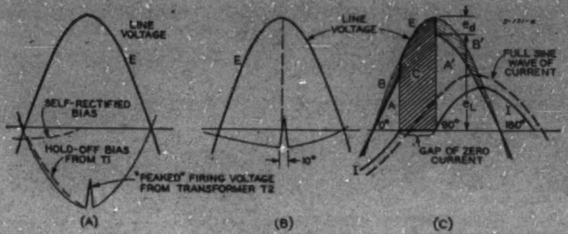


Figure 4A. One firing tube control voltage—standby

Figure 4B. Same firing tube voltage—for welding. Resultant grid firing voltage shown

Figure 4C. One half-cycle welding voltage and current showing instantaneous line drop

wide range of phase-angle advance for a variety of conditions that depend primarily on heat current setting and power factor. It must also be taken into account that the regulation may vary considerably, depending on the number of other machines that "hit" simultaneously. Differing portions may also be "chopped" off the useful area of the line-voltage wave as governed by the fractional heat dial settings of these machines. Then, too, interfering welders may also "come and go" early or late during a weld period. Fortunately, the control bridge circuit is very sensitive and responds correspondingly to all degrees of regulation, handling instantaneous line drops readily.

It would be well to consider briefly the instantaneous voltage drop imposed on a welding station by power transformer and feeder reactance. Standardization has not yet fully clarified the meaning of percentage voltage regulation for phase-controlled welding machines caused by this effect. From Figure 4C it should become apparent that with the form of voltage wave shape shown, the use of a voltmeter to register welding load voltage is inaccurate. The cathode-ray oscilloscope offers a better means of obtaining the voltage drop. The question then arises as to where to measure the voltage drop when speaking in terms of percentages. One criterion might be to consider the regulation as:

$$\%\ \text{instantaneous regulation} = \frac{e_d}{e_L}\ \text{at line-voltage peak (90 degrees)} \qquad (1)$$

Here e_d and e_L are initial line drop and load voltage respectively. Also:

$$e_d = L\,di/dt\ \text{for}\ i_0 \qquad (2)$$

where L = lumped power source inductance

In considering over-all welding regulation, it is necessary to discard the no-load voltage area *C* shown in Figure 4*C*. Regulation for the half cycle shown then becomes:

$$\%\ \text{total regulation} = \frac{\text{area } B+B'}{A+A'+B+B'} \qquad (3)$$

The voltmeter could measure this accurately for one condition, namely, full sine wave welding current, where an instantaneous drop does not normally occur. Strictly speaking, equations 1 and 3 must be reconsidered each time another fractional heat current is used. The reason for this is that partial heat currents are nonsinusoidal and have initial rates of rise that depend on the ignition point, that is, the controlled firing angle. The welding power factor also contributes to the degree of the instantaneous voltage drop. Thus the importance of instantaneous voltage drop cannot be overemphasized.

The detection circuit of the voltage compensator was designed to take into account this complication of measuring just such voltage regulation. The rectifier supply, which is in effect the detection circuit, has an inductive input filter which integrates the voltage wave, so that instantaneous drops that occur after 90 degrees will readily be averaged into the resultant output voltage.

A test was conducted using actual weld pull specimens and dropping the voltage by means of another timer operated simultaneously. The results with and without compensation are shown in Table I. Note that the average for 0.016-inch stainless steel with compensation is 399 pounds pull strength, whereas when uncompensated the average fell to 333 pounds strength because of external line drop. An ampere-squared second recorder indicated that approximately only 70 per cent heat was getting to the weld when uncompensated.[8] The difference is even more marked for 0.050-inch stainless steel, shown in Table II. Here the average fell from 2,634 pounds pull when compensated down to 2,063 pounds pull when not compensated for the particular line drop. The recorder indicated approximately 76 per cent heat into weld for the uncompensated case. A definite advantage in favor of the compensator was found as a result of these tests.

The compensator was next tested under severe conditions and operated with an instantaneous voltage drop as high as 27 per cent. Without compensation the recorder indicated that the heat into weld was about 50 per cent of normal. The compensator was therefore able to bring the heat up to 100 per cent (entirely back to normal). This is strikingly illustrated by shear strength pull tests which showed perfect weld strength of 2,300 pounds when compensated but fell off to 1,350 pounds when uncompensated for the drop referred to. Such a test points to suitable compensation for several heavy welders operating simultaneously, provided the power source is not being too severely overloaded. The main purpose of this test was to determine evenness of compensation under the wide variety of conditions mentioned earlier. With

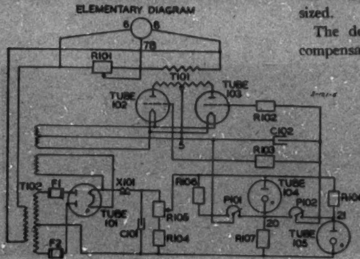
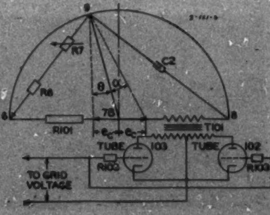


Figure 5 (left). Elementary diagram of compensator panel

Figure 6 (right). Vector sketch showing how phase of peaked firing voltage is shifted by compensation

Heat control circuit is included. e_c is compensation voltage



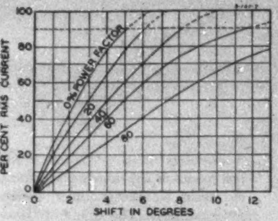


Figure 7. Phase shift required to maintain constant current with ten per cent line-voltage drop

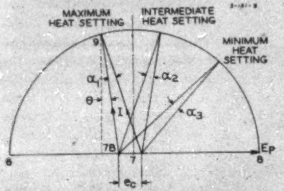


Figure 8. Vector sketch showing heat control range and point 7B shifted as required

various combinations of heavy and light loading for operation of both timers, deviations of the recorder for the most part hovered well within five per cent of normal. Adjustment of the compensator was not changed during this test. The greatest deviation experienced was over compensation of 15 per cent with the extremely heavy external drop referred to and then only for very low heat settings of the compensated timer. Even this is within tolerance limits and is remedied by reducing the power-factor-gain control slightly, if machine is operated continuously on this range. The power factor used was 0.6, which is fairly high, and the time was eight cycles. Consistent results were also obtained on lower power factors.

Oscillograms were taken to illustrate compensator action. In order to provide the line drop, a second timer known as a line dropping timer was operated on the same line. Referring to Figure 9, the two timers came on the line together, and the compensator-test timer had the compensator-gain-control knob *P*102 turned off. Thus, the test timer operated without compensation and when the dropping timer *C* stopped, the difference in the magnitude of weld current *A* is apparent.

Figure 10 illustrates normal compensation. The compensator acted fully on the weld current *A* within two cycles from the initial cycle dropping the line. After the external drop went off, the compensator required two cycles to phase back to normal, as seen by the amplitude of cycles 5 and 6 which are above normal. Since these oscillograms were taken, the speed of operation has been reduced to one cycle to yield full compensation. Notice the instantaneous drop expressed by the line voltage *B*. This same voltage drop (Ldi/dt) is experienced by every control transformer and circuit in the control panel and is seen by the dip of *T*101 (primary) voltage *D* of compensator and its related branch *R*101 shown as voltage *E* (reduced for clarity).

A better comparison is obtained from Figures 11 and 12. Figure 11 is without compensation and illustrates the current decrease from a normal rms value of 750 amperes, as read on the primary side of welding transformer. Figure 12 shows how this decrease was made up by compensation. Primary weld current *A* at cycle 2 became lower from an external load, but cycle 3 is almost up to normal compensation. Cycles 4 and 5 (and all further succeeding compensated cycles) appear normal. However, after release from the external load, cycles 6 and 7 continued to respond to phase advance. This is not particularly a disadvantage, as it tends to make up for the early partially compensated cycles (2 and 3). Cycles 8, 9, and 10 duplicate the initial cycle. It can be noticed that the shoulders, or phase concurrence for cycles 8, 9, and 10 are later than for compensated waves 3 to 7 inclusive. This accords with the principle of compensation employed. Were the oscillograph element *A* as responsive as element *C*, these shoulders would have the same flatness. Of importance, the peak amplitude of compensated current loops 4 and 5 need not equal normal loops 8, 9, and 10, since the criterion for equal compensation is that the rms value of the loop areas be equal in both cases. The instantaneous values of *D* and

Table I. Sample Shear Pull Strength Tests for Type 304 Stainless Steel 0.016 Inch Thick

Line Condition During Weld	I^2T Recorder in Per Cent of Normal Heat		Corresponding Weld Pull (Pounds)	
	With Compensator	Without	With Compensator	Without
Compensated weld machine. No line drop (start of test)	100 (Reference)	100 (Reference)	380	397
Compensated weld machine. No line drop (finish of test)	100 (Reference)	100 (Reference)	390	400
External line drop	95	71	418	332
External line drop	100	70	380	342
External line drop	100	70	400	328
External line drop	102	71	412	325
External line drop	100	72	385	340
External line drop	97	73	370	350
External line drop	100	71	375	322
External line drop	101	72	448	320
Average	99.3	71.3	398	332

Type 304 stainless steel 0.016 inch thick—1/4 hard

Weld time: four cycles. Weld current: 70 per cent (fractional). Electrode size: 1/4 inch. Electrode force: 340 pounds total. Line: 240 volts a-c. Regulation: six per cent. (Read with voltmeter during welding current, but using longer time.)

Regulation for instantaneous external line drop: 10 per cent. (Read with oscilloscope for partial current firing at approximately 100 degrees on voltage wave.)

Figure 9. Oscillogram of timer performance without compensation

A. Primary current of welding machine with installed compensator

B. Line voltage (240 volts a-c) common to both welding machines

C. Primary current of line-voltage dropping time

D. Voltage across *T*101 transformer primary winding in compensator. Adjusted value 260-volts a-c rms

E. Voltage across *R*101 bridge resistor in compensator. (Reduced in size for clarity.) Adjusted value 220 volts a-c rms

Figure 10. Oscillogram of welding machine performance with compensation. Early line drop

See subcaption to Figure 9

Figure 11. Oscillogram of welding machine performance without compensation. Late line drop

See subcaption to Figure 9

E found in the oscillogram determine the shift of midpoint 7 of Figure 7. Voltage *D* corresponds to *T*101 voltage and *E* to *R*101, and their shift toward the right for compensation upward agrees with previous discussion. The I^2T recorder read 100 per cent normal heat for the oscillograms being compensated and dropped more than 15 per cent for those which were uncompensated, thereby causing lockout of the welding machine.

Although data were not taken for slow drifts in the sine wave value of line voltage, the response on seam welders using thermocouple ammeter indication has shown that the compensator handles such changes readily.

Investigating Compensator Performance

Several methods of investigating performance are available.

1. The cathode-ray oscilloscope, when connected to read one full cycle of weld current, will indicate the phase-angle advance during compensation. The oscilloscope is also helpful for making electronic control-panel power-factor heat adjustment after installing a compensator.

2. The I^2t recorder, illustrated in Figure 4, is well adapted to record changes in welding current. Where this recorder is available, a quick test is performed by operating two welding machines on the same line individually and together. Uniform recorder response, when the control panel is set to various heat positions, does indicate correct compensation where the regulation would otherwise drop the recorder below the 15 per cent heat limit and cause bad welding.

3. Spot-welding machines may be tested with either clamp-on pointer stop ammeter or pointer stop ammeters in conjunction with current transformers. The previous method for operating two welding machines together is recommended.

4. Seam welding machines may take advantage of a suitable ammeter such as a thermocouple type with an adjustable primary current transformer for indication of operation. Adjustment of the compensator is readily accomplished here, provided a definite line disturbance can be initiated during the test.

5. Where none of the aforementioned methods is available, the compensator can be adjusted and its operation checked by using merely a voltmeter across the control signal bridge and also across the primary winding of *T*101 for visually checking voltage swings. Normal adjustments once made need not be disturbed following one of these performance checks. The only special adjustments are

(*a*). The degree of compensation desired (same for all normal operation).

Figure 12. Oscillogram of welding machine performance with compensation. Late line drop

See subcaption to Figure 9

(*b*). A slight unbalance of voltages across bridge arms *R*101 and *T*101 to allow uniform response in installations where the welder power factor is much higher than normally experienced. All adjustments are quickly performed, using a combination voltage test set.

Table II. Sample Shear Pull Strength Tests for Type 304 Stainless Steel 0.050 Inch Thick

Line Condition During Weld	I^2T Recorder in Per Cent of Normal Heat		Corresponding Weld Pull (Pounds)	
	With Compensator	Without	With Compensator	Without
Compensated weld machine. No line drop (start of test)	100 (Reference)	100 (Reference)	2,700	2,800
Compensated weld machine. No line drop (finish of test)	100 (Reference)	100 (Reference)	2,600	3,100
External line drop	96.5	82	2,725	2,200
External line drop	97	83	2,725	2,400
External line drop	105	70	2,600	1,900
External line drop	100	75	2,625	2,000
External line drop	100	73	2,500	1,800
External line drop	95	73	2,600	1,800
External line drop	95	72	2,700	1,850
External line drop	100	76	2,750	2,100
Average	98.6	75.5	2,634	2,063

Type 304 stainless steel 0.050 inch thick—$^1/_4$ hard.

Weld time: nine cycles. Weld current: 75 per cent (fractional). Electrode size: $^3/_8$ inch. Electrode force: 1,200 pounds total. Line: 240 volts a-c. Regulation: 10 per cent. (Read with voltmeter during welding current, but using longer time.)

Regulation for instantaneous external line drop: 15 per cent. (Read with oscilloscope for partial current firing at approximately 90 degrees on voltage wave.)

Conclusions

1. This voltage compensator helps to maintain uniform current for resistance welding despite the disturbing influence of other machines on the line feeder. With wartime urgency of using existing feeders and with limited use of copper, the problem of poor voltage regulation is eased considerably.

2. Instantaneous reactance line drops (chopped voltage waves) in the order of 30 per cent have been compensated for 100 per cent. Without compensation the heat output was otherwise 50 per cent of normal value.

3. Another feature is compensation for upward or downward line-voltage shifts during the working day.

4. The ability to make consistent welds on poorly regulated lines, is, of course, the chief advantage of the compensator. On installations where the weld recorder locks out the equipment when the value of I^2T swings out of the allowable range, the operator is forced to reset the welding machine and usually to make new weld tests and rewelds. The voltage compensator eliminates to a large degree the number of such lockouts caused by variations in current with a resulting saving in lost time.

5. The unit is inexpensive, simple, compact, easy to install and adjust. It is entirely automatic, since the response was designed for uniform compensation for any fractional heat current settings.

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Thyratron Motor Control

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Synopsis: The control of d-c motors was one of the first electronic-control projects. Recent developments, particularly the fully automatic and co-ordinated control of the armature power circuit and the field power circuit, have greatly increased the acceptability of these controls for industrial use.

This paper describes the control functions associated with an all-electronic d-c motor drive, in which grid-controlled thyratron-tube rectifiers automatically perform the variable voltage functions associated with the d-c generator, exciter, and field rheostats of a conventional motor-generator drive.

In addition, the grid-control action of the thyratron-tube rectifier dispenses with the usual magnetic starting and accelerating control devices and gives superior motor performance in the matter of almost idealized constant-current acceleration and flat preset speed-control characteristics resulting from *IR*-drop compensation and regulated field excitation.

Reversing is accomplished by magnetic reversing switches which reverse the armature terminals relative to the rectifier. Contact duty during switching is minimized by the supplementary action of the thyratron tubes which themselves serve to interrupt and initiate the armature current. Proper grid-control sequence in these same tubes causes them to act as inverters during the decelerating portion of the reversing cycle so that the rotational energy of the armature is actually pumped back into the a-c system.

Stopping is normally by means of a dynamic braking resistor, although the motor may be decelerated to a stop by utilizing the inverter action associated with reversing to regenerate power back into the a-c system.

Similar control schemes are applicable to motor-generator-set drives to control the generator and the motor fields in response to co-ordinated signals of motor speed and loop currents.

THE basic problem of realizing the flexibility of a d-c motor where only a-c power is available can be readily solved by the use of thyratron-tube rectifiers. The source of direct current is obtained from the rectifier, and the grid-control feature offers a means of controlling the motor by low-energy grid circuits.

The control can make the motor perform in practically any way desired. The speed can be controlled in accordance with requirements of the machine it drives, such as holding tension in a wire-reeling machine or holding loops between the stands of a rubber, cloth, paper, or steel mill. The speed can be held constant over a wide range of load or line-voltage conditions. The torque output can be held substantially constant from zero to base speed. Protection during acceleration and on overload is possible by means of current limit built into the control.

The rating of the rectifier, the type of circuit used, and the rating of the individual power tubes determine the size of motor which can be so controlled. In the case of larger size motors, the output wave shape is more of a determining factor on the circuit used than the size of the motor. The rectifier circuits used conform to standard rectifier practice and will not be covered in detail in this paper. The conventional biphase half-wave circuit controlling a one-horsepower 230-volt d-c motor will be used as the example in describing the theory of operation. It will be noted that two rectifiers are required, one for the armature power and one for the field. When the motor is to be operated from base speed down, by varying the armature voltage, the field rectifier is uncontrolled. A variable-speed motor can be made to cover a wide speed range by using a controlled rectifier on the field as well as on the armature.

The armature rectifier consists of grid-controlled thyratrons, and the output voltage is varied by shifting the phase of the grid voltage of the thyratrons with respect to their anode voltage. There are several ways of shifting the grid voltage, but the one used in these controls is the conventional inductance-resistance bridge. The inductance will be the variable element by the use of a saturable reactor. In this way the output of the rectifier can be controlled by the small d-c current in the saturating winding of the saturable reactor. Such a circuit is shown in Figure 1.

Auxiliary D-C Control Power and Voltage Standards

Since direct current is required for these saturating windings, a source is provided by a small thermionic rectifier and anode supply as shown in Figure 2. The d-c voltage is filtered by means of a reactor $X1$ and capacitor $C1$ and fed through a current-limiting resistor $R1$ and the voltage-regulating tubes A and B.

Tube B is primarily used as a constant voltage reference against which various signal voltages are compared. It is an inherent characteristic of a glow tube that its terminal voltage will remain essentially constant at rated value even though the current through the tube changes widely. The tubes used in this circuit maintain approximately 75 volts, and it is by this means that a reference voltage for regulating purposes is established that will remain relatively constant irrespective of a-c line-voltage changes.

Tube A is connected in series with tube B to give a constant potential three-wire d-c system of 150/75 volts for the plate and grid voltages of the control tubes.

The d-c winding of a saturable reactor is connected in series with a triode vacuum tube C, and this combination is connected across the 75-volt d-c control bus furnished by regulator tube A. (See Figure 3A.) With a negative voltage on the grid of tube C there will not be any plate current, the reactor will be unsaturated, and the thyratrons are phased off. If the grid is made less negative, tube C will pass some current, and the reactor will be partially saturated, which will advance the phase of the grid voltage. As the grid voltage of tube C approaches zero, the reactor will be completely saturated, and the thyratrons will be full on.

Armature-Voltage Control

The control scheme consists of comparing a portion of the armature voltage with a preselected portion of the voltage-regulator tube's voltage. The difference between these two voltages is amplified and applied to the grid of the triode, which saturates the reactor controlling the grid phase shift on the thyratrons supplying the armature of the motor.

To couple the armature voltage to the triode which saturates the reactor, it is necessary to have a common point for the signal voltage from the armature and for the grid voltage applied to the triode. It is also necessary that the relation of these voltages be correct; that is, an increase in armature voltage above a preset voltage level should make the grid of tube C more negative so as to retard the phase of the thyratron grid voltage. By studying Figure 3B, it will be apparent

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that the voltage relations are incorrect and, if directly connected, would give incorrect operation. It is therefore necessary to change this relation.

This can be done as shown in Figure 3C as follows: The grid of tube *C* is connected to a voltage divider across the 150-volt control bus so that the grid is positive when tube *D* is passing very little or no current. Then, as more current is caused to flow through tube *D*, the grid of tube *C* will be pulled negative by the increased current through *R*2. It is now possible to control the phase of the thyratron grid voltage by changing the voltage on the grid of tube *D* with the relation correct for regulation of the armature voltage.

Armature-Speed-Control Potentiometer

With the cathode of tube *D* connected to the mid-point of a voltage divider across glow tube *B*, as shown in Figure 3C, the cathode will be $37^{1}/_{2}$ volts positive with respect to the negative bus. With zero voltage on the armature, the grid of tube *D* will be $37^{1}/_{2}$ volts negative, and the thyratrons will be in a condition to conduct full half cycles. As voltage on the armature builds up to nearly $37^{1}/_{2}$ volts, the thyratrons will shut off just enough to maintain a balance between the armature voltage and the $37^{1}/_{2}$-volt reference.

In order to permit the adjustment of the armature voltage or speed, resistor *R*5 is replaced with a potentiometer so that the cathode potential of tube *D* can be changed. In order to permit the use of 230 volts on the armature of the motor, a voltage divider is connected across the armature and a portion of the armature voltage compared to the reference voltage. This is shown in Figure 4. The potentiometer shown in the armature voltage divider gives a means of setting the base speed with the speed control set for maximum speed. This adjustment gives a means of compensating for commercial tolerances in resistors and tubes.

IR-Drop Compensation

Armature speed is proportional to armature terminal voltage only when the armature is drawing no current and is spinning freely with the field excited. Under these conditions, the armature voltage is the counter electromotive force or generated voltage of the armature conductors. When current flows in the armature, as when the motor shaft is loaded, the counter electromotive force and hence the true speed is less than the terminal voltage by an amount equal to the voltage drop in the resistance of the armature conductors themselves. If a constant shaft speed is to be maintained irrespective of load, it is necessary to raise the terminal voltage progressively as the load current increases, by an amount equal to the armature-resistance voltage drop. To do this it is only necessary to introduce in the armature-control circuit a voltage proportional to the armature current in such a direction as to hold a higher armature voltage as the load on the motor increases.

Although it would be possible to build an amplifier system that would amplify the millivolt drop of a conventional shunt to such a point that it could be used, it offers serious difficulties. In order to get a more suitable voltage, a special current transformer is designed to operate in the anode leads of the rectifier tubes. The pulses of current through these anode leads are fed into the two primary windings so as to act as an alternating current that bears a direct relation to the direct current. The secondary winding of this current transformer is loaded by a resistor and a full-wave rectifier, and the resultant d-c voltage is directly proportional to the armature current. The value of this voltage for a given current can be adjusted with a potentiometer.

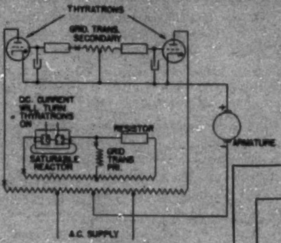


Figure 1. Grid-controlled rectifier with phase-shift bridge

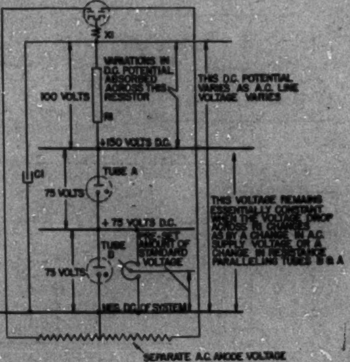

X1
VARIATIONS IN D.C. POTENTIAL ABSORBED ACROSS THIS RESISTOR
THIS D.C. POTENTIAL VARIES AS A.C. LINE VOLTAGE VARIES
100 VOLTS
R1
+150 VOLTS D.C.
C1
TUBE A
75 VOLTS
THIS VOLTAGE REMAINS ESSENTIALLY CONSTANT WHEN THE VOLTAGE DROP ACROSS R1 CHANGES AS BY A CHANGE IN A.C. SUPPLY VOLTAGE OR A CHANGE IN RESISTANCE PARALLELING TUBES B & A
+ 75 VOLTS D.C.
TUBE B
PRE-SET AMOUNT OF STANDARD VOLTAGE
75 VOLTS
NEG. D.C. OF SYSTEM
SEPARATE A.C. ANODE VOLTAGE

Figure 2. Regulated rectifier for control amplifiers

This d-c voltage that is proportional to armature current is connected in the lower end of the armature-voltage-divider circuit as shown in Figure 5. This added voltage in effect subtracts from that part of the armature voltage used as a feedback and therefore causes a higher voltage to be maintained by the regulating action. The adjustment provided permits setting the control for only partial compensation, or it can be overcompounded.

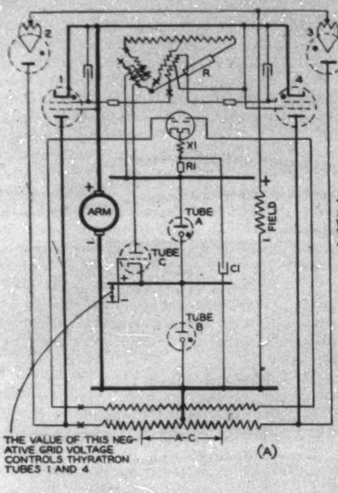
Figure 5A shows the effectiveness of the *IR*-drop compensation. The dotted curves represent the normal uncompensated motor-regulation characteristics. The solid curves were taken with the *IR*-drop compensation set for flat compounding at 100 per cent rated speed. Note the great improvement in the motor characteristics at the lower speeds.

Current Limit

Current limit is necessary to prevent excessive currents being drawn during acceleration so as to protect the tubes and keep within the limits of commutation of the motor; otherwise, overloads could occur by suddenly applied shaft loads or by starting with a preset speed adjustment. A current-limit control should be inoperative from zero current up to the preselected value and then prevent the current rising above this value. If this were not the case, the current would be influencing the speed; instead, it should take over sharply as near the preset current value as possible.

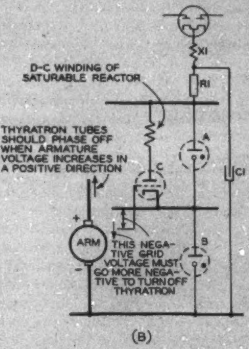
A d-c voltage proportional to armature current has been established for *IR*-drop compensation, and this same voltage can be used for current limit. A current-limit signal is established by comparing a portion of the d-c voltage that is proportional to current with a known voltage standard and amplifying the difference by a triode. The circuit showing the connections for this control added to the armature control and IR-drop compensation is shown in Figure 6.

The value at which the current limit takes over is adjustable by means of the current-limit potentiometer. As long as the voltage on the slider is less than 75 volts, the current-limit tube E will be biased to cutoff, or practically so, and the armature-control tube D will have complete control. If high armature current causes the voltage on the current-limit-potentiometer slider to reach 75 volts, tube E will pass full anode current which will retard the grid voltage phase on the thyratrons. This reduces the current, and a balance will be reached. When the load on the motor is reduced and the armature current goes below the preset value, the armature-control tube D will take over and hold armature voltage as long as the current stays below the current-limit point.



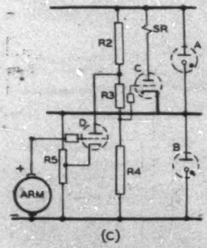
2
3
1
4
R
XI
RI
ARM
+
-
TUBE A
TUBE C
TUBE B
CI
FIELD
+
THE VALUE OF THIS NEGATIVE GRID VOLTAGE CONTROLS THYRATRON TUBES 1 AND 4
A-C
(A)

A (left). Variable d-c source for saturable reactor



XI
RI
D-C WINDING OF SATURABLE REACTOR
THYRATRON TUBES SHOULD PHASE OFF WHEN ARMATURE VOLTAGE INCREASES IN A POSITIVE DIRECTION
A
C
CI
ARM
+
-
THIS NEGATIVE GRID VOLTAGE MUST GO MORE NEGATIVE TO TURN OFF THYRATRON
B
(B)

B (above). Showing that armature voltage has incorrect sense to be coupled directly to grid of tube C

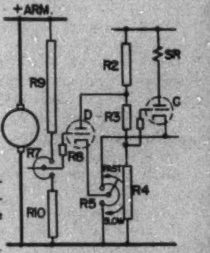


SR
R2
A
C
R3
D
R5
R4
B
+
ARM
(C)

C (left). Showing that armature voltage now has correct sense to be coupled directly to tube D

If armature voltage tends to increase (becomes more positive), the grid of tube D becomes less negative and tube D turns on which turns tube C off which reduces saturation of SR which retards grid phase and reduces thyratron output

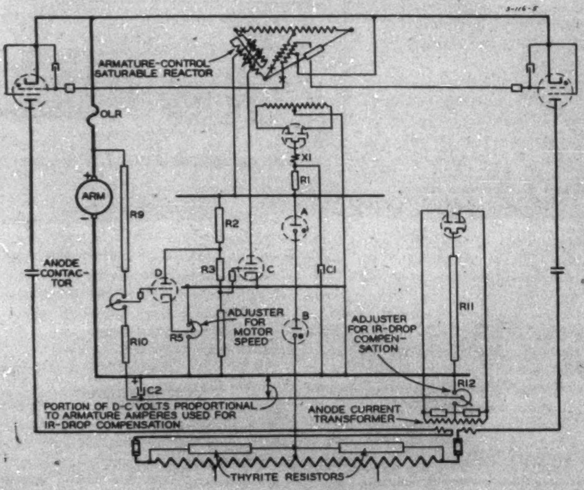
Figure 3. Armature-voltage-regulation circuit



+ARM.
R2
SR
R9
D
R3
C
R7
R8
RST
R5
R4
R10
3.0W

Figure 4. Armature-voltage-regulating circuit complete with armature-voltage divider

Figure 5 (below). Armature-voltage-regulation circuit with *IR*-drop compensation added



3-116-5
ARMATURE-CONTROL SATURABLE REACTOR
OLR
ARM
+
-
R9
XI
RI
R2
A
ANODE CONTACTOR
D
R3
C
CI
R10
R5
ADJUSTER FOR MOTOR SPEED
B
ADJUSTER FOR IR-DROP COMPENSATION
RII
RI2
C2
PORTION OF D-C VOLTS PROPORTIONAL TO ARMATURE AMPERES USED FOR IR-DROP COMPENSATION
ANODE CURRENT TRANSFORMER
THYRITE RESISTORS

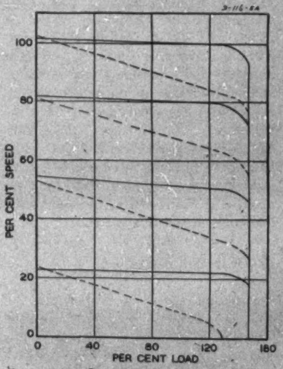
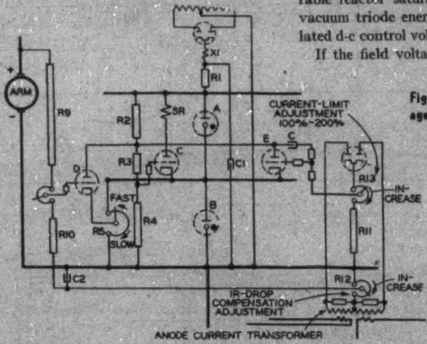


Figure 5A. Speed-regulation curves showing effect of *IR*-drop compensation

Dotted regulation curves are without *IR*-drop compensation. Solid regulation curves are with *IR*-drop compensation

With this type of control it is possible to accelerate from zero to full speed without drawing more than the preset value of current through the armature. During the operation of the motor, if excess load is applied, the speed will be maintained up to the point where the current reaches the current-limit setting. Beyond this point the preset value of current will be maintained in the armature even down to the stalled armature condition. The current will be maintained until the thermal overload relay functions.

When starting from rest with zero current or when reversing, there is no current to generate a current-limit voltage, and the armature voltage is zero; therefore, the first few half cycles of current might be of destructive magnitude. To overcome this objection, a normally closed interlock on the initiating contactor is used to give a false signal in the current-limit circuit. By means of a capacitor this false signal is "remembered" for a time after the circuit is closed and makes the current build up to the current-limit value rather than have one large surge of current and come down to the correct value.



ARM
+
-
R9
R10
D
R5
FAST
SLOW
R2
R3
R4
5R
C
XI
R1
A
B
C1
C2
E
C
CURRENT-LIMIT ADJUSTMENT 100%-200%
R13
INCREASE
R11
R12
INCREASE
IR-DROP COMPENSATION ADJUSTMENT
ANODE CURRENT TRANSFORMER

Figure 6. Armature-voltage-regulation circuit with current limit added

Stopping

The rectifier characteristics of the thyratrons supplying direct current to the motor prevent the reversal of power from the motor to the a-c system when the motor is running faster than the speed called for by the control. This condition exists, for example, when the speed-control potentiometer is suddenly turned to a lower speed. In the case of high inertia loads this "electrical free-wheeling" characteristic is objectionable, but on friction loads it is of no consequence.

When power is removed for stopping, the same system of dynamic braking can be used as is found on magnetic control; that is, a resistor may be connected across the armature to absorb the stored energy. If quick slowdown is required, it is possible to add a control tube and a relay that will actuate the dynamic braking contactor to connect a resistor across the armature when the motor speed is above that called for by the control. When the speed is reduced to the called-for value, the tube initiates the function of disconnecting the resistor.

Field-Weakening Control

In order to expand the range of speed control, the shunt field rectifier tubes can be replaced with thyratrons and control added to the field. The control of the field rectifier is accomplished by using another saturable reactor and resistor bridge with the d-c winding of the saturable reactor saturated through a high vacuum triode energized from the regulated d-c control voltage.

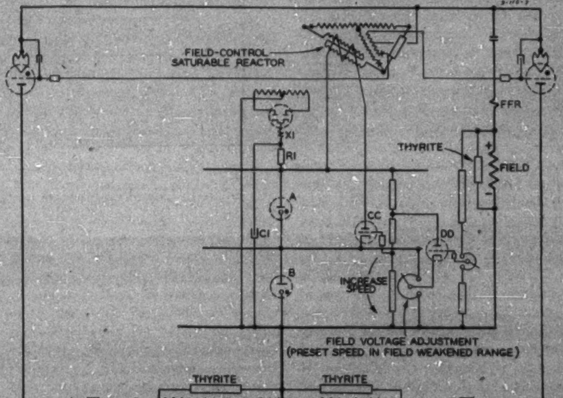
If the field voltage is accepted as an indication of field flux, the control system can be similar to the armature-control circuit in that a voltage-regulating system is used. This regulating circuit is shown in Figure 7. The armature controls are not shown, to avoid confusion. The electric feedback of the field-voltage signal will maintain a preset level of field excitation independent of line-voltage variations.

Where the motor speed is being controlled by varying the armature voltage, the field excitation should remain at rated value to develop rated torque. Conversely, when operating the motor in the weak field range, the armature voltage should remain at rated value to develop constant horsepower. This characteristic can be had by ganging both the armature-control potentiometer and the field-control potentiometer on the same shaft. To do this each control is compressed into half the rotation, with the other half a conducting segment of negligible resistance. These are staggered so that with the armature-control slider in its active range the field-control slider will be on the conducting segment holding full field.

Speed Regulation at Reduced Field Excitation

When the field excitation is reduced the ratio between speed and armature counterelectromotive force is changed such that the armature must rotate at a higher speed in a weaker field in order to generate a given electromotive force. A given preset speed level is maintained relatively constant by grid phase control of the armature thyratrons as they phase

Figure 7 (below). Field rectifier with voltage control



9-116-7
FIELD-CONTROL SATURABLE REACTOR
XI
R1
A
B
C1
CC
DD
INCREASE SPEED
FFR
THYRITE
+
FIELD
-
FIELD VOLTAGE ADJUSTMENT (PRESET SPEED IN FIELD WEAKENED RANGE)
THYRITE
THYRITE

advance to compensate for *IR* drop accompanying changes in armature current.

In this system speed is not regulated by changes in field excitation. Speed is only preset to new levels by the field control but is regulated by armature control. When the armature voltage has been increased to the limit determined by the anode voltage, there can be no further regulatory action.

Field Excitation During Acceleration

It is necessary to apply full excitation to the motor field in order to accelerate it from standstill to base speed. Above base speed the field should be weakened slowly enough to keep the armature current within the commutating ability of the motor. Therefore, the application of armature voltage and the weakening of the field must be controlled by the current-limit circuit. By doing this, the motor is made to accelerate at the maximum possible rate permissible without drawing excessive current. Full field during acceleration from zero to base speed is maintained regardless of the setting of the field-voltage control.

The circuit that accomplishes the aforementioned features is shown in Figure 8. It is similar to the current-limit control on the armature voltage shown in Figure 6 in that the d-c voltage proportional to armature current is compared with a standard voltage, and the difference voltage is applied to a triode. The triode acts on the saturating circuit of the grid phase shifting reactor of the field rectifier.

There are two differences between the armature and the field control. First, an increase in armature current beyond a predetermined amount must increase the field strength by saturating the saturable reactor. When controlling the armature voltage, an increase in armature current necessitates a decrease in the saturation of the armature-control saturable reactor. Second, the standard voltage is not directly the constant voltage of regulator tube *B* but is the slightly variable voltage between the grid of tube *CC* and the negative bus, which voltage is in turn referred to the standard voltage of tube *B*.

The field forcing triode tube *EE* is connected so that, when it is caused to conduct by high armature current, the grid of tube *CC* will be pulled positive. The increased current in the anode of tube *CC* will increase the saturation of the field-control saturable reactor and apply more field.

Figure 8. Voltage-regulated field control with armature-current limit added

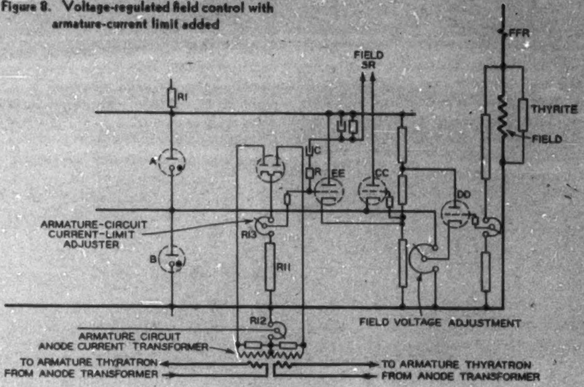

FFR
FIELD SR
R1
THYRITE
FIELD
A
C
R
EE
CC
DD
ARMATURE-CIRCUIT CURRENT-LIMIT ADJUSTER
R13
R11
B
R12
FIELD VOLTAGE ADJUSTMENT
ARMATURE CIRCUIT ANODE CURRENT TRANSFORMER
TO ARMATURE THYRATRON
FROM ANODE TRANSFORMER
TO ARMATURE THYRATRON
FROM ANODE TRANSFORMER

Figure 9. Armature-current-limit control connected to both the armature-voltage and field-voltage control

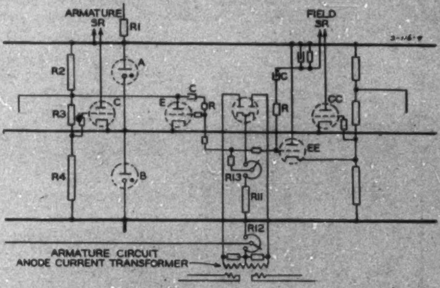
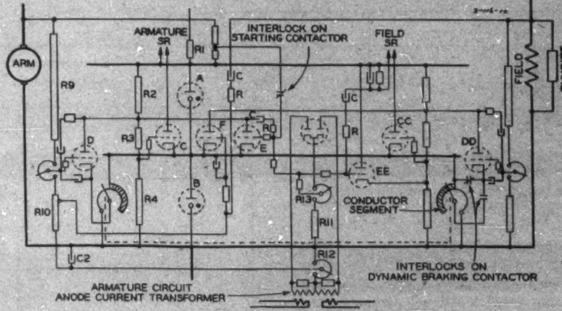

ARMATURE SR
R1
FIELD SR
R2
A
C
R3
C
E
R
C
R
CC
EE
R4
B
R13
R11
R12
ARMATURE CIRCUIT ANODE CURRENT TRANSFORMER

Figure 10 (below) Complete armature- and field-voltage controls


ARM
R9
ARMATURE SR
R1
INTERLOCK ON STARTING CONTACTOR
FIELD SR
FIELD
THYRITE
A
C
R
R2
C
R
R3
D
C
F
C
E
R
CC
DD
EE
R4
B
R10
R13
R11
CONDUCTOR SEGMENT
C2
R12
INTERLOCKS ON DYNAMIC BRAKING CONTACTOR
ARMATURE CIRCUIT ANODE CURRENT TRANSFORMER

The normal operation of the circuit shown in Figure 8 will put the grid of tube *CC* and the cathode of tube *EE* slightly negative of the neutral bus, or almost 75 volts positive with respect to the negative bus. The grid of tube *EE* is connected to the negative bus except for the voltage proportional to armature current. This condition means that field forcing tube *EE* will be nonconducting until the armature current reaches a preset value determined by the setting of *R*13. Therefore, with the armature current below this preset value, the field regulator will operate normally, but tube *EE* will take over and increase the field as soon as the armature current exceeds the preset value.

The field forcing triode tube *EE* will act ahead of the armature current-limit

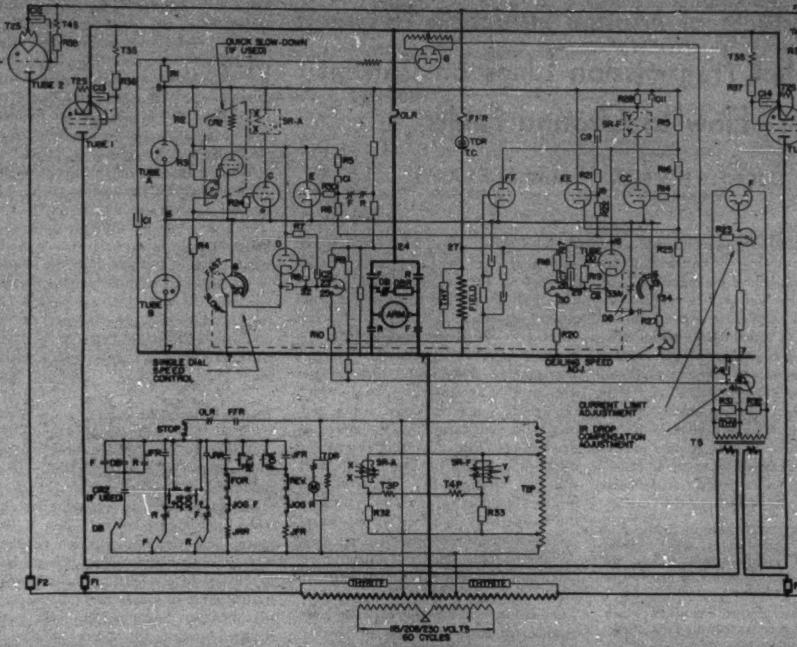


Figure 11. Complete power and control circuits of an electronic drive for a one-horsepower 230-volt d-c shunt motor

triode because the reference voltage of tube *EE* is slightly less than tube *E*. This is diagrammed in Figure 9 where both the field forcing and armature voltage control are shown connected to the same current-limit signal voltage. In the case of suddenly applied loads that do not give time for the field flux to build up to the needed value, the armature voltage will be reduced and prevent overloading the armature supply tubes. The two controls work independently and are arranged so that the field control works at a slightly lower value than the armature control.

The field circuit may be preconditioned to full field value prior to starting by means of interlocks on, say, the dynamic braking contactor. Then, when the starting is initiated, the false signal is removed, but its effect dies off more gradually because of capacitor discharge circuits and maintains full field until the armature current can take control via tube *EE*.

Armature-Voltage Limit When Decelerating

If the speed-control potentiometers are suddenly turned from the weak field condition to a speed calling for full field, the armature voltage will rise above the applied voltage. The voltage will rise because the full field would be applied suddenly and the motor becomes a generator. Because of the rectifying action of the thyratrons, current cannot flow from the machine into the line and dynamically brake the motor as it would be if connected directly to a d-c generator. This would be an objectionable condition, as it would endanger the insulation of the commutator and associated equipment.

This high generated voltage can be avoided by taking a signal from the armature voltage and actuating a triode that will retard the application of field when the voltage exceeds a preset amount. This preset value can be well above the operating value so that it will not interfere with normal control. This control is accomplished by tube *F* of Figure 10.

On nonreversing controls where the armature circuit is interrupted by an anode contactor instead of contacts at the armature terminals this armature-voltage-limit feature allows the use of a lower value of dynamic braking resistor than would normally be used on a motor being stopped from a weak field condition. The delayed field strengthening prevents excessively high armature currents when dynamic braking to rest by limiting the voltage across the armature.

Reversing Control

When the armature circuit is reversed by magnetic contactors between the output of the thyratrons and armature terminals, the duty on the contacts is much less than when the motor is supplied from a d-c generator. The duty on the contacts is reduced because the action of the thyratrons assists in interrupting the armature current. Because of the combination of inverter action and armtuure-current-limit control, the reversing cycle is such as to cause a uniform and rapid deceleration to zero speed, followed by acceleration to the preset speed in the reverse direction. During deceleration to zero speed, the rotational energy of the armature and the load is pumped back into the a-c system with the armature thyratrons. The preset speed in

The Effects of Mutual Induction Between Parallel Transmission Lines on Current Flow to Ground Faults

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THIS paper is intended to present some of the problems encountered in determining unbalanced fault currents and unusual results that may be expected on systems with parallel lines on a common right of way. Correct results are required so that proper co-ordination and application of relays may be accomplished. Fault studies must be made and constantly revised, as a power system changes and expansions take place. Also, future conditions must be studied so that intelligent selection may be made of circuit breakers and current transformers. It is the purpose of this paper to discuss

1. The use of the a-c network analyzer in solving fault problems by using certain mathematical simplifications which are necessary in order to adapt the problem to the analyzer's limitations.

2. Some rather unusual conditions which may occur on a system during fault conditions because of the effect of mutual induction between parallel lines.

Frequently two or more power transmission lines are built on the same right of way parallel to each other for partial distances or for the full length. The problem of mutual induction between such circuits arises when unbalanced faults occur on one line causing induced voltages in the parallel circuit. Where the lines are of the same voltage and bussed at either or both ends, the calculation resolves itself into the use of a simple equivalent circuit. The problem becomes more complicated when the lines are operated at different voltages which obviously cannot be bussed at one end, or when a fault calculation is required on one end of a pair of parallel lines whose opposite ends are unbussed, such as a split-bus arrangement. In solving a problem of the latter type, use is made of the a-c network analyzer which utilizes one-to-one ratio transformers to represent the mutual induction between lines.

Usually fault-current calculations are within the scope of a d-c calculating board. As a power system becomes more complicated and more than one line occupies the same right of way, in some cases operating at different voltages, the effect of mutual induction between the lines becomes important for unbalanced faults. A d-c board cannot be used for these more complicated conditions.

Zero-Sequence Currents

Since the flow of zero-sequence current is not restricted to the physical conductor but must return through the ground circuit, the resistivity of the ground itself enters into the determination of its impedance. The effect of the ground return circuit upon the impedance can be determined by either one of two ways; namely, by direct measurement after the line is constructed, or by assuming an average value of ground resistivity for calculating the impedance by formula. The latter method is the one which must be used in most cases (see appendix). It is not deemed within the scope of this paper to discuss the various theories of current flow through the ground but to show how assumed conditions are used to obtain what are considered reasonable results.

The effect of mutual coupling or the resultant induction between parallel circuits is inversely proportional to the spacing between the circuits and directly proportional to the length of parallel.

Mutual Induction

When two transmission lines parallel each other close enough for the mutual induction to be appreciable, there is transformer action between the circuits when unbalanced currents flow. The mutual induction resulting from the flow of positive-sequence current is small and is reduced to a negligible value by the transposition of conductors. Transposition, however, has no effect on the zero-sequence induction. When paralleling two identical circuits of the positive-sequence network, the resulting impedance is one half of the impedance of one line. This is not true for the zero-sequence network for the reason that the flow of unbalanced currents will cause a certain amount of induction or mutual coupling to exist between the circuits resulting in a value somewhat greater than half the impedance for two identical circuits in parallel. The mutual impedance between the circuits tends to reduce the fault current under certain conditions and increase it under other conditions, de-

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each direction can be independently controlled by connecting different control potentiometers with interlocks on the reversing contactors.

Application to Motor-Generator-Set Drives

For those who have immediate need for this type of control characteristic for motors of, say 50- to 400-horsepower ratings, or even 2,000-horsepower, for that matter, it is practical to modify this equipment so that the thyratron functions associated with variable armature excitation of a one-horsepower motor would, instead, provide field excitation for the generator of a motor generator set, and the thyratron circuit for the field of the one-horsepower motor would be adapted to supply variable field excitation to the field of the larger motor. The loop-current control becomes more complex, particularly since d-c current must be measured directly and its direction of flow ascertained so as to give correct sequence of excitation to the respective fields of generator and motor during acceleration and deceleration. The reversing problem, too, is more complicated because of the necessity of reversing generator field excitation, but all this has been done.

Meanwhile, developments are progressing toward the use of ignitron-type tubes to provide armature excitation for motors of higher horsepower ratings than can now be supplied with the available hot-cathode thyratron tubes.

pending upon the relative directions of the ground current and the induced current. When a single-phase-to-ground fault occurs on an energized line, the resulting zero-sequence currents flowing through all three conductors in the same direction induce a voltage in the coupled circuit tending to cause an induced current to flow in the opposite direction to the one causing it. This may add to or subtract from the existing zero-sequence currents flowing in the coupled circuit to the fault through physical circuit ties.

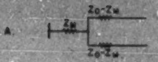
Under certain conditions the amount of induction may be great enough to reverse the current flow in the coupled circuit when large fault currents flow. It may even change the direction of flow in nearby transformer neutrals, causing the current to flow in reverse of the conventional direction.

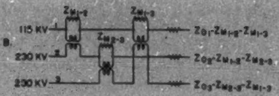
Devices for Representing Effect of Mutual Induction

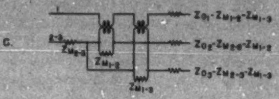
Two lines having mutual induction between them may be represented by an equivalent circuit, provided the lines are bussed at one end. This presupposes that they are operated at the same voltage. The equivalent impedance is determined by subtracting the mutual impedance from the zero-sequence impedance of each line and adding it as a common impedance permitting solution by a reduction of circuits through arithmetical means. See Figure 1A.

This equivalent circuit may be used on an analyzer, but the solution is limited in its scope to lines of the same voltage bussed at one end. A device providing a more general and flexible arrangement is used on the network analyzer with alternating current applied to the analyzer. A transformer with one-to-one ratio is used, shunting an impedance equal to the mutual impedance across one set of terminals such that a voltage drop proportional to the mutual induction is reflected to the coupled circuit, inducing a current in the opposite direction to the originating current. This device may be used for lines of differing voltages and for unbussed lines. The analyzer setup is the same as for the equivalent circuit, and therefore requires that the mutual impedance be subtracted from the zero-sequence impedance of each line and that one side of the transformer be shunted with an impedance equal to the mutual impedance. Either side of the transformer may be shunted by the mutual impedance.

Modern systems may have some rather complicated conditions involving mutual impedance resulting from the construction of several lines on the same right of way, and their operation at different voltages. In solving problems of this type, resort must be made to simplification of circuits because the usual a-c network analyzer has few transformers for representing mutual induction. Where a number of mutual reactances are to be considered, as many simplifications as possible are made to represent the impedances between busses, and expansions are made successively by sections throughout the system as a detailed study is made.







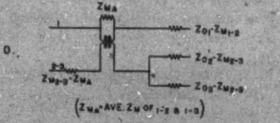


Figure 1A. Equivalent zero-sequence impedance circuit of two parallel lines (same voltage) for calculation of ground-fault current

Figure 1B. Use of one-to-one ratio transformers for representing the mutual induction between parallel lines on the a-c calculating board

Figure 1C. Simplification of the circuits in Figure 1B to reduce the number of one-to-one transformers required for the setup on the calculating board

Figure 1D. Further simplification of Figure 1B. This is only an approximation and is not accurate for all applications

Figure 2 shows a section of a possible system between stations *A*, *B*, and *C* with its several parallels. Figure 3 shows how the section was reduced to an approximate equivalent circuit requiring only two instead of six mutual transformers. Negative values of resistance and reactance may be encountered. The resistance component must be omitted. The reactance component may be handled in one of several ways. Where possible, the negative values of reactance may be combined with the positive values such as would be the case with the primary circuit of the station *B* transformer bank in Figure 3, losing the identity of the station *B* 230-kv bus. Where this cannot be done, a capacitive reactance may be substituted for the negative value of inductive reactance on an a-c analyzer. The diagram in Figure 3 could be further reduced to save reactor units on the analyzer by using a delta-star transformation of the lines, but the net result still shows a negative value of reactance. Negative reactance values can sometimes be avoided if the mutual transformers have a ratio equal to the line voltage ratio. This simplified network would be

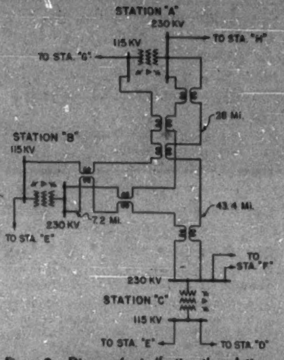


Figure 2. Diagram for indicating the relationships of mutual induction between the parallel lines connecting stations *A*, *B*, and *C*.

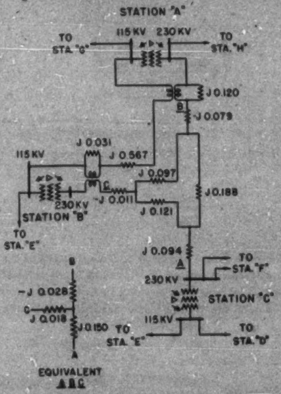


Figure 3. Simplification of the calculating-board circuits representing the lines connecting stations *A*, *B*, and *C* for calculating faults external to this section of the power system

Reactance values are on a per unit 50-megavolt-ampere base

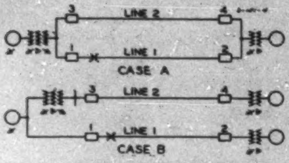

3
LINE 2
4
1
LINE 1
2
CASE A
3
LINE 2
4
1
LINE 1
2
CASE B

Figure 4. Examples of possible false relay operation

Case A represents two 230-kv lines operated temporarily at 115 kv

Case B represents the same pair of lines, with line 1 operated at 115 kv and line 2 operated at 230 kv, each line energized by a separate generator. The location of the ground fault in each case is marked by an *X*

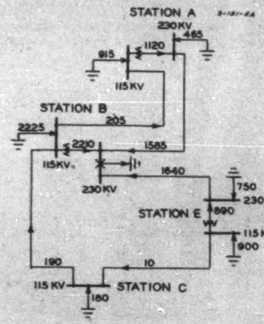

STATION A
230 KV
465
1120
915
115 KV
STATION B
205
2225
2210
1585
115 KV
1640
230 KV
750
230 KV
STATION E
890
115 KV
900
190
10
115 KV
STATION C
180

Figure 5A. Direction and magnitudes of the calculated ground-fault currents in amperes at 115 kv for a fault on the 230-kv bus of station *B* with the mutual reactances included for the calculating-board setup

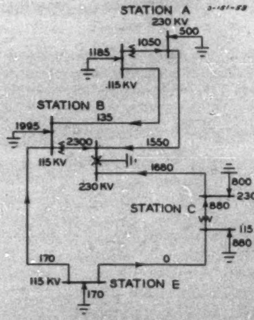

STATION A
230 KV
500
1050
1185
115 KV
STATION B
135
1995
2300
1550
115 KV
1680
230 KV
800
230 KV
STATION C
880
115 KV
880
170
0
115 KV
STATION E
170

Figure 5B. Direction and magnitudes of the calculated ground-fault currents in amperes at 115 kv if the mutual reactances of the parallel lines are ignored in the calculating-board setup

Compare with current values shown in Figure 5A which include the effects of mutual induction

used during the study of faults in another section of the system and would require expansion for detailed study.

In reducing a network consisting of two lines bussed at one end and paralleled by a third line of different voltage, the arrangement is as shown in Figure 1B.

Figure 1B is reduced as shown in Figure 1C by substituting the equivalent circuit of Figure 1A for the lines that are bussed. When the values of mutual impedance are equal or close enough to average, a single transformer may be substituted as shown in Figure 1D because the sum of the induced currents flow in the mutual branch of the equivalent circuit.

False Relay Operation Because of Mutual Induction

Cases A and B, Figure 4, are two examples of how the effects of mutual induction could cause false relay operation if it is not considered in the relaying.

Case A

Case A represents two 230-kv lines operated temporarily at 115 kv. A power source is connected to each end of the circuit, and the two lines are bussed at both ends.

When the fault occurs as shown near the end of the line, a large current flows through breaker 1 resulting in quick tripping. At the same time the induction-type current-polarized directional ground relay at 2 starts to operate. After breaker 1 opens, the direction of current flow in line 2 reverses, and, because of the extra induced current from mutual induction in the line, the directional overcurrent ground relay in position 3 may seal its contacts before breaker 2 opens, causing an unnecessary trip out of the unfaulted line. Breaker 2 would probably have cleared before relay 3 closed its contacts if the added influence of mutual induction had not increased the current in line 2 to speed up the operation of relay 3.

Case B

Case B, Figure 4, represents the same pair of lines except that circuit 2 has been raised to 230 kv, and each line is energized by separate generators.

The fault is again placed at the same point on line 1, causing breaker 1 to open. This entirely isolates the two lines except for mutual induction between them which caused the unbalanced current to reverse in line 2 and therefore reverse the normal direction of the current flow in the transformer neutral. Relay 4, if current polarized, has both its polarizing and actuating current reversed so the resultant direction continues to be the same until breaker 2 opens. Relay 4 may close its contacts before breaker 2 opens, causing false relay operation of the circuit.

Effect of Mutual Induction on Ground Currents

Figures 5–7, inclusive, show diagrams with data obtained from a system fault study made on the a-c network analyzer and provide examples of the effect of mutual induction between circuits for ground faults. These examples are for the same section of the system as shown in Figure 2 except that the line from station *A* to station *C* is omitted. These

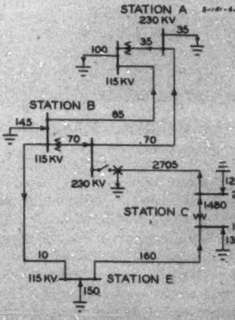

STATION A
230 KV
35
35
100
115 KV
STATION B
65
145
70
70
115 KV
2705
230 KV
1225
230 KV
STATION C
1480
115 KV
1320
10
160
115 KV
STATION E
150

Figure 6A. Because of mutual induction between circuits it is possible to have appreciable ground currents flowing in sections isolated from the ground fault

Note current flow between stations *B* and *A*

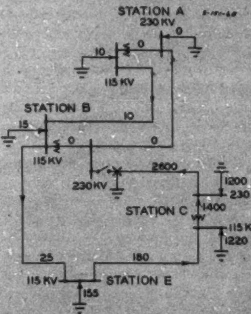

STATION A
230 KV
0
0
10
115 KV
STATION B
10
15
0
0
115 KV
2600
230 KV
1200
230 KV
STATION C
1400
115 KV
1220
25
180
115 KV
STATION E
155

Figure 6B. The effect of ignoring mutual induction

Note negligible flow of ground currents between stations *A* and *B*

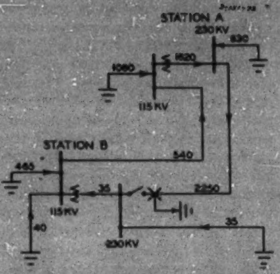


Figure 7A. The effect of mutual induction increases the over-all ground current flowing in parallel lines providing the currents flow in opposite directions

Note the effect on the station *A* to station *B* 115-kv line which parallels the 230-kv line for 35 miles. Compare with Figure 7B

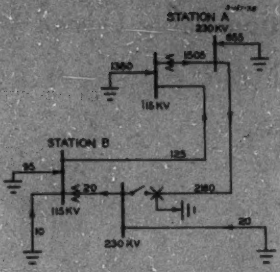


Figure 7B. Mutual induction is ignored in the calculations

The sum of the ground currents circulating in the 115-kv line and the station *B* 115-kv transformer neutral are equal to only 22 per cent of the calculated values of ground currents (shown in Figure 7A) when the mutual induction is included. In this case faulty relay operation could result if the effect of mutual induction is ignored

examples show faults on the end of transmission lines with values of ground current flowing before and after the adjacent breaker to the bus has opened for conditions with and without the consideration of mutual induction. All currents shown are equivalent amperes at 115 kv.

Figure 5 shows the effect of mutual induction between the 115-kv and 230-kv lines for a single-phase-to-ground fault on the station *B* 230-kv bus. When mutual induction is considered (Figure 5A), the calculated value of ground current in the 115-kv line is 205 amperes from station *B* to station *A*. However, when the effect of mutual induction is ignored (Figure 5B), the calculations show a flow of only 135 amperes in the 115-kv line, *but in the opposite direction to that shown in Figure 5A*. In this case the 115-kv line might be cleared by relay unnecessarily for a 230-kv line fault if one were not aware of the effect of mutual induction between paralleling lines of different voltages.

It is possible, as shown in Figure 6, to have appreciable ground currents flowing in sections isolated from the fault because of mutual induction between circuits. The effect shown in Figure 6 would be much more pronounced with longer parallels or larger concentrations of ground currents. Figure 6A shows the magnitude and direction of the ground currents circulating between stations *A* and *B* resulting from the effect of mutual induction when a fault occurs on a radial 230-kv line fed from station *C*. If mutual induction is ignored, results will be obtained similar to those shown in Figure 6B. In the latter case practically no current circulates in the lines between stations *A* and *B*.

The effect of mutual induction increases the over-all ground current flowing in parallel lines providing the currents flow in opposite directions. Figure 7 shows this effect very clearly on the station *A* to station *B* 115-kv line which parallels the 230-kv line for 35 miles. The mutual induction (Figure 7A) increases the ground current in the line and the station *B* transformer neutral practically fivefold over the values obtained in Figure 7B, which neglects mutual induction, for the same 230-kv ground fault. This is an instance that could very easily cause false relay operation, since erroneous results would be obtained by neglecting the mutual induction. Proper results could not be obtained on a d-c board since the lines are of different voltages.

Conclusions

A d-c calculating board cannot be used to represent the effects of mutual induction between lines operated at different voltages. The neglect of these effects may give erroneous answers serious enough to result in false relaying.

With two lines paralleling each other on the same right of way through which unbalanced fault current is flowing in the same direction, the effect of mutual induction between the lines is to reduce the flow of ground current.

The effect of mutual induction between two lines paralleling each other on the same right of way through which unbalanced current is flowing in opposite directions is to increase the flow of ground current.

It is possible, because of the effects of mutual induction between lines, to reverse the normal direction of the ground current flowing in the grounded neutral of a transformer bank to a ground fault.

Appendix. Formulas for Calculations of Zero-Sequence Impedances of Short Transmission Link Without Ground Wires

Zero-Sequence Impedance

$$Z_0 = R_c + 0.00477f + j.01397f \times \log_{10} \frac{D_e}{GMR\text{ circuit}}$$
(1) (Reference 1, page 157)

Z_0 = ohms per phase per mile

$$Z_m = 0.00477f + j.01397f \times \log_{10} \frac{D_e}{GMD\text{ circuit}}$$
(1) (Reference 1, page 158)

Z_m = ohms per phase per mile

f = frequency in cycles

D_e = 2,790 feet based on an average ground resistivity of 100 meter-ohms
(1) (Reference 1, pages 146–8)

$$GMR\text{ circuit} = \sqrt[3]{GMD\text{ sep}^2 \times GMR\text{ conductor}}$$
(1) (Reference 1, page 157)

GMR conductor obtained from tables
(1) (Reference 1, page 138)

$$GMD\text{ sep} = \sqrt[3]{\text{three distances between conductors}}$$

R_c = resistance per mile of one conductor

$$GMD\text{ circuit} = \sqrt[9]{\text{nine distances between conductors}}$$

Conversion of ohms to per unit Z

$$\text{Per unit } Z = \frac{\text{base mva} \times \text{ohms}}{\text{kv}^2} \qquad (2)$$

For mutual impedance between lines of different voltages

$$\text{Per unit } Z = \frac{\text{base mva}^* \times \text{ohms}}{\text{kv}_1 \times \text{kv}_2}$$

References

1. Symmetrical Components (book), C. F. Wagner, R. D. Evans. McGraw-Hill Book Company, Inc., 1933.

2. Calculation of Short-Circuit Currents in A-C Networks, W. M. Hanna. *General Electric Review*, April 1937, pages 180–96.

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* Megavolt-amperes.

High-Voltage-Ignition-Cable Design for Aircraft

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IN the design of an insulation, mechanical and chemical problems must be considered as well as the electrical problem. This is particularly true in the design of an ignition cable for aircraft.

Before constructing a suitable insulated wire or cable for any purpose, it is essential to know the conditions of usage. An aircraft ignition cable may be subjected to the following:

1. Abrasion and chafing.
2. Pulling.
3. Compression.
4. Vibration.
5. Heat.
6. Cold.
7. Moisture.
8. Oil and solvents.
9. Ozone.
10. Nitric acid.
11. Low pressures.

In addition, the insulation must have high dielectric strength and low capacitance.

The cable is subjected to abrasion, chafing, and pulling during assembly into the manifold and flexible conduit leads. This is emphasized by the close fitting manifold, made in an effort to save space and weight. Insufficient space is an enemy of insulation. Many engineers often neglect to allow enough space to prevent injury to insulations. It is appreciated that weight and space saving are very important, especially in aircraft, but proper functioning and safety should take precedence over space saving.

Compression of the cable takes place at the tightly fitting grommets within the spark plug and elbow fittings.

Vibration is quite severe while the engine is running.

According to aircraft-engine manufacturers, and engineers of the Air Services, the temperature in the spark-plug elbow may at times reach 325 to 350 degrees Fahrenheit. The temperature at the spark-plug gasket is 400 to 500 degrees Fahrenheit, and the spark-plug barrel or well may reach a temperature of 375 to 400 degrees Fahrenheit.

We are told that temperatures as low as −70 degrees Fahrenheit or even lower are encountered in high altitude flying and reach this at ground level in parts of the world where our planes are in operation. Although the cables are not flexed or moved at these temperatures, spark plugs may be changed at −20 degrees Fahrenheit or even lower. In such event the cable is flexed or bent to some extent.

Although the cable is entirely enclosed in shielding, moisture enters the system because of breathing, and condensation may take place. If moisture is absorbed by any part of the cable, arcing and premature failure may occur.

The cable comes in contact with lubricating oil and with solvents which are used to wash down and clean the engine.

The cable is exposed to the effects of corona. The ozone formed because of the high electrical stress on the air surrounding the cable is a powerful oxidizing agent. The effects of ozone on the cable can be observed internally next to the conductor as well as externally.

Ozone combines with nitrogen of the air to form oxides of nitrogen. These oxides form nitric acid in the presence of moisture. Nitric acid is also a very powerful oxidizing agent.

High-altitude flying has greatly complicated the problem of designing satisfactory aircraft ignition cables. Cables that function reasonably well at moderately low altitudes fail after relatively short service at very high altitudes.

So far as can be learned, the peak voltages impressed on the cables are in the magnitude of from 7.5 to 10.0 kv.

The dielectric constant of the insulation should be such that the capacitance of the cable will be low enough not to interfere with proper firing of the spark plug.

Consideration of the conditions of service makes it clear that designing a cable that will function satisfactorily is no easy problem. The conventional type of rubber-covered, braided, and lacquered cable is basically lacking in certain respects. The lacquer coating is deficient in resistance to abrasion and chafing. It is difficult, if not impossible, to moistureproof the braid completely. The lacquered braid is readily attacked by the combined action of heat and nitric acid, particularly in the confined space in the spark-plug well and elbow. Furthermore, the lacquer coating when subjected to heat, especially at high altitudes, is seriously affected.

The primary purpose of the lacquer coating is to prevent ozone deterioration of the rubber compound. If the lacquer coating is ruptured, electrical failure is likely to result in a relatively short time.

It is apparent that there is a need for an improved cable, especially for high-altitude flying. The cable designer must therefore find more suitable insulating materials to produce a cable which will meet the requirements. It is also apparent that there must be a meeting of minds of the cable manufacturers, the harness manufacturers, the aircraft-engine manufacturers, and the engineers of the Air Services on certain compromises. It is unlikely that a cable can be made that will meet all the conditions enumerated previously to the extent desired by each individual engineer. One engineer may emphasize heat resistance, another cold resistance, or some other requirement. This emphasis on any specific property can only be met at the expense of some other equally important requirement. What we must arrive at is a balance of properties so that we will obtain a cable with the maximum of any one characteristic without undue reduction of any other necessary characteristics.

The present trend in specification writing is to emphasize performance tests rather than constructional details or requirements based on specific materials. Very often specifications which are not based on performance will prevent the development of improved products. This can occur when new materials become available which will meet conditions of service more satisfactorily but may fail to meet tests based on specific materials. Performance specifications on the other hand encourage development, use of new materials, and better design. It is more difficult to prepare performance specifications since the object is to simulate conditions of service with a reasonable margin of safety. However, when such specifications are finally achieved, it will usually procure a product more satisfactory for the intended purpose.

It is unusually difficult to determine the suitability of an aircraft-engine igni-

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tion cable. It is at best a long procedure to make power-plant tests, and heretofore laboratory tests have revealed comparatively little as to how the cable would perform in actual flight.

Ignition-cable manufacturers have had this under consideration for some time and have attempted to prepare performance specifications that would reproduce as closely as possible conditions actually encountered in service. One test method suggested for incorporation in a specification is termed "simulated flight test." Figure 1 shows this apparatus. It consists principally of a bell jar mounted on a base plate, with proper fittings for attachment of a spark-plug chamber, a spark plug and elbow assembly with a length of standard flexible conduit. A suitable heater coil is mounted around the elbow and spark plug. The bell jar is partially evacuated to the desired pressure. The sample of cable to be tested is assembled in the flexible conduit lead, and the assembly at the spark plug is made identical with that made on an actual engine installation.

These test conditions have been suggested and may be modified as more experience and data are obtained in testing with the apparatus:

Gasket temperature	500 ± 10 degrees Fahrenheit
Elbow temperature	325 ± 5 degrees Fahrenheit
Pressure in electrode chamber	20 ± 5 pounds
Absolute pressure in vacuum chamber	10 ± 0.5 centimeters mercury
Peak voltage	12.5 ± 0.5 kv
Added capacitance	250 micromicrofarad

An attempt has been made to simulate flight conditions by cycling as shown in the tabulation:

Time	Heat	Vacuum	Voltage
Start	On	On	On
$^{1}/_{4}$ hour		Off	
$1^{1}/_{2}$ hours		On	
2 hours	Off		
$2^{1}/_{4}$ hours		Off	
3 hours	On	On	On

The voltage is on continuously, and the schedule is continued a definite number of cycles or until failure occurs.

Figure 1. "Simulated flight test" apparatus with and without bell jar

The cycling is shown graphically in Figure 2.

This test method has been in use by cable manufacturers, and a great deal has been learned about faults and deficiencies of present types of cables. It has also been very useful in the testing of new designs.

Aircraft-Ignition-Cable Design

CONDUCTOR

The size of the conductor materially affects the capacitance of the cable, and its cross-sectional area should therefore be as small as possible, consistent with sufficient tensile strength for manufacturing and normal handling. The conductor commonly used for aircraft ignition cable is made with seven strands of 0.013-inch diameter corrosion resisting steel.

The conductor should be smooth, and the stranding should be concentric. This type of stranding most nearly approaches the form of a solid conductor. Any accentuated unevenness or roughness tends to concentrate the electric stress and promote puncture.

It has been found advantageous to use a "sealed" conductor in the construction of aircraft ignition cables to prevent air leakage through the conductor. Leakage occurs at high-altitude flying when the pressure in the spark-plug well is greater than that of the surrounding atmosphere. In case a sealing compound is used in the spark-plug well, it is apt to be forced into the cable along and through the interstices of the cabled conductor. The presence of air inside the cable will also result in the formation of ozone which may lead to internal failure.

The sealing of the conductor results in better adhesion between the conductor and the insulation, thus preventing the insulation from sliding on the conductor. A sealed conductor tends to reduce twisting or dislocation of the conductor which may be the cause of internal corona cracking because displacement of the conductor with respect to the insulation will permit air to enter the cable along the conductor. Twisting or dislocation places the insulation under stress. Stressed insulation is readily attacked by ozone, resulting in cracking and ultimate failure.

The sealing of the conductor is accomplished by extruding or coating the strand of wire used in the center of the cabled conductor with a suitable compound. When the other six strands are cabled around the coated wire, the compound is forced between the individual strands, completely sealing the conductor.

INSULATION

If a sealed conductor is used, a properly designed rubber compound is satisfactory, since the effect of internal corona is eliminated. Rubber compounds have advantages in that the low temperature flexibility is excellent and electrical properties are good. The rubber stock pile is being rapidly reduced, and it may become necessary to find a substitute. Some of

Table I. Insulations

Material	Dielectric Strength	Resistance to Compression	Heat Aging	Ozone Resistance	Low Temperature Flexibility	Capacitance
Rubber	1	1	2	4	1	1
Buna *S*	1	1	1	4	2	1
Butyl rubber	1	2	1	2	3	1
Polyisobutylene	1	2	1	3	3	1

the synthetic rubbers appear to be suitable, and among those are the buna *S* and isobutylene types. Cables have been made with these substitutes, and tests indicate that they are quite satisfactory.

In Table I are listed materials which have been used or which are under investigation. It is assumed that the material is properly compounded for the application. The materials are rated in accordance with their comparative value as insulation for ignition cable. The lowest number indicates the greatest value.

PROTECTIVE COVERING

A lacquer coating applied over a braid is used to prevent deterioration of the insulation by ozone, oils, and solvents. It is lacking in resistance to abrasion and chafing. The lacquered braid does not have sufficient resistance to heat and nitric acid. It is, therefore, necessary to use a more suitable protective covering.

Table II. Protective Coverings

Material	Resistance	Moisture Resistance	Heat Resistance	Ozone Resistance	Abrasion and Chafing Resistance	Low Temperature Flexibility
Buna *N*'S	2	1	1	3	1	1
Neoprene	2	1	1	2	1	1
Thiokol	1	1	3	1	1	1

In Table II are shown some of the materials that have been investigated and applied as protective coverings for ignition cables. It is assumed that the material is properly compounded for the application. The materials are rated in accordance with their comparative value as protective covering. The lowest number indicates the greatest value.

Most of the failures of ignition cable on aircraft engines occur in the spark-plug well or spark-plug elbow. This is readily understandable, since the cable is subjected to intense ozone concentration at this point as well as nitric acid formed by the combination of ozone, nitrogen, and moisture. The temperature is also highest at this location. Heat combined with low pressure tend to cause blistering or swelling of the protective covering. With lacquered cable, blistering might occur on the first flight at very high altitudes. The condition can be readily reproduced in the "simulated flight test."

The disintegrated lacquer seems to contaminate the porcelain or other type of sleeving placed over the end of the cable as well as the insulating wall of the spark plug. This contamination may induce arc-over from the spring contact to the outside casing of the spark plug. The arc-over is most likely to occur at high altitudes, particularly if the Neoprene grommet does not completely seal the spark-plug well.

The most satisfactory material for a protective covering at present appears to be suitably compounded Neoprene. Neoprene sheaths are reasonably ozone resistant and are improved by incorporating into the compound antioxidants and waxy materials that tend to migrate to the surface. A coating of wax applied to the cable will also improve corona resistance.

Neoprene-sheathed cables have good low temperature flexibility and will withstand flexing at −40 degrees Fahrenheit. These cables should not be bent or flexed at lower temperatures. This resistance to low temperature is better than that of lacquered cables which should not be flexed at temperatures lower than −20 degrees Fahrenheit.

As previously stated, heat and low pressure existing at high altitudes tend to cause blistering or swelling of the Neoprene sheath at the spark-plug elbow. This exposes the insulation to the conditions in the spark-plug well and elbow.

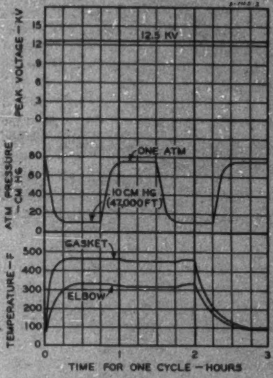

Figure 2. Graphs showing heat, atmospheric pressure, and voltage conditions during "simulated flight test"

It has been found that one factor causing blistering of Neoprene-sheathed cable is that the moisture absorbed by the cable in the steam vulcanization process tends to expand because of the temperature at the elbow. At low pressures the tendency to blister is greatly increased. The blistering caused by moisture can be prevented by preheating the Neoprene-sheathed cable for 24 hours at 180 degrees Fahrenheit.

Because of the limited space in the ignition-cable manifold and the increased surface friction of Neoprene-sheathed cables, there is greater mechanical abuse in wiring the harness than when lacquered cable is used. A glass-yarn braid applied between the primary insulation and the Neoprene sheath will impart added mechanical strength to the cable and help prevent stretching and twisting of the cable during manifold assembly. A glass-yarn braid also materially increases ozone resistance of the cable.

One disadvantage of a glass-yarn braid is the relative difficulty of sealing the cable when the pressure surrounding the spark plug and elbow is low as occurs at high altitudes. Air tends to flow through the interstices of the braid which may cause blistering or swelling of the Neoprene sheath at the elbow and lower the pressure in the spark-plug well. The spark-plug-well sealing compound may be forced up into the braid. This tendency of leakage through the braid can be partially overcome by applying a thin sheath of Neoprene over the insulation.

Table III. Relative Merit of Cables

	Braided With Lacquered Coating	Neoprene Sheath No Braid	Neoprene Sheath Glass-Yarn Braid Over Insulation	Neoprene Sheath Glass-Yarn Braid Between Layers of Neoprene
Abrasion and chafing	Poor	Excellent	Excellent	Excellent
Pulling	Fair	Poor	Excellent	Excellent
Compression	Poor	Fair	Fair	Fair
Heat	Fair	Good	Good	Good
Cold	Fair	Good	Good	Good
Moisture	Fair	Excellent	Good	Good
Oil	Excellent	Good	Good	Good
Aromatic solvents	Poor	Fair	Fair	Good
Ozone	Excellent	Good	Good	Excellent
Nitric acid	Poor	Excellent	Excellent	Excellent
Low pressure	Poor	Excellent	Fair	Good

Figure 3. Four ignition-cable constructions

A. Sealed conductor; insulation; Neoprene sheath

B. Sealed conductor; insulation; glass-yarn braid; Neoprene sheath

C. Sealed conductor; insulation; Neoprene layer; glass-yarn braid; Neoprene sheath

D. Conductor; insulation; cotton yarn braid; lacquer coating

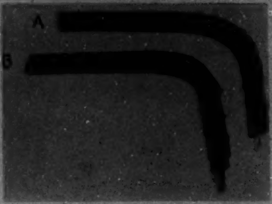

Figure 4. Samples of lacquered cable showing deterioration at spark-plug elbow

A. After "simulated flight test"

B. After service on aircraft engine

The glass-yarn braid is then applied, followed by a second sheath of Neoprene. The braid thus becomes thoroughly imbedded between the layers of Neoprene. This construction further increases the ozone resistance of the cable.

Figure 3 shows constructions which have been discussed. Figure 4 shows deterioration of lacquered-type cable at spark-plug elbow after being subjected to "simulated flight test" and after service on aircraft engine.

Use of Five-Millimeter Cable in Place of Seven-Millimeter Cable

In the wiring of the conventional type of manifold, the seven-millimeter cable receives considerable abuse. Undue pulling or twisting of the ignition cable in harness wiring may occur, resulting in very serious permanent injury to the insulation. Twisting of the cable is apt to produce air pockets along the conductor because of dislocation of the insulation with respect to the conductor. Excessive pulling on the cable may result in elongation of the conductor and stressing of the insulation. The Neoprene-sheathed cable is more resistant to abrasion and chafing than braided and lacquered type, but nevertheless must be handled with reasonable care. Because of the limitations of space, it is doubtful whether seven-millimeter Neoprene-sheathed cable can be assembled in the present manifolds without injury to the cable. The use of five-millimeter cable in place of seven-millimeter cable would simplify the problems of harness assembly and remove the principal source of cable abuse.

Since the puncturing voltage of the five-millimeter Neoprene-sheathed cable is ample, and since cable failure is largely due to deficiencies in chemical and physical properties, it appears that this cable would be entirely satisfactory. The capacitance of an installation with five-millimeter cable is satisfactory since with present manifold and flexible conduit sizes and the same type of cable, the capacitance of the individual leads is less than when seven-millimeter cable is used because of the additional air space in the manifold. This favorable condition would only exist so long as the manifold size remains the same or is reduced only to a point that ample space is allowed to prevent mechanical injury to the insulation of the five-millimeter cable. Should the manifold size be reduced to fit the five-millimeter cable as closely as the present manifold fits the seven-millimeter cable, the same objectionable difficulties in assembly will exist.

Four types of cables have been under consideration. None of these cables meet all of the requirements to the extent desired. Any one is superior in some respects and deficient in other respects. It must be recognized that some of the requirements asked for by aircraft-engine manufacturers and engineers of the Air Services cannot be fully met. As an example, if it is essential to have a cable withstand the solvent action of highly aromatic gasolines, none of the cables under consideration would be entirely satisfactory. Again, if flexibility much below −40 degrees Fahrenheit is required, these cables would not be suitable.

In Table III, an attempt has been made to estimate the relative merits of the four cable constructions under consideration as to the conditions of usage on engine installations previously outlined.

Cable manufacturers at present are doing intensive development work in an effort to improve aircraft ignition cables. The numerous new materials which are being made available by the chemical industry are being thoroughly investigated, and advantage is being taken of those showing promise. Recent close cooperation between the engineers of the Air Services, the aircraft-engine manufacturers, and the cable manufacturers has greatly accelerated the development and improvement of ignition cables, and this will no doubt result in continued progress.

Dethermalizing Arc Quenchers

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Synopsis: The primary function of the normal-type oil circuit breaker is to interrupt the circuit as rapidly as possible, without disturbing the connected system, and to leave the oil circuit breaker in substantially the same electrical and mechanical condition as before. The interruption of an electrical circuit is always accompanied by an arc, and it has been found that if the arc and its associated space are rapidly cooled or dethermalized it is readily extinguished. This paper describes dethermalizing arc quenchers that cause the arc and its associated space to be cooled by the controlled flow of oil through and around the arc. The action of these quenchers has been verified by field tests on the 16-kv and 220-kv systems of The Southern California Edison Company Ltd.

In the operation of large electric systems, effort is continuously exerted to gain reliability and at the same time to cut down maintenance costs. One of the costly maintenance problems is the overhauling of oil circuit breakers. It is the customary rule, based on experience, to overhaul oil circuit breakers after each short circuit or at least after two interruptions of short circuit. The overhaul periods of the oil circuit breakers could be greatly lengthened if the contact burning and the oil carbonization could be reduced. In order to accomplish these desired results it is important to cut down arcing time and contact separation during the arcing period.

Conclusion

The proper design of dethermalizing arc quenchers will interrupt short circuits of any magnitude without carbonizing the oil or changing its dielectric value, and because of the short arcing time the burning of the contacts becomes negligible.

Even though this short arcing time is accompanied by an extremely short contact separation, the continuously flowing oil prevents restriking of the arc. Consequently, with short arcing time and short arc lengths the arc energy is so greatly reduced that mechanical disturbances are eliminated.

The field tests on the Southern California Edison Company Ltd. system proved that the dethermalizers are adequate to interrupt short circuits ranging from a minimum to maximum duty without mechanical disturbance to the switch and without electrical disturbance to the system. The time of interruption decreased with the greater duty, thus giving the switch a larger factor of safety or an increased kilovolt-ampere rating.

Tests were made on the Edison system at widely separated locations, resulting in the same interrupting characteristics; that is, with the same kilovolt-ampere duty, the arcing time and contact separation were substantially the same, proving that the dethermalizers are adequate for all locations on the Edison system.

The record of operation of these dethermalizers during the past six or seven years has shown no deterioration of oil contacts, or insulating materials of the interrupters, and they have cleared all short circuits and line dropping incident to normal operation.

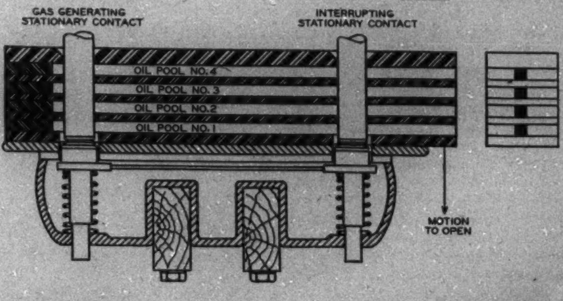
From this operating experience it becomes apparent that this type of arc quencher will result in the very minimum of maintenance and at the same time will give a maximum of system protection.

16-Kv Field Tests, August 25, 1936

Since the Edison company is practically the only company that uses 16 kv for distribution, it was desirable to have a breaker for this voltage with approximately 50,000-kva interrupting rating, small in size and fast in operation. Breakers of the normal type had insufficient interrupting capacity and required considerable maintenance. The dethermalizer for this breaker is shown in Figure 1. It can be seen from this sketch that the dethermalizing element has a multiplicity of pools of oil that are connected to restricted ports on one end, the other end is closed. The arc at the closed end generates gas which produces sufficient pressure to drive the oil in the pool, located between the contacts, through the arc and associated space of the other contact. As the contacts separate, other pools of oil are successively driven into the arc and the arc space of the second contact thus continually cooling the arc and the arc space until a point of extinction occurs, and sufficient cool oil is maintained through this space even after extinction so that restriking does not occur. This rapid cooling of the arc and its associated space is the dethermalizing action that extinguishes the arc.

Figure 2 illustrates a 16-kv 50,000-kva series-trip mechanical trip-free breaker set up for test. This breaker interrupted a 53,000-kva three-phase short circuit on a delta system feeding only the setup from a separate transformer bank. It operated on a close-open test in $3^1/_2$ cycles total time. Figure 3 shows the oscillogram of this test. This switch was closed by hand on a three-phase short circuit and cleared with no disturbance. This test was repeated several times with the same results and when examined showed very little deterioration of contacts, oil, and dethermalizer. Breakers of this design have an operating record of six and one-half years, show no signs of oil carbonization, and have not required any maintenance.

Figure 1. 16-kv dethermalizer, flat-plate construction



Paper 43-142, recommended by the AIEE committee on protective devices for presentation at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943. Manuscript submitted June 18, 1943; made available for printing July 20, 1943.

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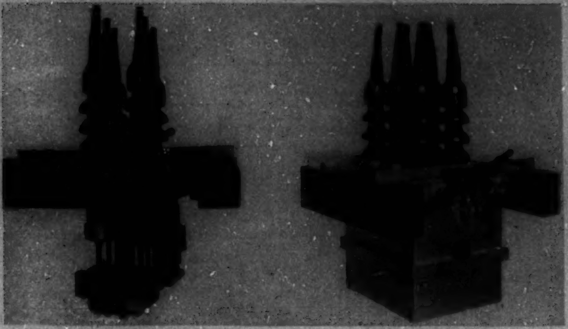


Figure 2. 16-kv oil circuit breaker with dethermalizers set up for field test

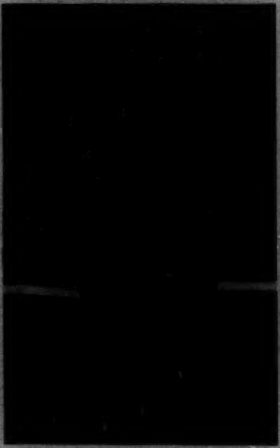


Figure 3. 16-kv close–open test, 53,000-kva three-phase short circuit

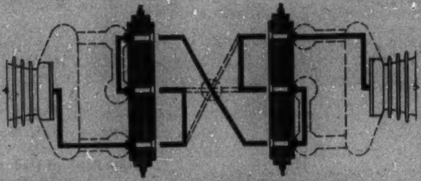
230-Kv Dethermalizers

The dethermalizers for the 230-kv oil circuit breakers are made in the form of six breaks per pole or three breaks per bushing. The electric connections and sequence of these breaks are shown by Figure 4. The dethermalizers are made in the form of large tubes. Figure 5 shows the end view of the tube type. The graded ports can be seen in this figure. Each dethermalizer is mounted on the end of the switch bushing as shown by the assembly photograph, Figure 6. This illustration clearly shows the shielding and one-half the crosshead.

Figure 7 shows the dethermalizer of the flat-type construction and the various oil pools or reservoirs. The end view shows the graded ports and the plan view shows the restriction of the ports.

When the switch contacts are parting, arcs are formed at each contact. The arc at the center contact will generate sufficient pressure to force the oil in the pools (located between the center contact and the outside contacts) through the outside arcs and through the ports at each end of the large tube. As the contacts are opening, new pools of oil are successively forced through the outside

Figure 4. Arrangement of six-break dethermalizers



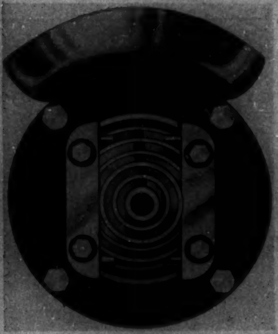


Figure 5. End view of 230-kv tube-type dethermalizer

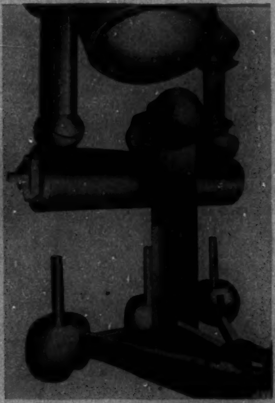


Figure 6 (right). Assembly of 230-kv dethermalizer on switch bushing

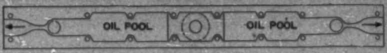


Figure 7. Type *H* dethermalizer flat-type construction, 230 kv

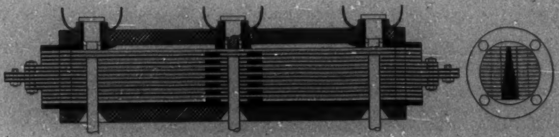


Table I. Tests Made at Saugus Substation, October 4, 1936

Kelman Dethermalizing Arc Quenchers, Type As Indicated

Test Number	Initial Voltage Phase to Phase (Kilovolts)	Type of Quencher Tube	Number of Breaks Per Phase	Phase Tank	Duty Cycle	Current Interrupted (Rms Amperes)	Equivalent Three-Phase Kilovolt-Amperes Interrupted	Closing Time (Cycles)	Over-all Time of Short Circuit (Cycles)	Relay Time (Cycles)	Breaker Interrupting Time (Cycles)	Contact Separation When Arc Extinguished (Inches)	Total Arc Length (Inches)
1	220	*C*	6	*A*	*O*	778	296,470		8.0	1.0	7.0	$3^{1}/_{8}$	$18^{3}/_{4}$
1*A*	220	*C*	6	*A*	*CO*	883	336,480	65	7.0	1.0	6.0	3	18
1*C*	220	*	6	*C*	*O*	787	299,897		6.5	1.0	5.5	$2^{1}/_{4}$	$13^{1}/_{2}$
1*AC*	220	*	6	*C*	*CO*	777	296,090	67	9.0	1.0	8.0	$3^{1}/_{8}$	21
2	220	*C*	6	*B*	*O*	1,162	442,800		6.5	0.75	5.75	$1^{5}/_{8}$	$8^{1}/_{4}$
2*A*	220	*C*	6	*B*	*CO*	1,171	446,225	68	6.5	1.0	5.5	$1^{3}/_{8}$	$8^{3}/_{4}$
3	220	*C*	6	*B*	*O*	Not recorded because of oscillograph film choke. Same kilovolt amperes interrupted as test 3A							
3*A*	220	*C*	6	*B*	*CO*	2,055	783,090	69	7.0	0.75	6.25	$1^{5}/_{8}$	$9^{3}/_{4}$
4	220	*C*	6	*B*	*O*	4,142	1,578,363		5.5	0.75	4.75	$1^{1}/_{8}$	$6^{3}/_{4}$
4*A*	220	*C*	6	*B*	*CO*	4,526	1,730,000	69	5.5	1.0	4.5	$1^{1}/_{8}$	$6^{3}/_{4}$
4*A*1	220	*C*	6	*B*	*CO*	4,249	1,619,140	69	5.3	1.0	4.3	1	6

* In tests 1*C* and 1*AC* the dethermalizers were of the flat-plate construction, and the ports were not graded. All other tests were made with the type *C* dethermalizers which were of the round-tube construction with graded ports.

O—Open. *CO*—Close-open.

Table II. Tests Made at Laguna Bell Substation

Tests Made on July 10, 1938, on Kelman RA6J Breaker, 1,200 Amperes, 230 Kv, Serial Number 16408

Test Number	Initial Voltage Phase to Phase (Kilovolts)	Type of Quencher Tube	Number of Breaks Per Phase	Tank	Duty Cycle	Current Interrupted (Rms Amperes)	Equivalent Three-Phase Kilovolt-Amperes Interrupted	Over-all Time of Short Circuit (Cycles)	Relay Time (Cycles)	Breaker Interrupting Time (Cycles)	Breaker Dead Time (Cycles)	Arcing Time (Cycles)	Contact Separation When Arc Extinguished (Inches)	Total Arc Length (Inches)
Test Made on July 10, 1938, Original Installation, $1^{1}/_{4}$-Inch Bayonets and $1^{1}/_{2}$-Inch Clearance Holes														
1	210	*A*	6	West	*O*	660	240,000	25.0	0.36	24.64	2.60	22.04	$17^{5}/_{8}$	$104^{1}/_{4}$
1*A*	210	*A*	6	West	*CO*	630	230,000	12.2	0.48	11.72	3.94	7.78	$5^{1}/_{4}$	$31^{1}/_{2}$
1_1	210	*A*	6	West	*O*	660	240,000	12.0	0.39	11.61	2.80	8.81	$5^{1}/_{2}$	33
1_1A	210	*A*	6	West	*CO*	660†	240,000	24.5	0.43	24.07	3.90	20.17	$17^{1}/_{2}$	105
Note: Larger values of short-circuit current were not imposed because of the excessive arc length.														
Test Made on July 31, 1938														
1-*C*	210	*C*	6	East	*O*		No record obtained							
1-*C*	210	*C*	6	East	*O*	576	210,000	8.10	0.37	7.73	3.26	4.47	$2^{3}/_{8}$	$14^{1}/_{4}$
1-*G*	210	*G*	6	West	*O*	576	210,000	8.02	0.40	7.62	2.75	4.87	$2^{5}/_{8}$	$15^{3}/_{4}$
1-*H*	210	*H*	6	Middle	*O*	573	209,000	7.97	0.36	7.61	2.71	4.90	$2^{3}/_{4}$	$16^{1}/_{2}$
1*A*-*C*	210	*C*	6	East	*CO*	570	207,000	10.3	0.34	9.95	4.11	5.85	$3^{5}/_{8}$	$21^{3}/_{4}$
1*A*-*G*	210	*G*	6	West	*CO*	576	210,000	10.47	0.33	10.14	4.03	6.11	$3^{7}/_{8}$	$23^{1}/_{4}$
1*A*-*H*	210	*H*	6	Middle	*CO*	576	210,000	8.92	0.31	8.61	4.21	4.4	$2^{5}/_{8}$	$15^{3}/_{4}$
2-*C*	210	*C*	6	East	*O*	1,517	551,000	6.54	0.26	6.28	2.79	3.49	$1^{3}/_{4}$	$10^{1}/_{2}$
2-*G*	210	*G*	6	West	*O*	1,529	557,000	6.55	0.37	6.18	3.09	3.09	$1^{1}/_{2}$	9
2-*H*	210	*H*	6	Middle	*O*	1,520	553,000	6.02	0.30	5.72	2.72	3.00	$1^{3}/_{8}$	$8^{1}/$
2*A*-*C*	210	*C*	6	East	*CO*	1,529	556,000	7.09	0.29	6.80	4.08	2.72	$1^{5}/_{8}$	$9^{3}/_{4}$
2*A*-*G*	210	*G*	6	West	*CO*	1,525	555,000	8.00	0.34	7.66	4.14	3.52	2	12
2*A*-*H*	210	*H*	6	Middle	*CO*	1,517	552,000	7.68	0.36	7.32	4.06	3.26	$1^{7}/_{8}$	$11^{1}/_{4}$
3-*C*	210	*C*	6	East	*O*	2,720	990,000	6.55	0.31	6.24	2.98	3.26	$1^{5}/_{8}$	$9^{3}/_{4}$
3-*G*	210	*G*	6	West	*O*	2,780	1,011,000	6.05	0.28	5.77	3.01	2.76	$1^{3}/_{8}$	$8^{1}/_{4}$
3-*H*	210	*H*	6	Middle	*O*	2,750	1,000,000	5.04	0.46	4.58	2.79	1.79	$^{3}/_{4}$	$4^{1}/_{2}$
3*A*-*C*	210	*C*	6	East	*CO*	2,659	967,000	6.59	0.40	6.19	4.17	2.02	$1^{1}/_{4}$	$7^{1}/_{2}$
3*A*-*G*	210	*G*	6	West	*CO*	2,639	960,000	7.20	0.38	6.82	4.02	2.80	$1^{3}/_{4}$	$10^{1}/_{2}$
3*A*-*H*	210	*H*	6	Middle	*CO*	2,610	950,000	6.63	0.30	6.33	4.03	2.30	$1^{3}/_{8}$	$8^{1}/_{4}$
4-*G*	210	*G*	6	West	*O*		No record obtained							
4*A*-*G*	210	*G*	6	West	*CO*	4,720	1,719,000	6.06	0.35	5.71	3.89	1.82	1	6
4*A*-*H*	210	*H*	6	Middle	*CO*	5,075	1,850,000	5.97	0.30	5.67	4.05	1.62	1	6
5-*H**	210	*H*	6	Middle	*O*	6,200	2,259,000	5.48	0.25	5.23	3.26	1.97	$^{3}/_{4}$	$4^{1}/_{2}$
5*A*-*H**	210	*H*	6	Middle	*CO*	6,070	2,100,000	6.06	0.30	5.76	4.20	1.56	$^{7}/_{8}$	$5^{1}/_{4}$

* See oscillograms in Figures 11 and 12.

Types of quencher tubes: Type *C*—Round-tube type as tested at Saugus 10-4-36. 1.050-inch bayonets, center clearance-hole $1^{3}/_{16}$ inch and clearance-holes round-end, $1^{3}/_{16}$ inch x $1^{7}/_{16}$ inch.

Type *G*—Flat-plate style, designed to have the characteristics of type *C*, as nearly as practical. 1-inch bayonets, clearance-holes $1^{1}/_{16}$ inches.

Type *H*—Flat-plate style. 1-inch bayonets, clearance-holes $1^{1}/_{16}$ inches. Graded ports.

After the test the contacts were found to be slightly burned, and a slight carbon deposit was found on the quencher tubes. A test of the oil showed no visible trace of carbon.

O—Open. *CO*—Close-open.

† Larger values of short-circuit current were not imposed because of excessive arc lengths.

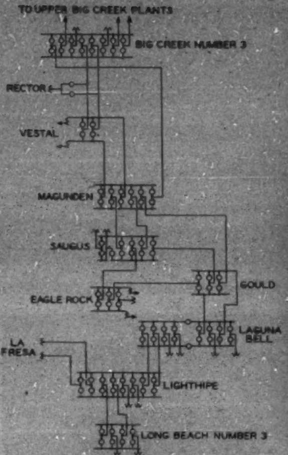


Figure 8. 220-kv system of the Southern California Edison Company, Ltd.

Figure 9. Kelman 230-kv 1,200-ampere 2,500,000-kva oil circuit breaker equipped with dethermalizer

arcs until the circuit is interrupted. At the time of extinction, fresh oil is between the contacts so no restriking occurs. In this arc quencher fourteen ports are used. The tube type operates in the same manner.

Further development has been made on a two-break device which will be the subject of a future paper.

Saugus and Laguna Bell Substations—Field Tests

In order to check the operation of these dethermalizers, actual phase-to-ground short-circuit tests were made on the main 220-kv system at the Saugus and Laguna Bell substations of the Southern California Edison Company Ltd. Figure 8 shows the 220-kv connections of the Edison system at the time of these tests.

The various kilovolt-ampere duties for the switch under tests were obtained by system arrangements. The lowest duty at these two locations was accomplished by feeding 220 kv to 66 kv and then back to 220 kv. This gave the impedance of the two transformer banks in series for limiting the short-circuit kilovolt-amperes. The other steps were accomplished by separating a line from a distant station, which supplied the test breaker. Then by selecting a shorter line the kilovolt-amperes duty was increased, and the final test at the two stations was the maximum kilovolt-amperes that could be delivered at the 220-kv bus, that is, all lines and transformer banks in service. At the Saugus substation this amounted to an equivalent three-phase short circuit of 1,730,000 kva, and at Laguna Bell it amounted to 2,259,000 kva.

The breakers at both locations performed perfectly throughout the tests without any mechanical trouble or without disturbing the system. After the series of tests the oil was tested and filtered and did not show any perceptible increase of carbon in the oil.

Figure 9 shows the test breaker at Laguna Bell substation. Figure 10 illustrates bars of various lengths that were placed on end on a steel plate between two tanks of the breaker. The mechanical movement of the breaker under test was so small that none of these bars were knocked over.

Table I is the tabulation of the tests taken at Saugus substation on October 4, 1936.

Table II is the tabulation of the tests taken at Laguna Bell substation on July 10, 1938 and July 31, 1938.

In these tests the type *C* dethermalizers are of the tube type, as shown by Figure 5, the *H* type are as shown by Figure 7, and the *A* type are of the flat-plate construction with more restricted ports.

The series of tests on July 10, 1938 were not continued above the values

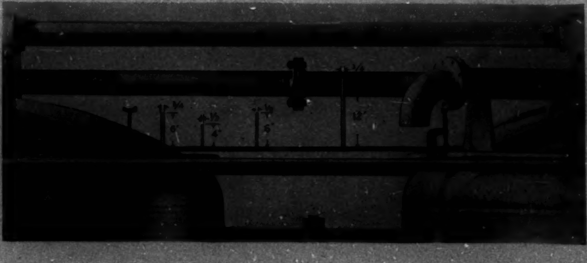


Figure 10. Rods for determination of switch movement

The four steel pins were set on end to test for any jar to the breaker during opening of short circuits. Pins were turned square on ends and set on a [illegible]-inch by 4-inch steel bar. On 230-kv tests at capacities from 554,000 kva to 2,259,000 kva, throughout a total of 16 tests, none of the pins fell. On the lighter tests, a high-speed moving-picture camera showed slight vibration of the pins, which vibration became less as the duty increased. On the heaviest tests, no vibration was found

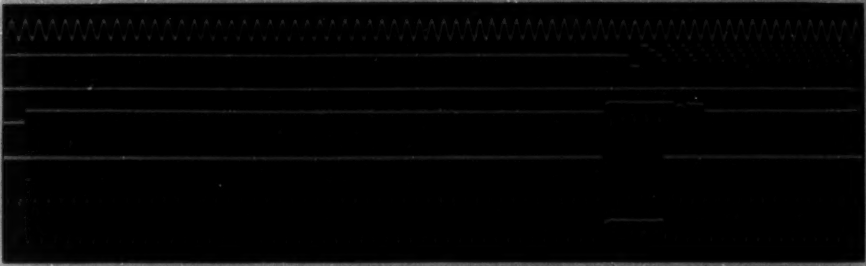
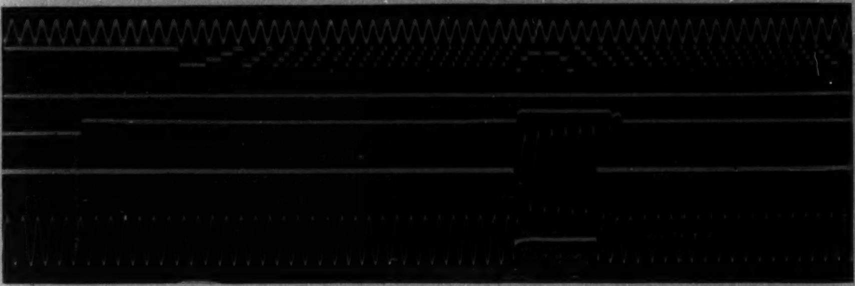


Figure 11 (above). Oscillogram of opening test at 2,259,000 kva

Figure 12 (below). Oscillogram of close–open test at 2,100,000 kva



shown because of the excessive arc length. It was discovered that these dethermalizers had too much clearance around the contacts and the ports were too much restricted, causing the oil to be forced out around the contacts giving practically no quenching effect. This difficulty was remedied, and the tests were completed on July 31, 1938.

It will be noted from these tables that as the duty increased the contact separation decreased at the time of arc extinction.

Oscillograph records were taken of these tests to give an accurate picture of the operation of the breaker. Figure 11 is an oscillogram of an opening operation with a full capacity short circuit at Laguna Bell substation. With the travel recorder it is possible to picture accurately the breaker action. From Figure 11 it can be seen that the arc was extinguished at exactly $^{3}/_{4}$-inch contact separation without any restrikes. Figure 12 is an oscillogram of a close–open operation of a full capacity short circuit at Laguna Bell substation. It can be readily seen from this figure that the prestrike was only $^{1}/_{4}$ inch, and contact separation at arc extinction was $^{7}/_{8}$ inch.

From these oscillograms and tables it should be noticed that dead time of the breaker was excessive. This was later corrected by a change in the tripping toggle.

It can be seen from the two oscillograms that no voltage disturbance occurred at arc extinction.

Since the Saugus tests were the first short-circuit tests to be made on the 220-kv Edison system, every precaution was taken to see that the 220-kv protection was in order and all switching was correct. The dispatching and switching were so well planned and the breaker functioned so perfectly that every test was carried out at the scheduled time without any system disturbance. This successful achievement allowed the privilege of future 220-kv short-circuit tests.

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A New One-Cycle Directional Overcurrent Relay

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Synopsis: This paper describes a new one-cycle directional overcurrent relay which is not limited in its application by lack of contact co-ordination. That contact co-ordination is important is indicated by the many incorrect relay operations which have been traced to the common practice of adding instantaneous attachment to directional overcurrent relays. For years it has been known that directional control is not sufficient for instantaneous directional overcurrent relays. This paper describes a new application of "memory action" which for the first time complements directional control to give always correct operation. The new idea is to use "memory action" to obtain a time delay in the operation of the overcurrent unit in the order of one-half cycle. Of course "memory action" is also used in the directional element to retain a high torque level even if the fault causes drastic voltage reduction. Both elements are of the rugged induction cylinder.

HIGH-speed relaying has become one of the most common means of permitting operation of a power system nearer its stability limit. Thus, extension of power transmission facilities has often been obviated. This, together with reduction of damage at the point of fault, has undeniably meant the saving of a large amount of critical material.

An important development in the high-speed relay family has been the distance relay with a stepped time–distance characteristic. With this type of relay, simultaneous instantaneous tripping is obtained for faults in the middle 80 per cent of the line section, and instantaneous tripping at one end for faults in the remaining 20 per cent. This is true over a large range of generating capacity.

For the protection of most of the lower voltage lines, distance relaying has generally been considered to be too expensive. On the other hand, it often occurs that the short-circuit currents of such lines are relatively fixed because the determining impedances are largely in the lines and transformers rather than in the connected generators. Thus, it is often possible to approach distance relay performance by means of instantaneous overcurrent relays. Generally, tripping must be allowed for faults in one direction only, thus necessitating directional action.

Application Requirements

Any directional overcurrent relay, whether instantaneous or time delay, may operate incorrectly while the fault is being cleared or after it has been cleared, if the overcurrent unit is not prevented from closing its contacts when the fault is in the nontripping direction. Fundamentally, the cause of incorrect operation is the loss of a contact race. If the overcurrent unit has operated because of a fault in the nontripping direction, it must open its contacts before the contacts of the directional unit can close, under any condition causing reversal of the directional unit.

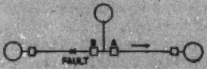

Figure 1. Basic system requiring correct directional action after fault is cleared

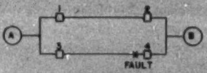

Figure 2. Basic system requiring correct directional action during sequence of circuit-breaker operation

Figure 1 shows a system where the directional overcurrent relay at *A* might trip the circuit breaker at *A* incorrectly after the fault is cleared, if the overcurrent unit is allowed to operate during the fault. If the normal load current is as shown by the arrow, and a fault appears at *X* as indicated in Figure 1, the contacts of the directional unit open, and the contacts of the overcurrent unit close. The instant that circuit breaker *B* opens, the current through the relay at *A* reverses and drops in magnitude. The directional unit closes its contacts quickly because it now has full voltage and load current applied to it. The overcurrent unit, on the other hand, opens its contacts slowly if the magnitude of the load current is near the drop-out value of the overcurrent unit. If the directional-unit contacts close before the overcurrent contacts can open, circuit breaker *A* will be tripped falsely even though circuit breaker *B* has already cleared the fault.

Figure 2 shows a system where a directional overcurrent relay might operate incorrectly during the sequency of circuit breaker operations to clear the fault, because of the fault current reversing when the first circuit breaker opens. For example, when the fault occurs, the overcurrent and directional units at 4 operate. If the generation at *A* is greater than that at *B*, the overcurrent unit at 2 may also operate, but circuit breaker 2 is not tripped because the fault current is in the direction to hold the directional-relay contacts open. When circuit breaker 4 opens, the current through circuit breaker 2 reverses causing its directional relay to reverse. If the directional-unit contacts close before the overcurrent-unit contacts can open, circuit breaker 2 is falsely tripped.

For simplicity, parallel lines were used in Figure 2. The same difficulty may arise in loop circuits.

Instantaneous directional overcurrent relays for phase protection have a contact

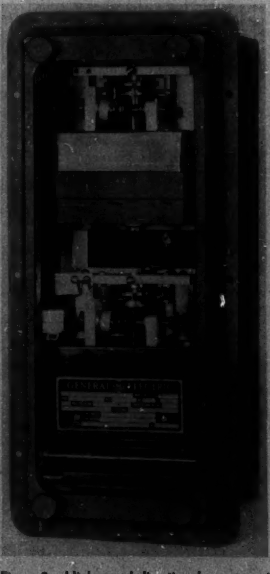

Figure 3. High-speed directional overcurrent relay

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race which is not present either in time-delay directional overcurrent relays or in instantaneous directional overcurrent relays for ground-fault protection. This is the race between the contacts of the directional-unit opening and the overcurrent contacts closing at the inception of the fault. For example, if load in Figure 1 is flowing in the direction of the arrow, the directional-unit contacts at *A* are closed. When the fault appears at *X*, the instantaneous overcurrent-unit contacts of the same relay begin to close. At the same time, the directional unit is attempting to open its contacts because of the reversal of the direction of current flow. Obviously, if the contacts of both units are closed simultaneously during the transition, circuit breaker *A* is tripped falsely.

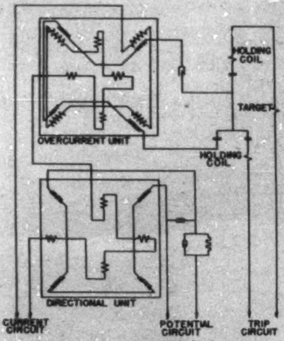


Figure 4. Internal connections of relay

Figure 5. Test oscillogram. Fault in non-tripping direction

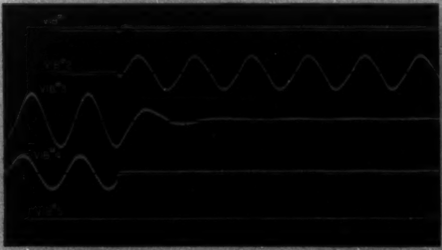
Vibrator 1—Current in flux-shifting coils of overcurrent unit

Vibrator 2—Line current

Vibrator 3—Current in potential coil of directional unit

Vibrator 4—Voltage across potential circuit of directional unit

Vibrator 5—Current in trip circuit



Development of the Relay

In developing a high-speed directional overcurrent relay that operates correctly under any circuit condition, it is necessary to obtain proper contact co-ordination.

Directional control, as in the case of the time overcurrent relay, prevents incorrect tripping either after the fault is cleared or when there is a reversal of short-circuit current during the fault.

To win the more difficult contact race, namely, that occurring when a reverse current fault supplants load current in the tripping direction, it is necessary that the time required for the directional-unit contacts to open be less than the time for the overcurrent-unit contacts to close. This requirement suggests either that the directional unit should be fast even at very low voltage, or that the overcurrent unit should be delayed slightly. Actually, in order to increase the safety factor, both methods are employed. The overcurrent unit is delayed slightly by preventing the immediate rise of torque to its steady-state value. The operating time of the directional relay is decreased, principally at low voltage, by delaying the decay of current in the potential coil, retaining, at the same time, substantially, circuit frequency.

Description of the Relay

The relay consists of an overcurrent unit and a directional unit mounted in a single case. Figure 3 shows the relay, and Figure 4 shows the internal connections. In Figure 3, the upper unit is the overcurrent unit, and the lower unit is the directional unit. Both units are of the eight-pole induction-cylinder construction previously described.[1]

The main windings of the overcurrent unit consist of eight series-connected current coils.

As with all induction devices, the development of torque is dependent upon the difference in time phase between two or more fluxes. (This is not a complete explanation of the torque development of induction devices but is sufficiently explicit for present purposes.) Consequently, four of the current poles, in alternate position, have auxiliary coils. These windings provide a means of controlling or shifting the time phase of the air-gap flux of these poles. The four auxiliary coils, in series, are connected in series with a capacitor and one set of contacts of the directional unit. The torque of the overcurrent unit is proportional to the product of the square of the current and the sine of the angle between the fluxes produced in the shifted and unshifted poles. As long as the contacts of the directional unit are open, the fluxes in all poles are in phase and the torque of the overcurrent unit is zero. When the contacts of the directional unit close, the auxiliary coil circuit of the overcurrent unit is completed. This auxiliary circuit then shifts the phase angle of the flux in the corresponding poles so that it is no longer in phase with the flux in the other poles and the overcurrent unit has a torque in the direction to close its contacts.

The auxiliary circuit is an oscillating circuit, and the build-up of its current is delayed.[2] Since current in the auxiliary winding shifts the flux, a delay of the build-up of this current delays the flux shift. The build-up of current in the auxiliary winding is shown by the trace made by vibrator 1 of the oscillogram shown in Figure 6. This slow build-up

Figure 6. Test oscillogram. Fault in tripping direction

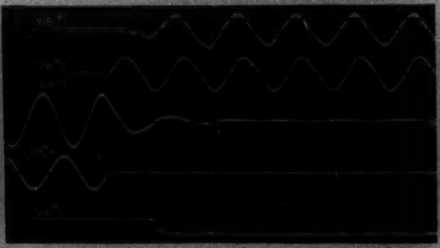
Vibrator 1—Current in flux-shifting coils of overcurrent unit

Vibrator 2—Line current

Vibrator 3—Current in potential coil of directional unit

Vibrator 4—Voltage across potential circuit of directional unit

Vibrator 5—Current in trip circuit



prevents the overcurrent unit from closing its contacts before the directional unit has time to open its contacts.

When the magnitude of the load current is near the pickup value of the overcurrent unit, the impulse that the overcurrent unit receives, under short-circuit conditions, before the contacts of the directional unit can open, will be sufficient to close the contacts of the overcurrent unit. The time required to close the contacts under this condition is greater than the time required to open the contacts of the directional unit, so that the trip circuit is not completed. This has been demonstrated by tests.

The windings of the directional unit consist of four current coils in series and four potential coils in series, mounted on alternate poles. The potential circuit has two capacitors and a resistor in addition to the coils. This potential circuit is also an oscillating circuit so that the flux in the potential coil does not immediately drop when the fault appears. The flux continues to follow system frequency for about one cycle which gives the directional unit ample torque to open its contacts quickly. This slow decay of the potential flux also gives the directional unit enough torque to close its contacts when the fault is in the trip direction even though the voltage across the potential circuit drops to a small value during the fault.

Figure 5 shows an oscillogram taken under the following conditions: Before the fault was applied, the directional relay had full potential. The current was in the direction to close the contacts of the directional unit but below the pickup value of the overcurrent unit. The instant the fault was applied, the current reversed and increased in magnitude to 13 times the pickup value of the overcurrent unit. Concurrently, the voltage across the potential circuit decreased to one per cent of rated potential. Vibrator 3 (Figure 5) shows how the current in the potential coils of the directional unit decayed. Vibrator 1 shows how the current in the auxiliary circuit of the overcurrent unit started to build up but was interrupted by the opening of the contact of the directional unit.

Figure 7. Time–current characteristic

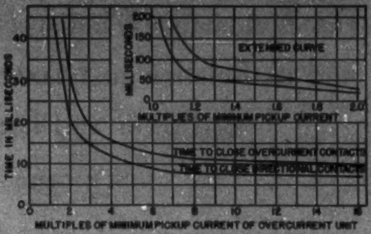


Figure 6 shows an oscillogram in which the conditions were the same as those described heretofore except that the current before the fault was in the nontripping direction and in the trip direction after the fault. This oscillogram shows the build-up of the current of the auxiliary circuit after the directional unit closed (vibrator 1). The oscillogram shows that the contacts of the directional unit closed to complete the auxiliary circuit in approximately one-half cycle (60-cycle basis). Then the overcurrent unit operated to close its contacts in approximately one-half cycle. The trip circuit is therefore completed in approximately one cycle. Since this relay is capable of operating in one cycle, it is believed that it can be called instantaneous, notwithstanding the AIEE definition of "instantaneous," which is "no intentional time delay."

Relay Characteristics

The directional unit of the relay has maximum torque when the relay current leads the relay voltage by 45 degrees. This means that, with the quadrature connection, the relay has maximum torque when the relay current lags the phase-to-neutral voltage of the same phase by 45 degrees. The directional unit will operate correctly at the maximum torque angle with one per cent of rated voltage and the minimum pickup value at which the overcurrent unit can be set.

The time to complete the trip circuit depends upon the direction of the load current before the fault appears. If the load current is in the trip direction, the contacts of the directional unit will be closed and the time to complete the trip circuit is the time to close the contacts of the overcurrent unit. If the load current is in the nontripping direction before the fault, the contacts of the directional unit will be open, and the time required to complete the trip circuit will be the time to close the contacts of the directional unit plus the time to close the contacts of the overcurrent unit. Figure 7 shows the time to close the contacts of the overcurrent unit at various multiples of minimum pickup current. Figure 7 also shows the time to close the contacts of the directional unit when the potential circuit voltage drops to one per cent of rated voltage and the current increases to various multiples of minimum pickup current of the overcurrent unit.

Summary

The relay will operate correctly on parallel lines or on a loop circuit, regardless of whether or not there is a possibility of a reversal of current during a fault or immediately following the interruption of the fault.

The directional unit will operate correctly and quickly when a fault appears even if the potential circuit voltage drops to as low as one per cent of rated voltage.

The overcurrent unit will operate only when the contacts of the directional unit are closed.

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The Automatic Welding-Machine Starter and Its Relation to Maximum Utilization of Power and Facilities

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MANY electric-arc-welding applications are of such a nature that the welding machine is running with no load for a large percentage of the operating time. With this low utilization factor or "arc time," no-load power consumption becomes an appreciable item. In plants employing many machines there will be long leads between the machines and the work, either by necessity or for convenience. Under these conditions, the practicability of expecting the operator, or welder, to turn his machine on and off each time he plans to weld is certainly very remote. The automatic welding-machine starter has been developed to shut down the machine during the normal no-load running periods. Automatic starters have been in operation under average shipyard welding conditions for the past few months. Application of the automatic starters effects a very appreciable saving in power cost, about 25 per cent or more. In addition, a marked reduction in average demand on distribution systems is realized so that additional machines, up to 75 per cent, may be added without further power facilities expenditure.

The Apparatus and Its Operation

The automatic welding-machine starter is an electrical relay-control mechanism which may be connected to nearly any conventional single-operator a-c to d-c motor generator welding set. Operation of the unit is simple. When ready to strike an arc, the welder firmly touches his electrode, or rod, to the article to be welded, or work, and the welding machine starts. The machine continues to run as long as welding is in progress. When welding is finished, and after a predetermined delay period of no-load operation, the machine automatically stops. The cycle may then be repeated as often as is desired.

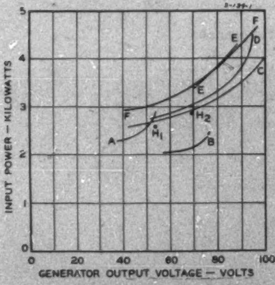

Figure 1. Welding-machine input power at no load

There are many methods of accomplishing the desired result from an electrical control standpoint. The apparatus must include the necessary relay and control components so that, when the electrode is touched to the work piece, that is, return or ground lead, thereby effecting an electrical contact, the machine will start and lock into the running position by the conventional line starter. Of particular interest is the time-delay relay as the no-load delay period directly effects power saving. A two- to three-minute interval is recommended to allow time for the welding machine to cool by ventilation. Moreover, according to welders themselves, two minutes should be allowed for changing rods and cleaning slag from the weld puddle. This prevents an unnecessary stop. Very short delay periods, such as one half a minute, increase the number of starts to a point where wear and tear on contacts is excessive. The automatic starter must be capable of immediately restarting, even at the instant the time-delay unit has called for stopping the welding machine. Any appreciable delay or erratic operation assignable to the electrical operation of the unit will have a detrimental psychological effect on the welder.

The numerous problems encountered in the development of a practical starter are mentioned but not analyzed here. Adaptability of the unit to various types and makes of welding machines, impedance of welding cable on steel plate, various contact resistances, the standstill impedance of welding generators,* and the requirements of various relays and component parts are matters of interest to the designer. A paper discussing these problems with circuit data will be prepared in the near future.

Relation to Power Cost

The no-load input for a group of popular welding machines is given in Figure 1. Nominal ratings are up to 400 amperes. Figure 2 is a plot of no-load power factor readings for the same group. The variation in input and power factor with output setting is expected and is a function of output voltage. Curves *C* and *D* represent machines with a wide range of open-circuit voltage. Less variation would be expected with machines of the more or less constant open-circuit voltage type, see curves *B* and *H*.

Power saving through the application of automatic starters is effected by many variables: the type of machine, type of welding,** output setting of the machine, the time-delay relay setting, and the cost of power.

Various power-saving tests were conducted. In all cases arc time was recorded. Arc time was measured with a synchronous time meter and relay control so that time was counted only when the arc was on. Power was measured with a graphic wattmeter for individual runs and by a watt-hour meter for banks of units. Graphic records provide other valuable data, such as the number of starts, duration of weld runs and off periods, and increase in power consumption with changes in output setting.

For arc times up to 35 per cent, the cumulative time of shutdown was found to vary between 6 and 14 hours on a 24-hour-day basis. The average centered about ten hours. The actual working-day period was checked from time-meter readings on welding machines without starters. This was found to be approxi-

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The author acknowledges the assistance of the following persons and thanks them: H. S. Doolittle, electrical superintendent, Richmond yard one; B. M. Woods, University of California; David Packard, Hewlett Packard Company, and William Fenchuk, Richmond yard one.

* The standstill impedance is the impedance measured across welding generator output terminals with generator at standstill.

** Power saving is effected by the type of welding, that is, intermittency of welding, arc time, duration of weld runs, necessity for long delays because of fit-up, tacking, and so forth.

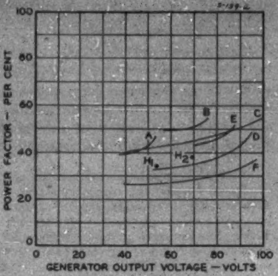


Figure 2. Welding-machine power-factor characteristics at no load

Table I. Power-Consumption Test

Test Number	1	2	3	4	5	6	7	8
Automatic starter connected during test	Yes	No	Yes	No	Yes	No	Yes	No
Test time (hours)	24	24	24	24	24	24	24	24
Power consumed by* four welding machines during test (kilowatt-hours)	277.7	284.1	199.1	378.1	305.0	462.3	315.0	497.5
Total number of arc hours of welding by four machines (hours)	20.99	15.66	19.8	23.96	26.53	26.29	23.8	27.0
Input per arc hour of welding (kilowatt-hours)	13.24	18.14	10.05	15.8	11.5	17.55	13.2	18.5
Average arc time, based on 21-hour work day (per cent)	25	18.6	23.6	28.5	30.4	31.3	28.4	32.1

Average values: Arc time .... 27.3 per cent
Kilowatt-hours per arc hour *without* starters 17.6
Kilowatt-hours per arc hour *with* starters 12.0
Saving in power with starters .... 31.7 per cent
Saving in power (kilowatt-hours per 24 hours per machine) .... 31.8 kilowatt-hours

* Average no-load input 3 kw, average load input 12-kw.

mately 21 hours. The departure from a 24-hour day is accountable to shift-change and noon-hour periods. The product of the no-load losses, shutdown time, and power cost will yield dollar value of the power saving.

Actual test data of a bank of welding machines with and without starters are given in Table I. The criterion for comparison is arc time.† Power readings were taken using a three-phase watt-hour meter.

Statistics and Average Power Input

Experience and logic tell us that a power circuit supplying an intermittent load will handle a much higher connected horsepower than the same circuit under continuous operating connected load. Welding machines present a highly intermittent load.

Applying the statistical theory of sampling, we can show that, with a circuit of four welding machines operating on 25 per cent average arc time, the probability is 0.42 that we will find three machines at no load and one machine welding, the probability is 0.315 that we will find all four machines at no load, and the probability is 0.265 that there will be more than one machine welding.

It must be assumed that load conditions on the four welding machines, at any interval, represent a sample taken from a grand lot of weld (load) and no-weld (no-load) intervals,* the average arc time of which is known to be 25 per cent. Since each welder operates individually and without regard to what the others are doing, there is some justification in assuming a normal distribution of the weld and no-weld intervals. The binomial law is then applied using the formula:

$$P = \frac{n!}{(n-c)!c!} P^{n-c} Q^{c}$$

This is simply the probability of finding *c* defectives in a sample of size *n*, where *n* is the number of welding machines, *c* is the number not welding, *P* is the fraction effective or average arc time (25 per cent effective in grand lot), and $Q = 1 - P$ the average not welding or fraction defective (the average not welding in the grand lot of weld and no-weld intervals).

If we increase the sample size,** *n*, or number of welding machines, it can be seen that factorial calculations become laborious. Moreover, if we wish to calculate the probability that there will be not more than a given number of machines welding, it is necessary to sum up a group of terms. Calculations then make use of the incomplete beta function for which charts have been prepared.[2]

Using charts on the incomplete beta function, we can show that for 100 welding machines operating on a 25 per cent average arc time, the probability is 0.955 (extremely probable) that there will never be more than 36 machines welding. Likewise, we can show that the probability is 0.900 (highly probable) that there will be not more than 30 machines welding for a given interval. This may be expressed differently, that is, for at least 90 per cent of the time there will not be more than 30 machines welding and not less than 70 machines at no load.

The statistical analysis is interesting and gives us a fair approximation of how close we may load a distribution system to the average load limit. As the number of machines is increased, the statistical analysis is more accurate, and average load values may more safely be used.

Relation to Maximum Utilization of Facilities

The relation of automatic starters to maximum utilization of facilities can best be emphasized through a hypothetical example, making use of certain reasonable assumptions. Consider a distribution system operating at capacity and supplying 100 welding machines at an average arc time of 25 per cent.‡ Average no-load input is three kilowatts and 40 per cent power factor. (See Figures 1 and 2.) From test data, the average welding input is 12 kw and 85 per cent power factor. If no automatic starters are employed, the average input will be:

$0.25 \times 100 \times 12$ kw = 300 kw at 85 per cent power factor, 187 rkva, 355 kva
$0.75 \times 100 \times 3$ kw = 225 kw at 40 per cent power factor, 516 rkva, 562 kva
Average input = 525 kw, 877 kva, and 50.8 per cent power factor.

If the machines are operated on a 21-hour work-day period and the installation of automatic starters results with an average shutdown time of ten hours per day per machine. Converting hours to per cent time on a 25 per cent arc time basis, the average input will be:

$0.25 \times 100 \times 12$ kw = 300.0 kw at 85 per cent power factor, 187 rkva, 355 kva
$0.274 \times 100 \times 3$ kw = 82.2 kw at 40 per cent power factor, 188 rkva, 205 kva

Average input is 382.2 kw, 535 kva, and 71.5 per cent power factor. The application of starters effects a decrease 27.2 per

---

† For shipyards, 25 per cent arc time is believed to be a conservative average.

* From graphic instrument charts and impulse counter readings, it is found that the number of impulses, or times that a welding arc is struck, in a 24-hour work day will be in order of 2,000. This may substantiate the concept of weld and no-weld intervals and accordingly, statistical normality.

** It is sometimes convenient to calculate factorial *n* when *n* is not too large. Stirling's formula gives satisfactory results. The formula:

$$n = n^{n} e^{-n} \sqrt{2\pi n}$$

For $n = 10$, the error is only 0.8 per cent.[1]

‡ The machines were welding, on the average, for 25 per cent of the time, or 5.25 hours on a 21-hour work-day basis. They were off, on the average of ten hours, and running at no load on the time-delay relay period for the balance of the time, 5.75 hours, which is 27.4 per cent of the period.

Table II

Item	Method 1 Additional Capacity	Method 2 Power-factor Correction	Method 3 Starters
1. Facilities cost	$ 7,900.00 (a)	$ 8,730.00 (b)	$ 9,600.00 (c)
2. Power cost, per month for 160 machines	3,933.00	3,601.00	2,775.00
3. Original power cost per month for 100 machines	2,460.00	2,460.00	2,460.00
4. Additional power cost per month for operating 60 machines (item 2 minus 3)	1,473.00	1,141.00	315.00
5. Additional power cost for 6 months period	8,850.00	6,846.00	1,890.00
6. Cost of facilities and power for 6 months (item 1 plus 5)	16,750.00	15,576.00	11,490.00
7. Cost per machine per month for 6 months period (not including machine cost)	279.00	260.00	191.00
8. Per cent of unit machine cost @ $450.00 per unit	62 per cent	27.7 per cent	42.5 per cent
9. Continued power saving per month over method 1	None	332.00	1,158.00

(*a*). 550 kva of additional capacity at $15.00 per kilovolt-ampere installed.

(*b*). 873 kva of correction required at $10.00 per kilovolt-ampere installed.

(*c*). 160 starters at $60.00 each installed. No allowance is made for maintenance in method 3. This item must ever be recognized where moving parts, that is, relays, and so forth, are employed. It is believed, however, that the maintenance cost for starters would be at least offset by the decreased maintenance cost on the welding machines themselves, because, on account of the starters, their total running time will be greatly reduced.

cent in average kilowatt demand, 39 per cent in average kilovolt-ampere demand, 46.7 per cent in average reactive-kilovolt-ampere demand, and an increase of 11.7 per cent in average power factor. With power at $0.01 per kilowatt-hour, on a 21-hour work-day period, this amounts to approximately $900 per month. If the installed cost is $60.00 per unit, starters will pay for themselves on power savings alone in less than seven months. The average reactive demand is reduced by 328 reactive kilovolt-amperes. In equivalent capacitance correction, at $10.00 per kilovolt-ampere installed, this represents more than half the first cost of the starters.

In the example, application of automatic starters allows for an increase of 64 per cent in capacity or 64 additional welding machines on the already loaded distribution system. Higher arc times will increase the average input and reduce the additional capacity allowance, and vice versa. Power saving and investment value will depend upon power rates and schedules. The saving will increase where power is sold on some form of kilovolt-ampere-hour basis.

Suppose in this hypothetical case, it was decided to purchase 60 additional welding machines at a cost of $450.00 each. Since the distribution system is operating at capacity, power supply for the new machines becomes a serious problem. There are three solutions to the problem:

1. Installation of additional distribution capacity.
2. Power factor correction of the existing system.
3. Application of automatic welding machine starters.

It may be desirable to write off the expenditure in a short period such as six months.

Using a typical rate, power cost is $0.007 per kilowatt-hour, and the total charge per month is decreased or increased, respectively, by 0.25 per cent for each one per cent average power factor above or below 85 per cent. Estimates, neglecting interest, depreciation, and items common to all methods, are given in Table II.

A continued power saving of $332.00 per month by method 2 and $1,158.00 per month by method 3, over method 1, will be realized. Longer investment periods will naturally place the balance more and more in favor of the automatic welding-machine starters. It is believed that the figures used and shorter period are in keeping with many wartime expansion policies. It is of interest to note that a combination of capacitance correction and starters would allow for more than doubling the capacity of the original loaded system in the example.

Conclusions

The automatic welding-machine starter has been in operation under average shipyard welding conditions for a sufficient length of time to prove its practicability and merits. Application of automatic starters offers two principal advantages:

1. A very recognizable saving in power cost is effected because the welding machine is automatically shut down for the greater part of its normal no-load operating time. Test results show this saving to be in order of 25 to 30 per cent of the total power consumed for average arc times of 25 per cent and with a three-minute delay setting on the automatic starter. The saving will vary with different welding application and will increase as the average arc time is reduced and with shorter time-delay relay settings. Since the no-load input for a-c to d-c motor-generator welding sets is inherently of a low power factor nature, application of automatic starters greatly reduces the average reactive-kilovolt-ampere demand (in the order of 50 per cent reduction), so that the average kilovolt-ampere input is reduced far more than the average kilowatt input. The economic advantage of automatic starters on a purely power cost saving analysis is entirely dependent upon power rate schedules and will be more favorable where power is sold on some form of kilovolt-ampere-hour basis.

2. Probably the most important advantage offered by the application of automatic starters is that, by reducing the average kilowatt and kilovolt-ampere demand, appreciable increase in capacity on existing distribution systems may be realized. The increased capacity allowance will vary from 25 per cent to 75 per cent, depending upon arc time and other conditions, and may be over 100 per cent in cases where starters are used in combination with capacitance correction.

It is believed that the application of starters will warrant consideration where new or additional distribution facilities are contemplated. In general, starters will show a greater economic advantage over any other method of reducing overall power and facilities cost and, in any case, will provide for the maximum utilization of welding power distribution systems.

References

1. HIGHER MATHEMATICS FOR ENGINEERS AND PHYSICISTS (book), Sokolnikoff and Sokolnikoff.

2. THE ENGINEERS' MANUAL OF STATISTICAL METHODS, Leslie E. Simon.

Lightning Surges Transferred From One Circuit to Another Through Transformers

P. L. BELLASCHI
FELLOW AIEE

IT is generally recognized that through the medium of transformers surges can be transferred from circuits exposed to lightning to circuits considered otherwise nonexposed. The latter may connect to rotating machines or to industrial apparatus of inherently low-impulse insulation level. On these seemingly nonexposed circuits may be found apparatus of old design which as a rule are even more vulnerable to surges. In these problems the question is to determine

1. The extent apparatus may be endangered.
2. Whether the relative risk incurred weighed against the importance of the installation and the cost of damage, warrants protective equipment.

In each case sound engineering recommendations demand a fairly close estimate of the transferred surges.

In examining cases of this character in addition to published data, the writer has found particularly helpful the results of an investigation at Sharon in 1932–33. Seasoned by experience, these results and the simplified methods derived from them are presented here to assist in a practical way those engaged in this type of protection studies. This contribution should serve besides to call attention again to the vital importance transferred surges may assume under certain conditions.

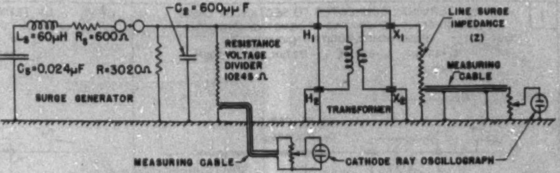
The Nature of the Problem

Contributions on the mechanism governing the transfer of surges through transformers have been reported by various investigators.[1–5] Concurrently, the protection of rotating machines and similar low-impulse-strength apparatus subject to these surges has been discussed.[6–11] Essentially, when a lightning surge is impressed on one winding of a transformer, a surge appears at the terminals of a second winding. The surge thus transferred to other windings and connected circuits consists respectively of an electrostatic component and an electromagnetic component. The electrostatic component results from the capacitance coupling between the windings and to ground (Figure 13), while the electromagnetic component depends on the turn ratio and the short-circuit inductance between the two windings (Figure 15). The terminal conditions naturally contribute also to determine the amplitude, the wave form, and the duration of the surge transferred. Superimposed on these components will be present in lesser or greater amounts the effects due to oscillations from the windings. All these factors will be apparent from the tests and the discussion which follow.

One Series of Tests

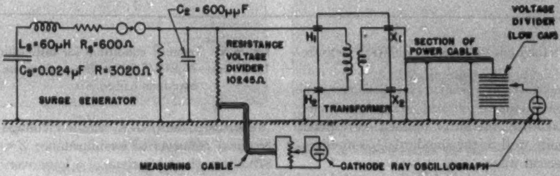
All tests and the corresponding measurements were conducted in the manner illustrated in Figure 1. In this series of tests the transformer is a single-phase two-winding core-form unit, rated at 1,000 kva and 76,210/2,300 volts. The turn ratio is 33.1 to 1. Other characteristics of the transformer are listed in Table I. The terminal arrangements cover a wide range of the practical conditions encountered in service. These are summarized in Figure 2. The voltage applied

Figure 1. Typical arrangements and method of test



A (above). Low-voltage terminal of transformer connected to line

B (below). Low-voltage terminal of transformer connected to section of cable



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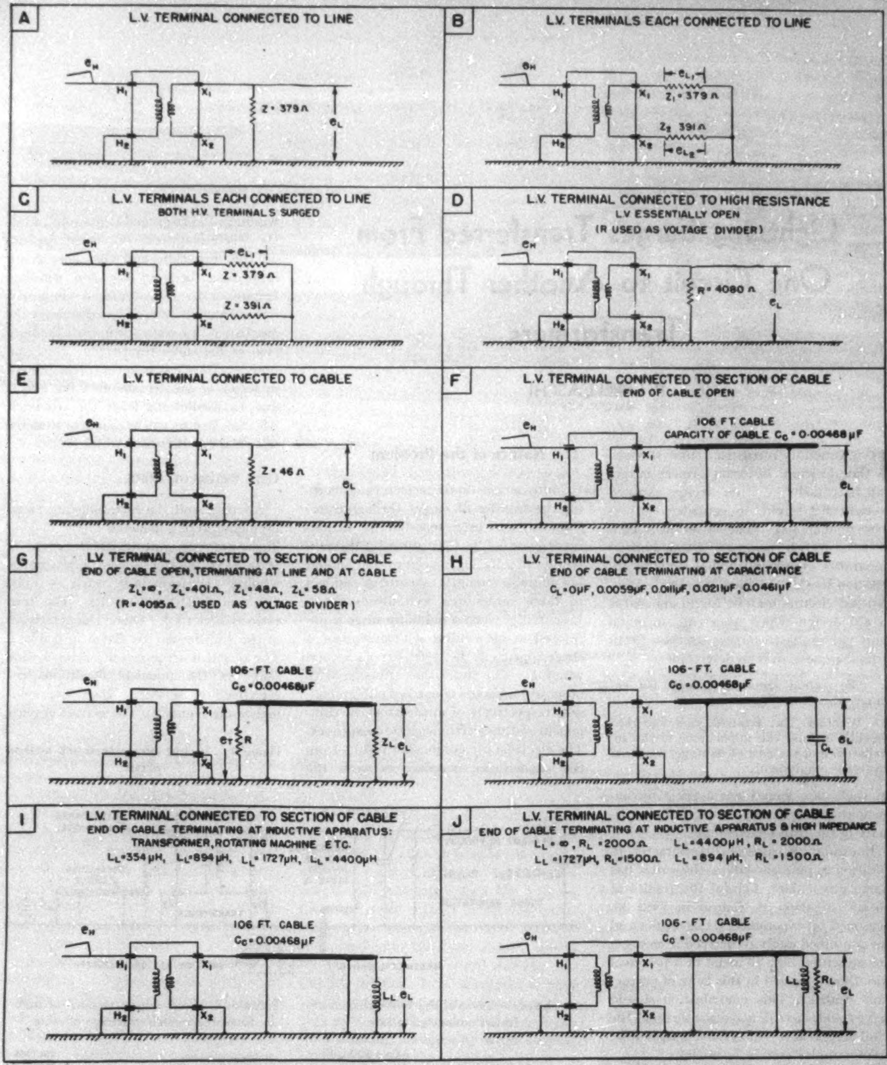

A
L.V. TERMINAL CONNECTED TO LINE
e_H
H_1
H_2
X_1
X_2
Z = 379 Ω
e_L
B
L.V. TERMINALS EACH CONNECTED TO LINE
e_H
H_1
H_2
X_1
X_2
e_{L_1}
Z_1 = 379 Ω
Z_2 391 Ω
e_{L_2}
C
L.V. TERMINALS EACH CONNECTED TO LINE
BOTH H.V. TERMINALS SURGED
e_H
H_1
H_2
X_1
X_2
e_{L_1}
Z = 379 Ω
Z = 391 Ω
D
L.V. TERMINAL CONNECTED TO HIGH RESISTANCE
L.V. ESSENTIALLY OPEN
(R USED AS VOLTAGE DIVIDER)
e_H
H_1
H_2
X_1
X_2
R = 4080 Ω
e_L
E
L.V. TERMINAL CONNECTED TO CABLE
e_H
H_1
H_2
X_1
X_2
Z = 46 Ω
e_L
F
L.V. TERMINAL CONNECTED TO SECTION OF CABLE
END OF CABLE OPEN
106 FT. CABLE
CAPACITY OF CABLE C_C = 0.00468 μF
e_H
H_1
H_2
X_1
X_2
e_L
G
L.V. TERMINAL CONNECTED TO SECTION OF CABLE
END OF CABLE OPEN, TERMINATING AT LINE AND AT CABLE
$Z_L = \infty$, Z_L = 401 Ω, Z_L = 48 Ω, Z_L = 58 Ω
(R = 4095 Ω, USED AS VOLTAGE DIVIDER)
106-FT. CABLE
C_C = 0.00468 μF
e_H
H_1
H_2
X_1
X_2
e_T
R
Z_L
e_L
H
L.V. TERMINAL CONNECTED TO SECTION OF CABLE
END OF CABLE TERMINATING AT CAPACITANCE
C_L = 0 μF, 0.0059 μF, 0.0111 μF, 0.0211 μF, 0.0461 μF
106-FT. CABLE
C_C = 0.00468 μF
e_H
H_1
H_2
X_1
X_2
C_L
e_L
I
L.V. TERMINAL CONNECTED TO SECTION OF CABLE
END OF CABLE TERMINATING AT INDUCTIVE APPARATUS:
TRANSFORMER, ROTATING MACHINE ETC.
L_L = 354 μH, L_L = 894 μH, L_L = 1727 μH, L_L = 4400 μH
106 FT. CABLE
C_C = 0.00468 μF
e_H
H_1
H_2
X_1
X_2
L_L
e_L
J
L.V. TERMINAL CONNECTED TO SECTION OF CABLE
END OF CABLE TERMINATING AT INDUCTIVE APPARATUS & HIGH IMPEDANCE
$L_L = \infty$, R_L = 2000 Ω L_L = 4400 μH, R_L = 2000 Ω
L_L = 1727 μH, R_L = 1500 Ω L_L = 894 μH, R_L = 1500 Ω
106-FT CABLE
C_C = 0.00468 μF
e_H
H_1
H_2
X_1
X_2
L_L
R_L
e_L

Figure 2. Terminal conditions for tests with 1,000-kva 76,210/2,300-volt single-phase core-form transformer

to the high-voltage winding (e_H) measures 364-kv crest, and in this too the tests have the significance of approaching the conditions of interest in actual service. Likewise, the surge applied corresponds quite well to the standard $1^1/_2$x40-microsecond wave.

For test condition *A*, one low-voltage terminal connects to an impedance $Z=$ 379 ohms, the equivalent of a long overhead line. The other terminal goes directly to ground. The surge voltage e_L transferred to the low-voltage terminal and to the line is recorded in Figure 3. In effect it rises to crest in eight microseconds, and recedes on the tail in about the same time as the wave applied. The

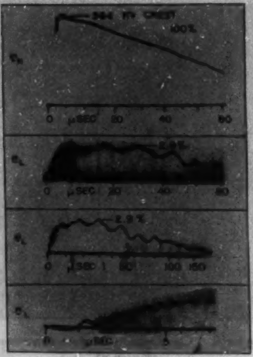


Figure 3. Oscillograms for test condition *A* (Figure 2)

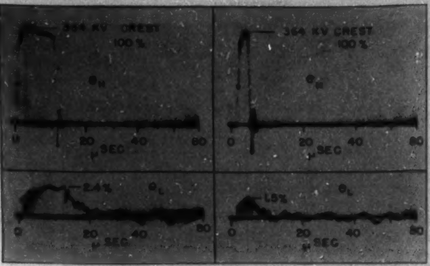


Figure 4. Oscillograms for test condition *A* (Figure 2)

electrostatic component accounts in part for the front. Beyond crest the electromagnetic component plus the superimposed oscillations predominate. The crest value 2.9 per cent corresponds essentially to turn ratio (100 per cent divided by 33.1). Test *A* was repeated with chopped waves applied. From Figure 4, as it should be expected, the surge e_L transferred to the line practically disappears for increasingly shorter impulses e_H.

In test *B*, each of the low-voltage terminals connects to a line impedance. In this case, the electromagnetic component divides about equally between the two lines, as in e_{L_1} and e_{L_2} of Figure 5. Note also the opposite polarity with respect to ground of the two. Only on the low-voltage terminal, physically opposite H_1, does the electrostatic component occur. In test *C*, surge e_H is applied simultaneously to both high-voltage terminals, so that in this case the electromagnetic component in e_L (Figure 5), disappears. Here the electrostatic component (1.3 per cent) is followed by the natural oscillations set up in the winding. In test *D*, the high resistance practically amounts to an open low-voltage terminal. Under this condition, because of the full development of the electrostatic effect and similarly because of the rapid development of the electromagnetic effect, the surge e_L (Figure 5) rises more abruptly and reaches a higher crest (3.7 per cent) than in any of the previous tests. The bottom oscillogram e_L of Figure 5 shows the influence of a long cable. This condition corresponds to *E*. As the result of the low impedance of the cable, the electrostatic effect practically has vanished insofar as appearing at the terminal. The surge transferred to the cable rises to crest in a much longer time than for the overhead line because of the greater time constant L_S/Z, attaining 2.1 per cent crest as against 2.9 per cent for the line.

In service transformer terminals may connect to a section of cable which, under certain conditions, may remain open in the manner of test *F*. In this case, as recorded in Figure 6, the cable voltage oscillates, reaching 5.2 per cent, which is nearly twice the voltage e_H divided by the turn ratio. The oscillations are largely between the short-circuit inductance (L_S) and the cable capacitance (C_C), though in part other winding effects enter into play. A chopped wave e_H results in a transferred surge of the character shown.

Under other service conditions, the section of cable may connect directly to an overhead line or continue with another cable for a great extension. The voltages developed under these conditions (test *G*) are shown in Figure 7. For either the long line or extensive cables the section of cable is not a major influencing factor. The oscillograms show that the voltages at the two ends of the cable section are much the same.

In test *H*, capacitances were connected to the open end of the cable. The capacitances applied in effect are equivalent to extending the 106-foot cable respectively to 240, 360, 580, and 1,120 feet. The results are recorded in Figure 8. The period of the transferred surge naturally increases with the capacitance, but the amplitude remains practically unchanged at 5.1 per cent.

Another practical case occurs where the transformer supplies inductive apparatus such as other transformers or rotating machines. This condition is approached in test *I*. On the basis that the short-circuit inductance (L_S) of the transformer is 100 per cent, the inductances L_L applied are respectively 26, 66, 128, and 325 per cent which values correspond to a wide range of effective loads. The oscillograms of Figure 9 show that both the amplitude and the period of the trans-

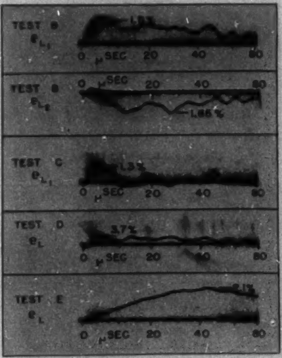


Figure 5. Oscillograms for test conditions *B*, *C*, *D*, and *E* (Figure 2)

Applied voltage e_H as in Figure 3

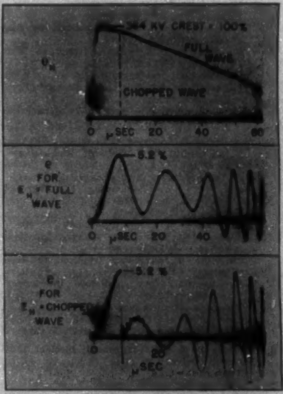


Figure 6. Oscillograms for test condition *F* (Figure 2)

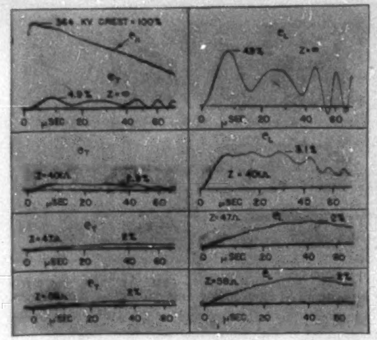


Figure 7. Oscillograms for test condition G (Figure 2)

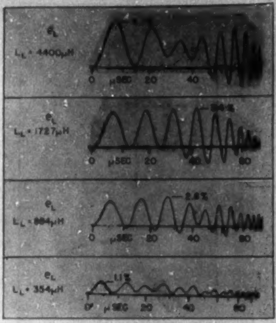


Figure 9. Oscillograms for test condition *I* (Figure 2)

Applied voltage e_H as in Figure 3

ferred surge decrease with decreasing inductance load.

Another set of load conditions is the combination of high surge impedances (resistances) and inductances in *J*. The corresponding transferred surges are recorded in Figure 10. These tests complete the investigation on the 1,000-kva transformer.

Other Tests and Experiences

In Figure 11 are recorded the voltages transferred through a single-phase 4,500-kva 144,000/12,000-volt, shell-form transformer. The turn ratio is 12 to 1. Other characteristics of this transformer are listed in Table I. In these tests the low-voltage winding in effect connects to a long overhead line, while the impulse voltages applied to the high-voltage winding reach practically the full test level of the insulation. The electrostatic component is clearly in evidence for the first ten microseconds; beyond this time the electromagnetic component with some superimposed oscillations of minor import is the dominating factor. In the chopped-wave test, the electromagnetic component practically disappears. The tests were repeated with a 100-foot section of cable connected to the low voltage as in *B* of Figure 1. In these tests the crest voltage developed was sufficient to flash over the cable at the end.

Table I. Design Characteristics of Transformers Used in Tests

1. Core-form, oil-immersed, self-cooled, 1,000-kva, 76,210/2,300-volt, single-phase, 60-cycle
    - Number of turns: high-voltage, 3,576 turns
      low-voltage, 108 turns
    - Turn ratio = 33.1
    - Impedance = 9.62 per cent
    - Short-circuit inductance (referred to low voltage) = 0.00135 henry
    - Short-circuit inductance (referred to high voltage) = 1.48 henrys
    - Construction, two-legged design
    - High-voltage winding, pancake coils, one column each leg
    - Low-voltage winding, two layers on each leg adjacent to core
2. Shell-form, oil-immersed, self-cooled, 4,500-kva, 144,000-120,000/12,000-volt, single-phase, 25-cycle
    - Number of turns: high-voltage, 1,582 turns
      low-voltage, 132–158 turns (taps)
    - Turn ratio (in tests) = 12
    - Impedance = 4 per cent
    - Short-circuit inductance (referred to low voltage) = 0.008 henry
    - Short-circuit inductance (referred to high voltage) = 1.15 henrys
    - Construction, four high–low interleaved (see Figure 13A)
    - Two high-voltage groups each 791 turns
    - Low-voltage center group 62–88 turns (taps)
    - Two low-voltage end groups each 35 turns
3. Shell-form model, oil-immersed, special
    - No rating assigned
    - Turn ratio = 6.14
    - Construction (see Figure 12)
    - High-voltage coil group 2,180 turns
    - Low-voltage coil *A* 18 turns
    - Low-voltage coil *B* 337 turns

In the course of the many commercial impulse tests applied to transformers at Sharon, the effects of surges transferred through windings have been much in evidence. Bushings of windings not under test often require protection from flashover. In this connection, it is well to point out that unusually high surge voltages can be transferred to high-voltage terminals when low-voltage windings of high insulation level are being tested. For instance, a 110-kv full-wave surge applied to the low-voltage windings of a 6,600/132,000-volt power transformer (ratio 20 to 1) is certain to flash over the high-voltage bushing.

The experience from commercial im-

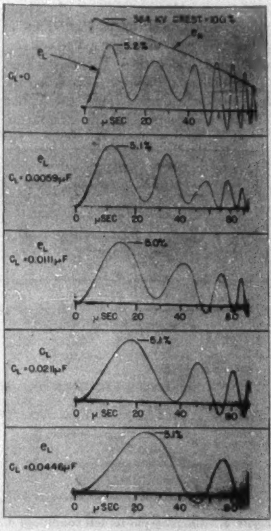


Figure 8. Oscillograms for test condition *H* (Figure 2)

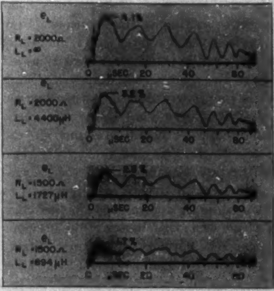


Figure 10. Oscillograms for test condition *J* (Figure 2)

Applied voltage e_H as in Figure 3

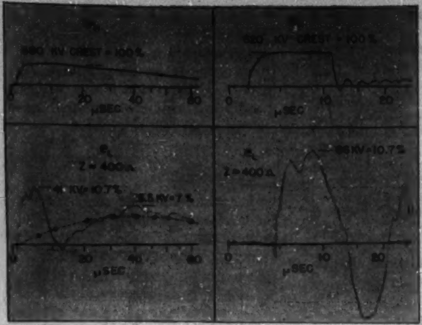


Figure 11. Oscillograms for test condition *A* (Figure 1) with 4,500-kva 144,000/12,000-volt single-phase shell-form transformer

Circled dots are calculated values of electromagnetic component

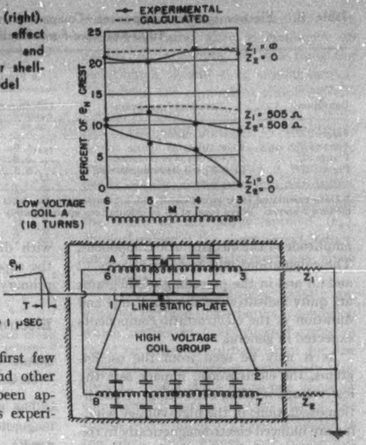


Figure 12 (right). Electrostatic effect—calculated and test data for shell-form model

pulse tests at Sharon bears out also that surges may be transferred through a number of transformers in succession. In the commercial tests, power excitation is supplied from a 5,000-kva, 2,300-volt generator of old design through a 2,000-kva 2,300/6,900-volt transformer, a 1,000-foot cable, and again through another 2,000-kva transformer which serves to adjust the 6,900-volt cable supply to the primary voltage of the transformer under impulse test. Depending on the nature of the commercial impulse test, transferred surges of various intensities would reach the rotating machine. These seriously damaged the machine windings on three occasions in the course of the first few years. Suitable capacitances and other protective devices have since been applied and no further difficulties experienced.

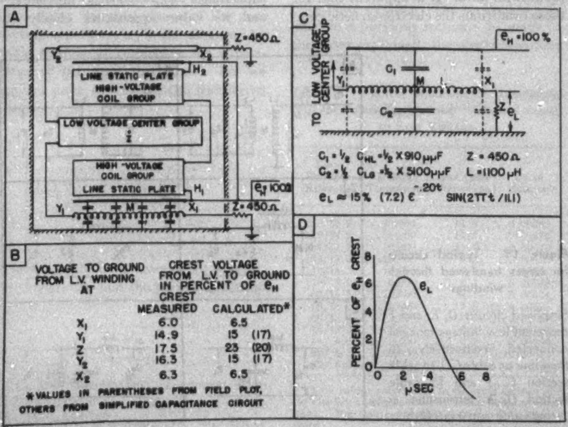
Electrostatic Component and Oscillations

The factors which affect the electrostatic component are several. These are discussed here in simple practical terms with reference to the typical examples in Figures 12 and 13. The electrostatic voltage induced in the low-voltage winding depends upon the capacitance relation between the high and low-voltage windings and to ground. In turn, the voltage induced in the low-voltage winding is discharged through the inductance of part of the winding into the terminal impedance. Thus from the simplified circuit in *C* of Figure 13 for the abrupt surge e_H,

$$e_L \approx \frac{C_1}{C_1+C_2}\cdot\frac{Z\epsilon^{-\frac{Z}{2L}t}\sin t\sqrt{\dfrac{1}{LC_2}-\dfrac{Z^2}{4L^2}}}{\sqrt{\dfrac{L}{C_2}-\dfrac{Z^2}{4}}}e_H \tag{1}$$

The calculated values from this simplified approach are in fairly good agreement with the test results. A more rigorous analysis requires that account be taken of the additional circuit elements shown in Figures 12 and 13 but in so doing the solution becomes quite complex.

Simply stated, the electrostatic component depends on the ratio, Z divided by $\sqrt{L/C_2}$, and on the length of the front of surge e_H with respect to the natural period $2\pi\sqrt{LC_2}$. In the case of a long cable where Z is small or for a segment of cable of large capacitance, the electrostatic component disappears. The electrostatic component needs be considered only where the low-voltage winding connects to aerial lines, to highly inductive apparatus or machinery, to loads of effectively high impedance, and where the capacitance to ground is small. The physical characteristics of the transformer windings are a factor, but these do not bear any simple relation in terms of the usual design constants. The ratio of the insulation test levels of the windings is a measure of the factor $C_1/(C_1+C_2)$ and, other things being equal, of the relative



$C_1 = \frac{1}{2}\ C_{HL} = \frac{1}{2} \times 910\ \mu\mu F$ $Z = 450\ \Omega$

$C_2 = \frac{1}{2}\ C_{LG} = \frac{1}{2} \times 5100\ \mu\mu F$ $L = 1100\ \mu H$

$e_L \approx 15\%\ (7.2)\ \epsilon^{-.20t}\ \mathrm{SIN}(2\pi t/11.1)$

VOLTAGE TO GROUND FROM L.V. WINDING AT	CREST VOLTAGE FROM L.V. TO GROUND IN PERCENT OF e_H CREST MEASURED	CALCULATED*
X_1	6.0	6.5
Y_1	14.9	15 (17)
Z	17.5	23 (20)
Y_2	16.3	15 (17)
X_2	6.3	6.5

*VALUES IN PARENTHESES FROM FIELD PLOT, OTHERS FROM SIMPLIFIED CAPACITANCE CIRCUIT

Figure 13. Electrostatic effect—calculated and test data for 4,500-kva shell-form transformer

Table II. Electromagnetic Component—Comparison of Calculated Data With Tests for 1000-Kva Core-Form Transformer

Test Condition	Terminal Impedance	Crest Voltage e_L in Per Cent e_H Crest		Wave Form
		Calculated*	Experimental	
Figure 2A	$Z_L=379$ ohms	2.6	2.9	See Figure 14 for comparison of calculated* and recorded wave form
Figure 2E	$Z_L=46$ ohms	1.6	2.1	
Figure 2F	$C_C=0.00468$ microfarad	5.8	5.2	
Figure 2H	$C_C+C_L=0.0493$ microfarad	5.2	5.1	

* Data calculated from equations 2 and 3, transformer constants in Table I, and terminal impedances in column 2 above. Wave form of e_H in calculations approximately as in tests.

amplitude of the electrostatic component. The calculations and tests in Figures 12 and 13 and in the preceding oscillograms are quite indicative of the amplitude and duration of the electrostatic components expected in general.

As it may be seen from the oscillograms, the electrostatic impact sets the low-voltage winding oscillating. In addition oscillations in the high-voltage winding are induced electromagnetically in the low-voltage winding, and these in turn appear in the transferred surge. Simplified calculations of these effects are beset with difficulties. In general, however, the superimposed oscillations are not of primary importance.

Table III. Comparison of Period of Oscillation for Various Cable Capacitances

Short-Circuit Inductance (L_S in Microhenrys)	Cable Capacitance (C_O in Microfarads)	Period of Oscillation	
		Calculated ($T=2\pi\sqrt{L_SC_O}$) (Microseconds)	Experimental (Figure 8) (Microseconds)
1,350	0.00468	15.8	16
1,350	0.0106	24.8	23
1,350	0.0158	29.0	28
1,350	0.0258	37.0	38
1,350	0.0493	51.3	52

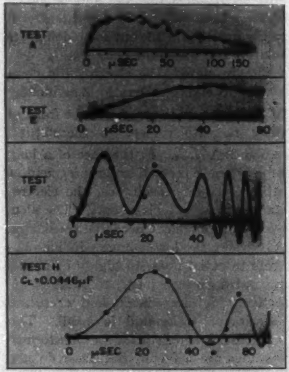


Figure 14. Electromagnetic effect—calculated (circled dots) and test waves of e_L for 1,000-kva core-form transformer

Test conditions *A*, *E*, *F*, and *H* (Figure 2)

Electromagnetic Effect

SURGE TRANSFERRED TO A LONG LINE OR CABLE

The general solution for the electromagnetic component is treated in the appendix. From a practical standpoint, refer to the equivalent simplified circuit of Figure 15D and let

$e_H=E\epsilon^{-\alpha t}$ = impulse voltage applied to high-voltage winding
L_S = short-circuit inductance of transformer (referred to low-voltage side)
N = turn ratio
Z = surge impedance of line or cable

Then the electromagnetic component transferred to the line or cable is

$$e_L=\frac{Z}{Z-\alpha L_S}\left[\epsilon^{-\alpha t}-\epsilon^{-\frac{Z}{L_S}t}\right]\frac{E}{N} \qquad (2)$$

In Figures 11 and 14 and in Table II are compared the calculations for three typical cases with the corresponding experimental data. It is apparent that for these conditions the electromagnetic component is conducive to fairly good predetermination. The preceding equation can be adapted readily to similar conditions such as those shown in tests *B*, *C*, and *D* of Figure 2. The behavior for shorter lengths of cable and line is considered in the following section.

SURGE TRANSFERRED TO SECTION OF CABLE (OR LINE)

For this condition the simplified circuit of Figure 15E applies. Let the applied voltage $e_H=E\epsilon^{-\alpha t}$, then

$$e_L=\left[\frac{\epsilon^{-\alpha t}}{1+\left(\frac{\alpha}{\omega}\right)^2}+\frac{\sin\left(\omega t-\tan^{-1}\frac{\omega}{\alpha}\right)}{\sqrt{1+\left(\frac{\alpha}{\omega}\right)^2}}\right]\frac{E}{N} \qquad (3)$$

where $\omega=1/\sqrt{L_SC_C}$ and C_C = capacitance of cable section.

On the whole, the comparison between the calculated and experimental data in Figure 14 and in Table II for the 106-foot section of cable is good. Equation 3, however, does not take into account the attenuation due to losses, nor does it consider the capacitance of the windings and other effects which apparently are responsible for the "beat-note" phenomena present in the oscillations.

As shown in the oscillograms of Figure 7, the cable section terminating in a lumped impedance equal to the surge impedance of the cable behaves essentially as a continuous long cable. In the case where the cable section connects to a long overhead line, the cable voltage no longer is oscillatory. The reason for this lies in the fact that the line surge impedance ($Z=401$ ohms) appears in Figure 15E as a circuit element shunting the cable capacitance ($C_C=0.00468$ microfarad), and its value approaches closely the

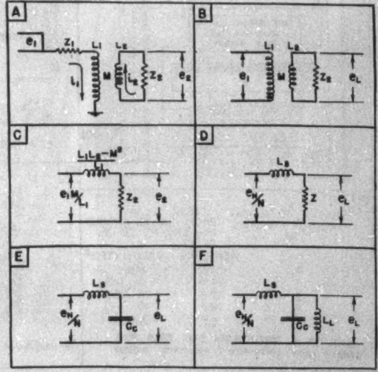


Figure 15. Typical circuits for surges transferred through windings

Simplified circuits *D*, *E*, and *F* represent low-voltage terminal connected, respectively, to long line or cable (*Z*), to cable section (C_C) and to cable section (C_C) terminating at induction apparatus (L_L)

critical damping impedance $^1/_2\sqrt{L_S/C_C}=$ 270 ohms.

In test *H* of Figure 2, the load capacitances amount in effect to extending the cable section. The surge transferred to these sections is given by equation 3 where the cable capacitance in the equation now becomes $C_O=C_C+C_L$. In Table III are compared the calculated and experimental periods of oscillation for the various cable capacitances.

In Figure 14 and Table II are compared the calculated and experimental data of the transferred voltage for $C_L=0.0446$ microfarad. This load capacitance is equivalent to extending the cable to 1,120 feet. In an actual section of this length, the cable would have an inductance less than ten per cent of L_S. For a much longer section, it is apparent that, besides the capacitance, the inductance of the cable need be considered. That is, in Figure 15E for segments of cable where the total inductance $L_C=Z^2C_C$ (constants of cable) is small relative to the transformer short-circuit inductance L_S, the cable may be treated as a lumped capacitance. For longer sections where the cable inductance becomes appreciable but is not excessively large compared to L_S, the cable may be replaced with good approximation by its equivalent π circuit. For still longer sections of cable (or line), transferred surges should be dealt with in terms of traveling-wave phenomena.

Surge Transferred to Section of Cable and Terminating Inductive Apparatus

For this condition the simplified circuit is given in Figure 15F. The capacitance C_C includes the capacitance of the connecting apparatus, or, in the absence of a cable section, it represents the capacitance of the apparatus alone. For an applied surge $e_H=E\epsilon^{-\alpha t}$, the transferred surge is

$$e_L=\left[\frac{\epsilon^{-\alpha t}}{1+\left(\frac{\alpha}{\omega}\right)^2}+\frac{\sin\left(\omega t-\tan^{-1}\frac{\omega}{\alpha}\right)}{\sqrt{1+\left(\frac{\alpha}{\omega}\right)^2}}\right]\times\frac{L_L}{L_S+L_L}\cdot\frac{E}{N}\qquad(4)$$

where L_L is the inductance of the connecting apparatus, and the period of oscillation is

$$T=\frac{2\pi}{\omega}=2\pi\sqrt{C_C\frac{L_SL_L}{L_S+L_L}}$$

In the investigation, L_L varies from approximately 25 to 325 per cent of the short-circuit inductance of the transformer (L_S), and therefore these values comprehend a wide range of inductances of the connecting apparatus (transformers, rotating machines, and so forth). In Table IV are compared the calculated and experimental data of the transferred surges appearing at the connecting apparatus. The good agreement again confirms the applicability of the simplified methods for practical calculations.

Table IV. Comparison of Calculated Data With Tests—Cable Section and Inductive Apparatus Connected to Secondary of 1,000-Kva Core-Form Transformer

Test Condition	Terminal Inductance (Henry)	Period of Oscillation (Microseconds)		Crest Voltage e_L in Per Cent e_H Crest	
		Calculated*	Experimental	Calculated*	Experimental
Figure 2I	$L_L=0.000354$	7.2	8.	1.2	1.1
Figure 2I	$L_L=0.000894$	10.	11.	2.4	2.6
Figure 2I	$L_L=0.001727$	12.	12.5	3.4	3.4
Figure 2I	$L_L=0.004400$	14.	14.	4.6	4.1

* Calculated data based on equation 4, and circuit constants in Table I, Figure 2I, and column 2 above. Crest voltage was determined from equation 4 simplified to $(2E/N)\cdot(L_L/L\ +L_L)$.

Application Problems

The problems encountered in practice are of a varied character, but in general they can be reduced to one of the several conditions in Figure 2 or to modifications and combinations of these. The electrostatic component need be considered only when terminals are left open or connected to an overhead line or other high-impedance circuits. The method is outlined in a previous section. In many of the cases examined, the electromagnetic component alone required attention. The transferred surge then is conducive to calculation according to the foregoing equations 2, 3, and 4, or similar derivations from the equivalent simplified circuit applicable to the problem (Figure 15). The crest voltage of the transferred surge is related to the turn-ratio relation, e_H/N, by a factor *A* which depends largely on the terminal conditions, the short-circuit inductance (L_S), and the wave of the surge applied (e_H). Typical values of the short-circuit inductance for power transformers are given in Table V.

Table V. Typical Short-Circuit Inductances (L_S) for Power Transformers Values Referred to Low Voltage

Line Voltages (Kilovolts)	Kva Per Transformer	Per Cent Impedance	L_S in Henrys*
220/13.2	20,000	14.8	0.00342
220/13.2	10,000	12.5	0.00578
154/13.2	10,000	11.7	0.00540
132/13.2	10,000	9.2	0.00425
110/13.2	10,000	8.7	0.00402
110/13.2	5,000	8.1	0.00748
66/13.2	10,000	8.1	0.00374
66/13.2	5,000	7.5	0.00692
66/13.2	2,500	7.0	0.01292
44/13.2	10,000	6.2	0.00286
44/ 6.6	2,500	6.2	0.00286
22/ 6.6	10,000	6.2	0.00072
22/ 6.6	2,500	6.2	0.00286

* These values correspond to 60-cycle ratings and for the low-voltage connected delta in three-phase bank. For wye connection reduce values to one third.

In practice, transformers usually are installed in three-phase banks, a condition which results in a transferred surge of equal or lower amplitude than for the single-phase connection previously discussed. For a surge e_H on one phase or simultaneous surges on two phases, the transferred voltage to ground is related to the single-phase transformer connection by the conversion factor K_A in Table VI for the various three-phase bank connections. Correspondingly, the duration of the front of the transferred surge is affected by the factor K_F indicated in the table. For simultaneous surges on all three phases, the electromagnetic component disappears (except for the wye-wye bank connection with both neutrals grounded). Thus the crest voltage of the transferred surge is given by the expression

$$e_L\,(\text{electromagnetic})=\frac{e_H}{N}\cdot A\cdot K_A\qquad(5)$$

A typical example will serve to illustrate the application of equation 5. A wood-pole line connects to a 115,000/13,200-volt, grounded-neutral wye–delta transformer bank. The secondary supplies, through a 125-foot cable, an industrial network which consists of a number of transformer units each feeding individual loads. On a single-phase basis, the circuit corresponds in effect to Figure 15F. In this case, the effective load inductance L_L and the short-circuit inductance L_S are of the same order so that the amplitude factor *A* in equation 4 is close to unity. The conversion factor for the three-phase bank is $K_A=0.33$. The advantage of arrester protection on the high side in limiting the transferred voltage is apparent. In the event of a severe lightning surge on the wood-pole line, a full-rated high-voltage arrester limits e_H to 450 kv or to a proportionally lower value, provided an 80 per cent arrester is permissible. Then from equation 5 the transferred voltage is (450/5)1x0.33 = 30 kv. In the absence of arrester protection on the high side, and should the industrial load become disconnected, the transferred surge across the secondary terminals and the cable may well reach 100 kv.

Similar problems arise in connection with rotating machines. These often supply transformer banks which connect on the high side to overhead lines exposed to lightning. The protection of rotating machines against surges requires that the voltage to ground at the machine should be limited to approximately the low-frequency test voltage of the machine, and, in order to limit the voltage between turns, the front of the surge appearing at the machine should as a rule exceed ten microseconds. The protection of machines is discussed in detail elsewhere.[8,9] The methods presented here are helpful in determining the transferred surge that may appear at the machine. In general, the transformer bank in conjunction with lightning arresters on the high side, as well as cable sections in the low-voltage circuit, usually provides substantial protection to machines. However, each case must be examined individually and on its merits. In important installations or for machines of older design located in vital zones, the addition of special protection at the machines may be justified.

In the problems on transferred surges that have been examined: in some, no protection was found necessary on the nonexposed circuit; in others, adequate protection on the exposed circuit also would provide protection to the nonexposed circuit; in some instances, the addition of protection on the nonexposed circuit appeared desirable. An analysis of the problem in each case provided the basis for sound engineering recommendations.

Appendix

Referring to Figure 15A where rectangular surge e_1 is applied to the primary winding over a transmission line and the secondary connects to a line, the transferred surge e_2 is derived from the circuit equations (expressed operationally)

$$e_1 = L_1 p i_1 + Z_1 i_1 - M p i_2$$
$$0 = L_2 p i_2 + Z_2 i_2 - M p i_1$$

The solution is

$$e_2 = \frac{2MZ_2}{\sqrt{(L_1Z_2 - L_2Z_1)^2 + 4Z_1Z_2M^2}} \times \epsilon^{-\frac{L_1Z_2 + L_2Z_1}{2(L_1L_2 - M^2)}t} \times \sinh \frac{\sqrt{(L_1Z_2 - L_2Z_1)^2 + 4Z_1Z_2M^2}}{2(L_1L_2 - M^2)} t \cdot e_1 \quad (6)$$

In the usual case $L_1 \gg M \gg L_2$, so that, in effect, equation 6 reduces to

$$e_2 = \frac{M}{L_1}\left[1 - \epsilon^{-\frac{L_1Z_2}{L_1L_2 - M^2}t}\right] e_1 \quad (7)$$

Table VI. Factors Applied to e_L for Conversion From Single-Phase to Three-Phase Connections*

Bank connections high-voltage to low-voltage winding	Grounded wye to grounded wye Delta to grounded wye Delta to wye	Grounded wye to wye Wye to grounded wye Wye to wye	Delta to delta	Wye to delta Grounded wye to delta
K_A (amplitude)	1.00	0.67	0.67	0.33
K_F (front length)	1.00	1.00	0.33	0.33

* For derivation, see paper by Palueff and Hagenguth, reference 1; also see appendix of this paper. The conversion factors correspond here to turn ratio. Application is discussed in equation 5.

That is, in the usual practical problem Z_1 is of secondary or no importance because of the relatively high value of L_1. Circuit *A* simplifies to *B* or its equivalent *C* which, in terms of the turn ratio (*N*) and short-circuit inductance of the transformer (L_S), becomes circuit *D*. For a steep-front, exponential-tail surge e_H, the solution of e_L is equation 2.

Should the secondary in Figure 15A connect to a cable section of capacitance C_2, from the circuit

$$e_2 = \frac{M}{(L_1L_2 - M^2)C_2} \times \frac{p}{p^3 + \frac{L_2Z_1}{L_1L_2 - M^2}p^2 + \frac{L_1}{(L_1L_2 - M^2)C_2}p + \frac{Z_1}{(L_1L_2 - M^2)C_2}} \cdot e_1$$

$$e_2 = \frac{M}{(L_1L_2 - M^2)C_2} \cdot \frac{p}{(p+\lambda)[(p+\beta)^2 + \omega^2]} \cdot e_1$$

and the solution is

$$e_2 = \frac{\frac{M}{(L_1L_2 - M^2)C_2}}{(\lambda - \beta)^2 + \omega^2} \times [\epsilon^{-\lambda t} + (\lambda - \beta)\epsilon^{-\beta t} \sin \omega t - \epsilon^{-\beta t} \cos \omega t] e_1 \quad (8)$$

For the common case $L_1 \gg M \gg L_2$, the circuit simplifies to Figure 15E. For a steep-front exponential-tail surge e_H, e_L is given by equation 3. Similarly, for a cable section and inductive apparatus, we have Figure 15F and equation 4. The transferred surge e_L for other terminal conditions can be derived in like manner.

For three-phase bank connections the solution of the transferred surge is the same form as for the single-phase, except that the conversion factors K_A and K_F appear, respectively, in the amplitude and time constant of the equations. Consider for instance the wye–wye connection (see Table VI), a rectangular surge e_1 applied to one terminal, and for the practical case considered here neglect Z_1. Current i_1 flows in the phase to which e_1 is applied and $i_1/2$ in the other two phases, all in the same direction. In the corresponding secondary windings the currents are i_2, $i_2/2$ and $i_2/2$. We then have the circuit equations

$$e_1 = \frac{3}{2}L_1 p i_1 - \frac{3}{2} M p i_2$$

$$0 = \frac{3}{2} L_2 p i_2 - \frac{3}{2} M p i_1 + \frac{3}{2} Z_2 i_2$$

The maximum voltage to ground is

$$e_2 = i_2 Z_2 = \frac{2}{3} \cdot \frac{M}{L_1}\left[1 - \epsilon^{-\frac{L_1Z_2}{L_1L_2 - M^2}t}\right] e_1 \quad (9)$$

Thus for this connection $K_A = 2/3$ and $K_F = 1$.

For the wye–delta connection the circuit equations are

$$e_1 = 3/2 L_1 p i_1 - 3/2 M p i_2$$
$$0 = L_2 p i_2 - M p i_1 + 3 Z_2 i_2$$

and

$$e_2 = \frac{1}{3} \cdot \frac{M}{L_1}\left[1 - \epsilon^{-\frac{3L_1Z_2}{L_1L_2 - M^2}t}\right] e_1 \quad (10)$$

so that, $K_A = 1/3$ and $K_F = 1/3$.

The conversion factors for other three-phase bank connections can be derived in a similar manner or by methods described elsewhere.[1] Their values for the various connections are given in Table VI.

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Effect of Wave Form on Let-Go Currents

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Synopsis: This paper is a continuation of "Effect of Frequency on Let-Go Currents"[2] and concludes studies made to determine let-go currents. This paper extends the analysis to cover various wave shapes and includes a method of determining reasonably safe currents for men, women, and children, for sine waves, direct current, and complex wave forms containing both a-c and d-c components. As in previous papers, conclusions regarding reasonably safe electric currents are based on the criterion that a safe current is the let-go current for $99^1/_2$ per cent of a large group of healthy subjects.

ALTHOUGH it has been demonstrated that let-go currents, and hence the electric-shock hazard due to relatively small electric currents, are controlled by the crest value of an a-c wave and not by its root-mean-square value, the significance of this fact has received little attention. Previous papers on the general subject[1,2] have been concerned chiefly with developing a method of analysis and establishing concepts of reasonably safe sine-wave electric currents for various frequencies and for direct current. The procedure disclosed in this paper extends the analysis to permit estimating reasonably safe let-go currents for practically any commercial wave form, from sine-wave alternating current on one hand to pure direct current as the other extreme. The results of the analysis are believed to be important in increasing the safety of certain electronic devices, filters, and communication circuits, to the end that the sustained shock current obtained from parts of the circuit readily accessible to an operator may be held within reasonably safe or known limits. Also, the results may have value in specifying allowable leakage current to the chassis of radiobroadcast receivers; for improved design of high-voltage television power supplies; for defining the permissible magnitude of the components in the sustained output current from capacitor-discharge electric-fence controllers, and other similar apparatus. Although the conclusions have been based largely on experiments made with 60-cycle alternating current and direct current, the results may also be used for cases involving higher frequencies, but with increased factor of safety.

Results

Mean let-go current values obtained from tests made with various wave shapes follow the same curve if the crest value of the a-c component is plotted on one axis versus the d-c component on the other. Two conditions must be met in order to have the experimental data fall on the same curve. These conditions are:

1. The reference axis for measurement of the alternating component must be the average value or the direct component.
2. The peak or crest value of the alternating component must be measured in the direction of the maximum total current.

These requirements are illustrated in Figure 1, which gives sketches of the wave forms used in these experiments.

When measurements were made according to these two conditions, points representing the mean let-go current obtained from groups of subjects tested on sine-wave alternating current, direct current, and the four complex wave forms of Figure 1 fell closely on the top curve of Figure 2. The fact that the points fell on the same curve is important, because it permits estimating let-go currents for other wave forms as well as those used to locate the curve. Thus, from two simple measurements one may determine the probable shock hazard for electric currents of any assumed wave form. The 99.5 percentile curves for men and women were computed from equations 3 and 4, reference 2. In the absence of experimental data, the reasonably safe current for children was taken as 50 per cent of the safe values for men. Percentile curves for either more conservative or more liberal probabilities may be computed readily by using a similar procedure.

In these experiments sine waves were superimposed on pure direct current to give the wave forms illustrated in Figures 1B and 1C. In these tests crest *A* was equal to crest *B*, and the requirement that the peak of the alternating component be measured in the forward direction, or in the direction of the maximum total current, was unnecessary. The current wave of Figure 1B was similar but not identical to that obtained from the filtered output of an ordinary rectifier. Measurements were made to determine the alternating component of two rectifiers using a shunt and a crest vacuum-tube voltmeter calibrated to read milliamperes. Considerable difference in the magnitude of the opposite crests was obtained, which emphasized the importance of selecting the proper crest of the a-c ripple when using the method for practical problems.

The importance of determining the crest of the alternating component in the direction of the maximum total current is further illustrated from consideration of the wave forms of Figures 1D and 1E. These current waves represent the output from a half-wave and a full-wave rectifier, respectively. Consider crest *B* for either case. The vacuum-tube crest milliammeter indicated a definite reading when connected to measure crest *B*, even though the total current was zero. This

Table I. Effect of Wave Form on 180-Cycle Let-Go Current

Subject No.	Triangular Wave		Sine Wave	
	Crest Milli-amperes	Rms Milli-amperes	Crest Milli-amperes	Rms Milli-amperes
2	22.1	12.8	21.4	15.1
12	22.6	13.0	21.9	15.5
21	30.0	17.3	42.4	30.0
22	38.7	22.3	36.3	25.7
23	25.2	14.5	23.2	16.4
27	21.2	12.2	20.1	14.2
30	37.1	21.4	31.7	22.4
31	25.5	14.7	25.2	17.8
32	26.0	15.0	23.3	16.5
36	26.0	15.0	25.7	18.2
37	34.4	19.8	32.5	23.0
40	27.2	15.7	25.5	18.0
45	26.8	15.5	23.5	16.6
54	27.7	16.0	32.8	23.2
55	30.3	17.5	26.0	18.4
61	27.4	15.8	29.4	20.8
64	30.0	17.3	30.0	21.2
67	24.0	13.8	22.9	16.2
74	24.1	13.8	23.8	16.[illegible]
Mean	27.7	16.0	27.2	19.2

Paper 43-135, recommended by the AIEE committee on safety for presentation at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943. Manuscript submitted June 3, 1943; made available for printing July 12, 1943.

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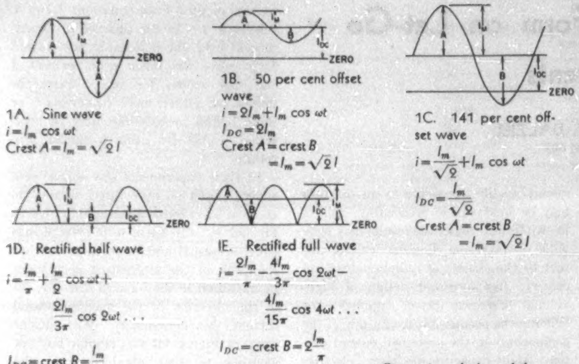
Data presented in this paper were obtained under the supervision of Major John B. Lagen, Medical Corps, United States Army, and Eric Ogden, professor of physiology, University of Texas Medical Branch, and consulting physiologist, John Sealy Hospital, Galveston, Tex. At the time this study was made Major Lagen was assistant professor of medicine and pharmacology, University of California Hospital, San Francisco, Calif., and Professor Ogden was associate professor of physiology, University of California Medical School, Berkeley, Calif. Acknowledgments are also given the men and women who volunteered for these experiments without whose interest and co-operation this work would have been impossible.



1A. Sine wave
$i = I_m \cos \omega t$
Crest $A = I_m = \sqrt{2}\, I$

1B. 50 per cent offset wave
$i = 2I_m + I_m \cos \omega t$
$I_{DC} = 2I_m$
Crest A = crest B
$= I_m = \sqrt{2}\, I$

1C. 141 per cent offset wave
$i = \frac{I_m}{\sqrt{2}} + I_m \cos \omega t$
$I_{DC} = \frac{I_m}{\sqrt{2}}$
Crest A = crest B
$= I_m = \sqrt{2}\, I$

1D. Rectified half wave
$i = \frac{I_m}{\pi} + \frac{I_m}{2} \cos \omega t + \frac{2I_m}{3\pi} \cos 2\omega t \ldots$

I_{DC} = crest $B = \frac{I_m}{\pi}$
Crest $A = I_m - I_{DC}$
$= I_{DC}(\pi - 1) = 2.14\ I_{DC}$

IE. Rectified full wave
$i = \frac{2I_m}{\pi} - \frac{4I_m}{3\pi} \cos 2\omega t - \frac{4I_m}{15\pi} \cos 4\omega t \ldots$

I_{DC} = crest $B = 2\frac{I_m}{\pi}$
Crest $A = I_m - I_{DC} =$
$I_{DC}\left(\frac{\pi}{2} - 1\right) = 0.571\ I_{DC}$

Figure 1. A few of the wave forms used in the let-go-current tests

was due to the fact that the alternating component and the direct component were equal and opposite for the instant considered. It would be surprising indeed if let-go currents were controlled by crest *B*. As far as determined, there are only two important factors which control let-go currents. These are crest *A* of the alternating component and I_{DC}, the average value, or the direct component of the wave. The required measurements may be made easily with direct reading instruments. The direct component may be measured with an ordinary d-c milliammeter, and the alternating component may be measured with a half-wave crest vacuum-tube instrument in conjunction with a cathode-ray oscilloscope. The latter is used to permit selecting crest *A* instead of crest *B*. Of course, the values may be obtained from a calibrated oscillogram, but this procedure is cumbersome and requires photographic processes.

Although the curves of Figure 2 were determined from six series of tests using 60 cycles and direct current, it is believed that conclusions based thereon will be conservative for wave forms containing higher harmonics or other wave forms likely to be met in practice. Let-go currents for sine waves have a broad minimum from about 15 cycles to 100 cycles. The reasonably safe current curves of Figure 3 indicate that human tolerance increases slowly at first and then quite rapidly for frequencies below or above this range. Predictions made from the curves of Figure 2 for alternating components having frequencies outside the middle band in reality assume that the let-go-current curves are straight lines tangent to the curves of Figure 3 at their lowest points. The predictions may therefore be conservative and well on the side of safety.

The relative-discomfort curve of Figure 3 was obtained by plotting the reciprocals of the average let-go current values for each frequency, that is, the reciprocal of the corrected mean currents, Table I, reference 2. The vertical scale was arranged so that the discomfort would be 100 per cent for 60 cycles. The curve indicates the relative discomfort caused by a given current for the frequency range included in the graph. Although a subject's let-go current increases considerably at the very low frequencies, his muscles follow the current variations, and the sensations, presumably caused by the peaks of the current wave, are more painful than those experienced on the 60-cycle tests. At very high frequencies, sensations of heat rather than pain predominate. The term "relative discomfort" must therefore be given a broad interpretation, and, in the absence of a means of specifying varying degrees of pain, the curve may be taken to show in a general way the discomfort or the relative danger of a given current as a function of frequency.

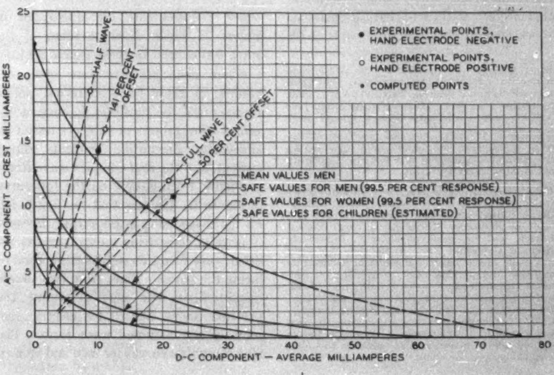
Experimental Data and Analysis

EFFECTS OF A-C WAVE FORM ON LET-GO CURRENTS

Before investigating the reactions produced by composite currents, it was considered desirable to make an additional investigation of the effect of wave form on let-go currents for complex alternating currents, that is, for wave forms containing no direct component. As previously reported, let-go currents appeared to be controlled by the crest and not by the root-mean-square (rms) value of the current. In these tests, comparisons were made for 60-cycle sine-wave current alone and in combination with a third harmonic equal to approximately $37^1/_2$ per cent of the fundamental. In one case, the third harmonic was phased so that the resultant wave was sharply peaked. For the other case, the third harmonic was shifted 180 degrees to produce a relatively flat-topped wave. Inspection of the data obtained from 26 subjects indicated that let-go currents were controlled by the peak of the current wave and not the root-mean-square value.[1]

A second series of tests was conducted subsequently in which let-go currents

Figure 2. Let-go-current curves plotted as a function of a-c and d-c components



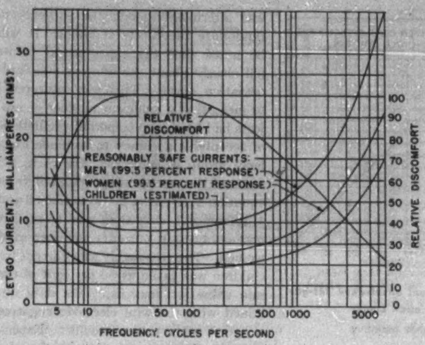


Figure 3. Sine-wave let-go currents and relative discomfort curves versus frequency

for 19 subjects were measured using sine and triangular waves of 180 cycles frequency. An oscillogram of the triangular current wave is shown in Figure 4. The triangular wave was obtained from a rheostat connected across the terminals of the interphase transformer of a small six-phase hot-cathode thyratron rectifier supplying a resistance load. Sine-wave 180-cycle currents were obtained from a beat-frequency oscillator and power amplifier. Experimental data for this test are given in Table I. The number 6 copper-wire electrode wet with salt solution was used for this test and for all other tests discussed in this paper. Although the let-go values were slightly more erratic than those observed for the 60-cycle wave-form tests, comparison of most individuals' values and the mean values for the group corroborate the conclusion that the crest of the a-c wave is the major factor which controls the let-go current.

Reasonably Safe 60-Cycle Sine-Wave Currents for Adults

The following data and analyses were taken from reference 2. The mean sine-wave 60-cycle let-go current for men was 15.87 rms milliamperes $=22.4$ crest milliamperes. The 99.5 percentile $=15.87\ (1-0.432)=9$ rms milliamperes $=12.7$ crest milliamperes (from equation 1). The mean sine-wave 60-cycle let-go current for women was 10.5 rms milliamperes $=14.8$ crest milliamperes. The 99.5 percentile $=10.5\ \ (1-0.432)=5.95$ rms milliamperes $=8.4$ crest milliamperes. These values were plotted on the vertical axis of Figure 2 to anchor the curves at this end of the graph.

Reasonably Safe 60-Cycle Sine-Wave Currents for Children

Obtaining let-go currents for young children appears difficult if not impossible of accurate determination. In addition to the reluctance of parents to permit experimentation on their children, limited experience indicates that children are likely to refuse to volunteer for a sufficient number of trials to permit determination of their let-go current with any degree of accuracy. As previously mentioned[2] let-go currents appear related to the muscular development of the wrist and forearm. However, all attempts to find a correlation, other than general appearance, with age, weight, strength of grip, or arm measurements were without conclusive results. In view of the early development of the grip of children it might be expected that their let-go currents would be relatively large; moreover it is a common observation that children appear to be very hardy in their ability to withstand a variety of conditions which would be disastrous to adults. In the interest of establishing a reasonably safe current for normal healthy children, and in the absence of experimental data, it seemed reasonable to estimate the probable safe current for children as 50 per cent of the safe value for normal adult males. Reactions caused by currents of this magnitude were unobjectionable to the men, and they were found to be amply safe for three small boys tested. The following are the 60-cycle sine-wave let-go currents obtained on the boys:

Age 5 years.....Let-go current 7 rms milliamperes*
Age 9 years 3..Let-go current 7.6 rms months milliamperes
Age 10 years 11..Let-go current 9 rms months milliamperes

Accordingly, the reasonably safe sine-wave value for normal children was com-

Table II. Let-Go Currents—Sine-Wave and 141 Per Cent Offset-Wave Tests

Hand Electrode Positive			Hand Electrode Negative		
Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Offset-Wave A-C Component (Rms Milliamperes)	Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Offset-Wave A-C Component (Rms Milliamperes)
73	9.2	7.7	73	9.2	7.5
104	12.6	10.0	104	12.6	8.0
128	13.2	10.0	123	17.8	8.8
93	15.9	11.2	93	15.9	9.3
99	15.8	11.6	99	15.8	9.4
130	16.9	12.2	130	16.9	10.4
129	16.5	12.7	124	21.5	10.5
134	15.1	13.0	132	19.4	11.5
132	19.4	13.0	128	13.2	11.6
			129	16.5	12.0
			134	15.1	12.8
Mean	14.96	11.27		15.81	10.12
Corrected mean:					
D-c average and a-c rms	11.27×15.87/14.96 = 11.96 milliamperes			10.12×15.87/15.81 = 10.16 milliamperes (equation 2, reference 2)	
A-c crest	$\sqrt{2}$×11.96 = 16.91 milliamperes			$\sqrt{2}$×10.16 = 14.37 milliamperes (see Figure 1C)	
99½ percentile:					
A-c crest men	16.91(1.0−0.432) = 9.60 milliamperes			14.37(1.0−0.432) = 8.16 milliamperes	
D-c average men	11.96(1.0−0.432) = 6.79 milliamperes			10.16(1.0−0.432) = 5.77 milliamperes (equation 3, reference 2)	
A-c crest children	0.5×9.60 = 4.8 milliamperes			0.5 × 8.16 = 4.1 milliamperes	
D-c average children	0.5×6.79 = 3.4 milliamperes			0.5 × 5.77 = 2.9 milliamperes	
A-c crest women	9.60×10.5/15.79 = 6.4 milliamperes			8.16 × 10.5/15.87 = 5.4 milliamperes	
D-c average women	6.79×10.5/15.87 = 4.5 milliamperes			5.77×10.5/15.87 = 3.8 milliamperes (equation 4, reference 2)	
Polarity effect	10.16/11.96 = 0.85				

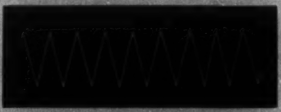


Figure 4. Triangular current wave used to determine effect of wave form on let-go currents for complex a-c wave forms

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* Determined by Royce E. Johnson, director, electrical standards laboratory, University of Wisconsin, Madison, Wis.

puted as $0.50 \times 9 = 4.5$ rms milliamperes = 6.3 crest milliamperes.

REASONABLY SAFE DIRECT CURRENTS

The following d-c release currents were taken from reference 2:

Men, corrected average = 76.1 d-c milliamperes
99.5 percentile = 62.0 d-c milliamperes
Women, 99.5 percentile = 41.0 d-c milliamperes

Using the reasoning of the preceding paragraph, the reasonably safe direct current for healthy children was computed as $0.50 \times 62.0 = 31.0$ d-c milliamperes. These values were plotted on the horizontal axis of Figure 2 to fix the lower limits of the curves. At the conclusion of the a-c let-go-current tests, the two boys used in this investigation released the following direct currents easily and without comment. Higher values were not attempted:

Age 9 years 3 months . . . 15 d-c milliamperes
Age 10 years 11 months . . . . 26 d-c milliamperes

141 PER CENT OFFSET-WAVE-FORM LET-GO-CURRENT TESTS

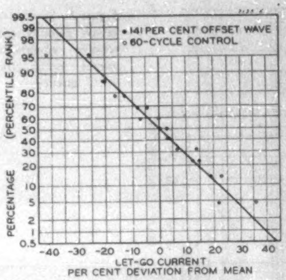

Figure 5. 141 per cent offset-wave let-go-current deviation curve

Hand electrode negative

In these tests 60-cycle sine-wave alternating current was superimposed on direct current to produce the current wave forms shown in Figure 1C. The proper proportion of the alternating current to direct current was maintained by connecting an ordinary d-c milliammeter in series with a shunt to which was connected a General Radio type 726*A* vacuum-tube voltmeter. Adjustments were made so that the d-c milliamperes were always equal to the root-mean-square a-c milliamperes, resulting in a wave having a ripple equal to

$$\text{Ripple} = \frac{\text{crest a-c milliamperes}}{\text{average milliamperes}} = \frac{\sqrt{2}\ \text{rms milliamperes}}{\text{d-c milliamperes}} = 1.41$$

Experimental values and analysis are given in Table II, and the corresponding deviation curves are shown in Figures 5 and 6. Three determinations of let-go currents were made; one test with the wire electrode negative, one with the wire electrode positive, and one test with commercial 60-cycle sine wave to serve as a control. The three tests were conducted on the same afternoon to obtain comparable results. The sequence of the tests was alternated among the subjects to randomize the effects of fatigue. It will be noted that the experimental points fell closely about the 60-cycle standard-deviation curve. This was interpreted to mean that a sufficient number of subjects had been used to permit predictions of acceptable accuracy to be made for a large group of subjects. The corrected mean of the sample was computed from equation 2, reference 2. Equations 3 and 4, reference 2, were then used to predict reasonably safe currents for men and women. The reasonably safe current for children was taken as 50 per cent of the safe value for men, using the data obtained with the wire electrode negative to give conservative results. Experimental and computed points for the wire negative were then plotted on Figure 2 to assist in locating the mean and safe let-go current curves.

50 PER CENT OFFSET-WAVE-FORM LET-GO-CURRENT TESTS

A similar procedure was used to determine the let-go currents for a wave having 50 per cent a-c ripple. For these tests the proportion of alternating current to direct current was maintained so that the d-c milliammeter read exactly twice the crest a-c milliamperes. Experimental values and analyses are given in Table III and the corresponding deviation curves in Figures 7 and 8.

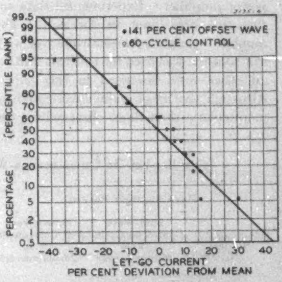

Figure 6. 141 per cent offset-wave let-go-current deviation curve

Hand electrode positive

Although the experimental points (especially the 60-cycle control points) did not follow the 60-cycle standard-deviation curve as closely as desired, this was the best that could be done with the available data. At the beginning of the investigation some six years ago, it was planned to obtain values for at least 25 subjects for each test in order to eliminate possible criticism due to deriving conclusions from

Table III. Let-Go Currents—Sine-Wave and 50 Per Cent Offset-Wave Tests

Hand Electrode Positive			Hand Electrode Negative		
Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Offset-Wave A-C Component (Rms Milliamperes)	Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Offset-Wave A-C Component (Rms Milliamperes)
123	17.5	7.2	93	17.2	6.0
125	19.3	7.7	123	17.5	6.3
93	17.2	7.9	132	18.3	6.8
82	16.2	8.3	125	19.3	7.1
129	15.1	8.4	129	15.1	7.8
132	18.3	8.6	130	17.6	7.8
135	16.7	8.6	82	16.2	8.1
124	21.5	9.2	124	21.5	8.3
130	17.6	9.3	135	16.7	8.6
134	19.8	9.5	134	19.8	9.8
Mean:	17.92	8.47		17.92	7.66
Corrected mean:					
A-c crest	$\sqrt{2} \times 8.47 \times 15.87/17.92 = 10.61$ milliamperes			$\sqrt{2} \times 7.66 \times 15.87/17.92 = 9.60$ milliamperes (equation 2, reference 2)	
D-c average	$2 \times 10.61 = 21.22$ milliamperes			$2 \times 9.60 = 19.20$ milliamperes (see Figure 1B)	
$99\frac{1}{2}$ percentile:					
A-c crest men	$10.61(1-0.432) = 6.02$ milliamperes			$9.60(1-0.432) = 5.45$ milliamperes	
D-c average men	$21.22(1-0.432) = 12.05$ milliamperes			$19.20(1-0.432) = 10.92$ milliamperes (equation 3, reference 2)	
A-c crest children	$0.5 \times 6.02 = 3.0$ milliamperes			$0.5 \times 5.45 = 2.7$ milliamperes	
D-c average children	$0.5 \times 12.05 = 6.0$ milliamperes			$0.5 \times 10.92 = 5.5$ milliamperes	
A-c crest women	$6.02 \times 10.5/15.87 = 4.0$ milliamperes			$5.45 \times 10.5/15.87 = 3.6$ milliamperes	
D-c average women	$12.05 \times 10.5/15.87 = 8.0$ milliamperes			$10.92 \times 10.5/15.87 = 7.2$ milliamperes (equation 4, reference 2)	
Polarity effect	$9.60/10.61 = 0.90$				

Table IV. Let-Go Currents—Sine-Wave and Six Per Cent Offset-Wave Tests

Hand Electrode Negative

Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Offset-Wave Components A-C Rms Milliamperes	Offset-Wave Components D-C Average Milliamperes	Remarks
45	16.2	2.3	53	Let-go current
134	15.1	2.9	70	Limit of endurance
140	15.8	3.2	73	Limit of endurance
Mean:	15.7	2.8	65.3	

A-c crest $= 2.8\sqrt{2} = 3.96$

Per cent ripple $= \frac{3.96}{65.3} = 0.06$

insufficient data. Conclusions based on available data are therefore submitted, subject to slight adjustments, should this be found desirable when conditions permit resumption of the project. Experimental research was stopped when the nation became involved in the war, and it will not be resumed for the duration.

Six Per Cent Offset-Wave-Form Let-Go-Current Tests

The results of let-go current tests made with a composite wave having a ratio of crest alternating current to direct current equal to 0.06 are given in Table IV. This was the last test made before the experiments were terminated, and only three subjects were used. Three values are too few in number to warrant drawing conclusions, and the point representing this test was omitted from Figure 2. However, if the point representing the average for the group is inserted on Figure 2, it will be found to be well above the mean curve. This is consistent with the conjecture that the lower end of the curve, which was determined from tests using pure direct current, is well on the side of safety. These data are instructive, since one of the three subjects reached his let-go limit while the other two did not. Although two subjects could still release the electrode, they refused higher currents because of the severity of the shock at the instant of releasing the electrode. This is the only case where let-go currents and release currents were encountered in the same test. These observations stress the importance of considering the alternating component, regardless of how small it may be in comparison to the direct component, when studying shock hazards. Sensations produced by the filtered output of a *B* eliminator, in which the a-c ripple was so small that it was thought negligible, were much more painful than the same current obtained from a battery.

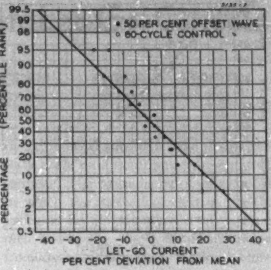

Figure 7. 50 per cent offset-wave let-go-current deviation curve

Hand electrode negative

Table V. Let-Go Currents—Sine-Wave and Rectified-Half-Wave Tests

Hand Electrode Positive

Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Rectified Half-Wave (Average Milliamperes)
45	18.5	6.7
77	14.4	7.0
126	17.8	8.5
129	16.9	8.6
130	18.5	8.9
125	17.3	9.1
132	15.6	9.1
124	17.3	9.5
37	20.9	9.8
21	25.1	10.0
93	17.1	10.1
Mean:	18.13	8.84

Determination of polarity correction factor	
From Table II	0.85
From Table III	0.90
Mean	0.875

Corrected mean:
D-c average men . . . . . 8.84×15.87/18.13=7.74 milliamperes, hand electrode positive (equation 2, reference 2)
A-c crest men . . . . . 2.14×7.74=16.57 milliamperes, hand electrode positive (see Figure 1D)
D-c average men . . . . . 0.875×7.74= 6.77 milliamperes, hand electrode negative (using polarity correction factor=0.875)
A-c crest men . . . . . 2.14×6.77=14.49 milliamperes, hand electrode negative (see Figure 1D)

$99^1/_2$ percentile:
A-c crest men . . . . . 14.49(1−0.432)= 8.22 milliamperes, hand electrode negative
D-c average men . . . . . 6.77(1−0.432)= 3.85 milliamperes, hand electrode negative (equation 3, reference 2)
A-c crest children . . . . . 0.5×8.22= 4.1 milliamperes, hand electrode negative
D-c average children . . . . . 0.5×3.85= 1.9 milliamperes, hand electrode negative
A-c crest women . . . . . 8.22×10.5/15.87= 5.4 milliamperes, hand electrode negative
D-c average women . . . . . 3.85×10.5/15.87= 2.5 milliamperes, hand electrode negative (equation 4, reference 2)

Table VI. Sine Wave and Rectified Full-Wave Tests

Hand Electrode Positive

Subject No.	Sine-Wave Control 60 Cycle (Rms Milliamperes)	Rectified Full-Wave (Average Milliamperes)
82	12.2	15.0
128	13.5	16.0
123	16.2	18.5
45	16.2	18.5
131	16.8	19.0
132	16.1	19.8
129	13.5	20.2
122	19.8	21.0
124	18.2	21.5
130	14.6	22.1
125	17.3	23.4
109	20.0	26.4
37	20.9	26.4
105	23.0	27.5
Mean:	17.02	21.09

Corrected mean:
D-c average men . . . . . 21.99×15.87/17.02= 19.66 milliamperes, hand electrode positive (equation 2, reference 2)
A-c crest men . . . . . 0.571×19.66= 11.22 milliamperes (see Figure 1E)
D-c average men . . . . . 0.875×19.66= 17.20 milliamperes, hand electrode negative (using polarity correction factor=0.875)
A-c crest men . . . . . 0.571×17.20= 9.82 milliamperes, hand electrode negative (see Figure 1E)

$99^1/_2$ percentile:
A-c crest men . . . . . 9.82(1−0.432)= 5.58 milliamperes, hand electrode negative
D-c average men . . . . . 17.20(1−0.432)= 9.77 milliamperes, hand electrode negative (equation 3, reference 2)
A-c crest children . . . . . 0.5×5.58= 2.8 milliamperes, hand electrode negative
D-c average children . . . . . 0.5×9.77= 4.9 milliamperes, hand electrode negative
A-c crest women . . . . . 5.58×10.5/15.87= 3.7 milliamperes, hand electrode negative
D-c average women . . . . . 9.77×10.5/15.87= 6.5 milliamperes, hand electrode negative (equation 4, reference 2)

Rectified Full- and Half-Wave Let-Go-Current Tests

Let-go current tests were conducted, using the output of a single-phase mercury-arc rectifier. The results obtained using one anode are given in Table V and Figure 9. Similar data obtained for both anodes are given in Table VI and Figure 10. These tests were conducted with the hand electrode positive. Sixty-cycle sine-wave let-go tests were also conducted to serve as a control for computing the response for a large group, as previously discussed.

It was found that the sensations produced by the current were more painful and the let-go current was less when the hand electrode was negative in comparison to tests made with the hand electrode positive. The tests using the rectified waves were conducted with the hand electrode positive, and it was necessary to determine a correction for polarity, in order to compare the points representing these tests with the preceding data. The effect of polarity was determined from the 141 per cent and the 50 per cent offset-wave tests. A polarity correction factor was taken equal to the ratio (mean let-go current, hand electrode negative) ÷ (mean let-go current, hand electrode positive). The average ratio obtained from these two tests was taken as the polarity correction factor = 0.875. This factor was used in the analysis of the data of Tables V and VI, and the points determined thereby were given due consideration in locating the curves of Figure 2.

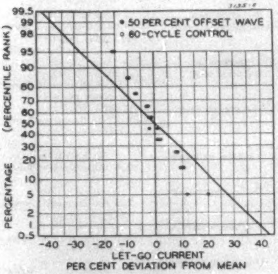

Figure 8. 50 per cent offset-wave let-go-current deviation curve

Hand electrode positive

The polarity effect, which was found to decrease the average let-go currents $12^{1}/_{2}$ per cent when the hand electrode was negative, may have value from a safety standpoint. The decrease in both the sensations and the shock hazard for a given current when the hand electrode is positive may be helpful in arriving at a decision whether to ground the positive or the negative side of a circuit.*

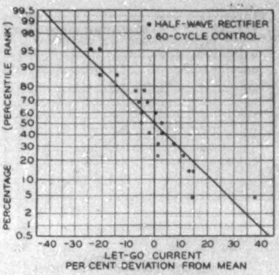

Figure 9. Rectified half-wave let-go-current deviation curve

Hand electrode positive

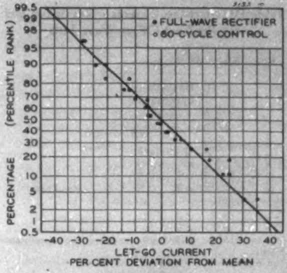

Figure 10. Rectified full-wave let-go-current deviation curve

Hand electrode positive

As can be seen from inspection of Figure 10, the experimental points for the full-wave-rectifier test followed the 60-cycle standard-deviation curve closely. In comparison, the points for the half-wave-rectifier test were somewhat more scattered about the deviation curve (see Figure 9). It should be noted that the scattering about the deviation curve was about the same for both the wave form under investigation and the 60-cycle control. The scattering was explained as due to the limited number of subjects tested rather than to suggesting a different response for rectified half-wave currents. It is believed that data from a larger group of subjects would have reduced the discrepancies and resulted in the points following the 60-cycle deviation curve as closely as that found for the other tests.

Smooth curves were drawn through the computed points (wire negative) to give the curves of Figure 2. Several other attempts were made to analyze the data, but for each case tried at least some of the computed points failed to follow a common curve. Points obtained from the analysis described in the foregoing paragraphs closely follow the same curve, and for this reason it is believed that the procedure is sufficiently general to permit predicting reasonably safe currents with acceptable accuracy for wave shapes similar as well as those actually used in the experiments. Although one must use caution when arriving at conclusions, the good agreement between the experimental points and the theory suggest that the analysis should be valid for predictions involving complex or composite waves containing a-c components and d-c components in the proportions likely to be met on commercial apparatus or associated circuits. As previously discussed, it is believed that errors due to wave forms containing high-frequency components should be on the safe side, and the resulting currents should be on the side of safety.

References

1. Electric Shock, Charles F. Dalziel, John B. Lagen, Joe L. Thurston. AIEE Transactions, volume 60, 1941, pages 1073–9; discussion pages 1295–7.

2. Effect of Frequency on Let-Go Currents, Charles F. Dalziel, Eric Ogden, Curtis E. Abbott, AIEE Transactions, volume 62, 1943, December section, pages 745–50.

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* It is well known to physiologists that during passage of direct current through a nerve the regions in the neighborhood of the electrodes are altered with respect to

1. Sensitivity to stimulation.
2. Ability to conduct nerve impulses.

The negative region becomes more sensitive and a better conductor.

Effect of Frequency on Let-Go Currents

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Synopsis: This paper on electric shock covers the subject of sine-wave let-go currents for both men and women and contains an analysis which permits improved accuracy in predicting the response for large groups based on experiments made on a relatively small number of subjects. It should be of especial interest to persons who have had accidents in which they barely escaped "freezing" to an electrified conductor and also to those interested in electrical safety. The range of frequencies covered is from 5 to 10,000 cycles and also direct current. The paper is the most comprehensive treatment of the subject yet published as the analysis permits predicting currents of a specified degree of safety for both men and women for this wide frequency range.

THIS paper presents the results of a study to determine the effect of frequency on let-go currents made on 134 men and 28 women at the University of California. Let-go currents are important, because it has been found that an individual can withstand, with no ill aftereffects, repeated exposure to his let-go current for at least the time required for him to release the conductor. Larger currents are dangerous, because release from the circuit during accidental contact is problematical. A reasonably safe current for normal men and women is defined as the let-go current for $99^1/_2$ per cent of a large group. A knowledge of let-go currents is important for the formation of safety codes, for explaining accidents, and for the design of electrical devices with exposed electrodes.

The majority of the data was presented in a preliminary report before the Institute in 1941.[1] The number of male subjects for the 60-cycle test now totals 134, the data on females are new, and an improved analysis is disclosed. Use of the new analysis permits improved accuracy in predicting results for a large group from tests made on a small but representative sample. The paper includes an example in which the method is used to predict probable let-go currents for all normal men and women for a frequency range from 5 to 5,000 cycles. Estimates are given for 10,000 cycles and also for direct current.

Determination of Let-Go Currents

An individual's let-go current is defined as the maximum current he can tolerate when holding a copper conductor in one hand and yet let go of the conductor by using muscles directly affected by that current. No material changes in experimental procedure, safety precautions, selection of subjects, or equipment were found to be necessary, and, in general, the experiments were continued as described in the preliminary report. The following résumé is included for convenience. The subjects held and then released a test electrode consisting of a number 6 copper wire, and the circuit was completed by placing the other hand or foot on a flat brass plate, or by clamping a conducting band lined with moist cloth firmly on the upper arm. After one or two preliminary trials to accustom the subject to the sensations and muscular contractions produced by the current, the current was increased to a certain value and the subject was commanded to let go of the wire. If he succeeded, the test was repeated at a slightly higher value. If he failed, a lower current was used, and the values were again increased until the subject could no longer release the test electrode. The end point was checked by several trials. The agreement between those trials was within about two to five per cent, and the highest value was taken as the individual's let-go value in order to eliminate the effects of fatigue.

The tests reported here were conducted with hands wet with a common saltwater solution to secure uniform conditions and to reduce the sensation of burning caused by high current densities at tender spots and at the instant of releasing the electrode. Other tests were made with dry hands, hands moist from perspiration, and hands dripping wet from weak acid solutions. The effect of the size of the electrodes was also investigated. As reported previously, the location of the indifferent electrode, the moisture conditions at the points of contact, and the size of the electrodes had no appreciable effect on an individual's let-go current. It is believed that conclusions based on tests using the wet copper electrode may be used to predict reasonably safe electric currents with an accuracy sufficient for most practical purposes.

There was considerable variation in an individual's value on succeeding trials, the trend usually being toward higher values. Therefore the largest current released on the *first* test on any frequency was taken as the let-go current for that frequency. This was done to include the element of surprise to as great an extent as possible and to give conservative results. Psychological factors, especially fear and competitive spirit, were the most important causes for the variations. Physiological factors also played an important part, but so far their exact mechanism remains unknown. It seemed that let-go currents in both sexes were related to the muscular development of the wrist and forearm. Husky subjects having low let-go values could almost invariably be persuaded (or heckled by other subjects) to continue the test until their values were in line with others of similar physique. Although attempts were made to quantitate this relationship by correlating let-go currents with the strength of grip and with forearm and wrist diameters, the results were inconclusive. Perhaps this was because of the narrow range and lack of precision involved in the physiological measurements; moreover, forearm and wrist diameters are determined to an uncertain degree by fat and bone as well as by muscular tissue.

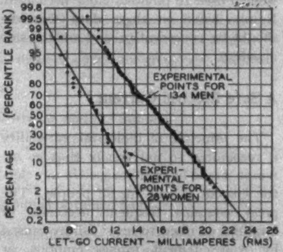

Figure 1. 60-cycle let-go-current distribution curves for men and women

Electric currents of a specified degree of safety for normal men or women may be obtained directly from the curves

Sixty-cycle let-go currents were meas-

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ured on 27 additional women, bringing the total to 28. The subjects ranged in age from the late teens to the early twenties, were light in stature, and obviously not accustomed to hard physical work, and their forearm muscles were not particularly well developed. Although the women volunteered freely for the tests, it proved impossible to develop enthusiasm or any degree of competition at high currents. The results are probably representative for the sedentary type; however, from observation of the reactions of subjects having the greatest muscular development, it is likely that the values were considerably lower than those which would have been obtained, had a typical group of mature healthy women used to physical labor been used. Results based on these data therefore should be conservative and on the side of safety.

A Safe Electric Current

The principle of biological variability is recognized so universally that no attempt should be made to specify any electric current as safe for all people. The press contains frequent accounts of fatalities ascribed to heart failure caused by overexcitement, intense emotion, or shock (shock of injury, *not* electric shock). Some of the subjects volunteering for these tests became frightened and trembled all over; some even complained of pain when holding the test electrode before the current was turned on. Although these persons were not used in the experiments, the experiences dramatically illustrated the possibility that a person with a diseased heart might succumb from any contact, or even the fear of contact, with an electric circuit. This possibility must be recognized, and an occasional death is to be expected as a result of casual contact involving electric currents known to be harmless to the great majority of healthy individuals.

Quite aside from determining an absolutely safe electric current for all human beings is the practical problem of determining a current which would be reasonably safe for most normal healthy adult men and women. In certain cases, economics or special applications may necessitate the use of current values which are hazardous to only a small percentage of a large group. This is particularly important at the present time, because of the widespread use of electric devices having exposed electrodes.[2,3]

Several factors must be considered in deciding what constitutes a reasonably safe electric current. The victim of a severe accidental electric shock can often release himself by using muscles little affected by the current. The muscular reactions produced by the current may tend to break the circuit rather than to improve it, or loss of balance may free the victim. However, it is doubtful if any of these methods would be of avail when contact is established by gripping an electrified conductor so that the path of the current was across the body. The hazard due to electric shock depends upon several factors, the most important being the magnitude of the current, the current pathway through the body, the time of contact, and the physical condition of the victim.

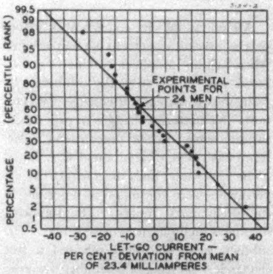

Figure 2. Five-cycle deviation curve for men

Conclusions regarding reasonably safe electric currents were based on the factors mentioned in the preceding paragraphs, together with the results of carefully conducted experiments made on a total of approximately 250 men and 28 women, and involved several thousand individual tests. Without a single exception, it was found that an individual could withstand, with no ill aftereffects, repeated exposure to his let-go current for at least the time required for him to release the conductor. The current pathway for the majority of the tests was between the hands. A few experiments were made with current pathway between one hand and one or both feet; in other tests the pathway was between the hand and an arm band.[2,3]

It is the authors' studied opinion that, for most practical purposes, the maximum electric current which is safe for an individual is the greatest current he can release by using muscles directly affected by that current. From this, it was concluded that a reasonably safe electric current for most normal healthy physically fit adult men and women is the let-go current for $99^1/_2$ per cent of a large group. Numerical determination of reasonably safe currents from the let-go current tests will be discussed later at an appropriate point in the text.

Analysis of Let-Go Currents

Let-go currents were obtained for groups of male subjects ranging in number from 25 to 134 using sine-wave alternating currents from 5 to 10,000 cycles and also direct current. The let-go currents for each test were then plotted on probability cross-section paper, and the straight line governed by the majority of the points was drawn to obtain the normal distribution curve for the sample. An advantage of this method is that points obviously not conforming to the sample may be disregarded by inspection. The procedure is illustrated in Figure 1 which gives the 60-cycle let-go-current distribution curves for 134 men and 28 women. Distribution curves for the other frefrequencies were similar, except for the current scale or the slope of the distribution curve.

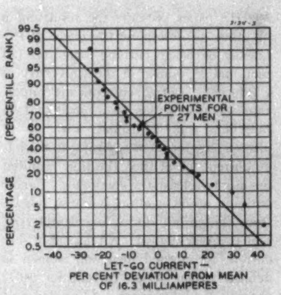

Figure 3. Ten-cycle deviation curve for men

It was found that, when let-go currents were plotted as per cent deviations from the mean of any test, the slope of all the resulting straight lines was the same. This was true for both men and women on the 60-cycle tests and for other tests from 5 to 5,000 cycles. Deviation curves for the 60-cycle tests are given in Figures 5 and 5A. Since only a small number of subjects was used for other than the 134-men 60-cycle test, the straight line response of Figure 5 was drawn on each curve as the probable response for the other frequencies also. Within the range of 5 to 5,000 cycles, the straight line representing the 60-cycle response was the best line that could be drawn through the majority of the points (see Figures 2 to 10 inclusive). The fact that let-go current values follow the same deviation curve may be illustrated in a rather striking manner if all the points (excluding the 60-cycle values, to avoid confusion) are plotted on the same curve sheet. The wide discrepancies of one or two points from the deviation curves were due to the relatively small number of subjects used. The reason that let-go currents follow the same curve for this range of frequencies may be related to the similarity of the physiological phenomena, which were characterized by painful muscular contractions. Outside of this frequency range, sensations of heat predominate. Strictly comparable measurements are not possible for direct current.

In previous studies, the mean of a test was taken as the most reliable measure for the sample, and the accuracy of prediction was chiefly dependent upon the number of subjects used for a particular test. Improved accuracy for small samples results from plotting the data as percentage deviations from the mean of the sample and comparing the response with the slope of the 134-men 60-cycle deviation curve of Figure 5. In the five-cycle test, shown in Figure 2, two points deviated from the rest of the data sufficiently to suggest that they should be discarded. Accordingly, the points were recomputed after the data from these two subjects had been rejected, leaving a total of 24 points. Such rejection was not found to be necessary in the other tests.

Predictions for any percentile rank are obtained from

$$\text{Percentile rank} = \text{mean of sample} \pm \text{deviation from mean} \times \text{mean of sample}$$

or

$$\text{Percentile rank} = \text{mean of sample}\ (1 \pm \text{deviation from mean}) \quad (1)$$

It is evident that the accuracy of prediction depends to a large extent upon the accuracy of the 134-men 60-cycle deviation curve and upon the subjects being a true random sample of the population. The data upon which this curve was constructed were obtained from carefully conducted tests made during a six-year period. Although care was taken to obtain only subjects in good physical condition, they were not selected especially with respect to age, weight, or muscular development. They represented a wide variety of normal men, with an age range of from 21 to 46. The close agreement between the 134 points and resulting straight line indicates that the response actually followed a normal distribution. This is exactly what should have been observed, had an infinite sample been tested. This close agreement is presented to substantiate the conclusion that a sufficient number of subjects has been examined to permit determination of the response of a large sample. Proceeding to the limit, it is assumed that a sufficient number of subjects has been tested to obtain the representative response of all normal men with an accuracy sufficient for engineering purposes.

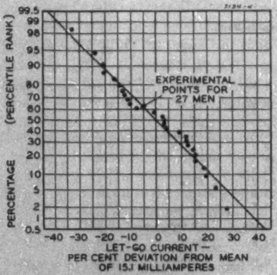

Figure 4. 25-cycle deviation curve for men

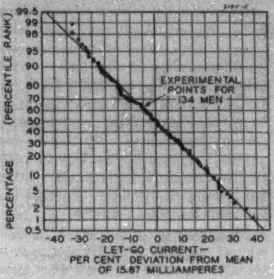

Figure 5. 60-cycle deviation curve for men

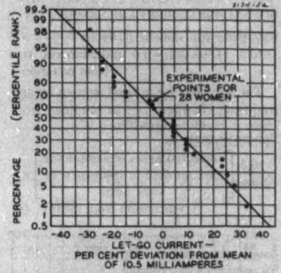

Figure 5A. 60-cycle deviation curve for women

An important factor controlling the accuracy of predictions for other tests is the necessity of procuring a representative sample. It is entirely possible that any small sample may have a mean considerably above or below that of an average sample, irrespective of agreement with the standard deviation curve. This is particularly important in experiments such as this, because it was usually found that let-go currents on the same frequency had a tendency to increase as the number of tests increased. This was attributed to the subject's becoming accustomed to the ordeal. Let-go-current tests should be conducted for the given test conditions and also on sine-wave 60 cycles to serve as a control. The order of the tests among the subjects should be changed frequently to randomize effects of fatigue.

The corrected mean, that is, the mean for an infinite sample equals

$$\text{Corrected mean of sample} = \frac{\text{Mean 60-cycle let-go current of all men}}{\text{Mean 60-cycle let-go current of sample}} \times \text{mean of sample for given test conditions}$$

or

$$\text{Corrected mean of sample} = \frac{15.87}{\text{60-cycle control}} \times \text{mean of sample} \quad (2)$$

The percentile rank for an infinite group is obtained from

$$\text{Percentile rank} = \text{corrected mean of sample} \times (1 \pm \text{deviation from mean}) \quad (3)$$

For example, the $^1/_2$ or $99^1/_2$ per cent response for any frequency between 5 and 5,000 cycles is given by

$$\text{Deviation from the mean for } ^1/_2 \text{ per cent or } 99^1/_2 \text{ per cent} = \pm 0.432 \text{ (from Figure 5)}$$

$$^1/_2 \text{ or } 99^1/_2 \text{ percentile rank} = \text{corrected mean of sample} \times (1 \pm 0.432)$$

Figure 14 shows the effect of frequency on let-go currents. Significant data are included in Table I to illustrate the method of analysis. At the time these data were obtained, no 60-cycle control was taken, but the subject's mean 60-cycle let-go current was used to give an

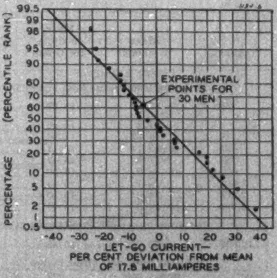

Figure 6. 180-cycle deviation curve for men

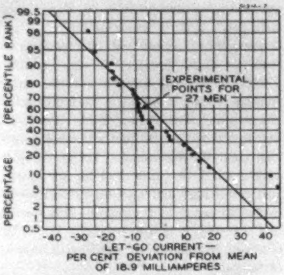

Figure 7. 500-cycle deviation curve for men

approximate correction. Computed points lie close to smooth curves, the data appear to be consistent, and the curves should represent the response for a large group of normal men. The curves read from below upwards illustrate the probability that the current they represent will be dangerous. As previously discussed, a reasonably safe current for normal male adults is proposed as that represented by the lowest curve which indicates values safe for at least $99^1/_2$ per cent of the group tested.

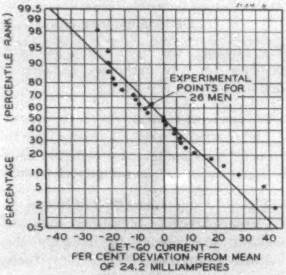

Figure 8. 1,000-cycle deviation curve for men

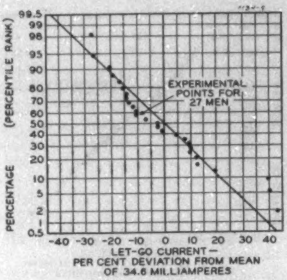

Figure 9. 2,500-cycle deviation curve for men

A safe current for normal women (Figure 5A) was computed as follows:

$99^1/_2$ percentile rank for 60 cycles

$$=10.5\ (1-0.432)=6.0 \text{ milliamperes rms}$$

from equation 1

The response for normal women on any frequency or wave form is obtained from

Percentile rank

$$=\frac{10.5}{15.87}\left[\begin{array}{c}\text{corrected mean of sample for}\\ \text{men on desired frequency or}\\ \text{wave form}\end{array}\right]\times (1\pm\text{deviation from mean}) \qquad (4)$$

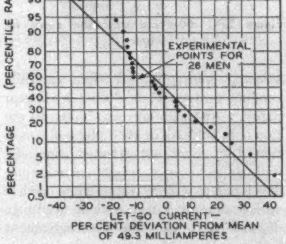

Figure 10. 5,000-cycle deviation curve for men

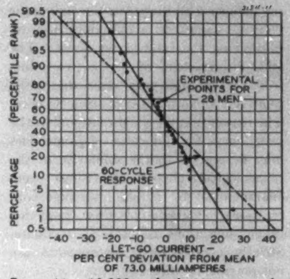

Figure 11. 10,000-cycle deviation curve for men

from which it is apparent that the let-go current for women is equal to $10.5 \div 15.87 = 66$ per cent of the corresponding percentile values for men. Hence the safe current for normal women is equal to approximately two-thirds of the bottom curve of Figure 14. This procedure is predicated on the assumption that the physiological phenomena associated with let-go currents for both normal men and women are similar and that the ratio of the let-go currents is the same at all fre-

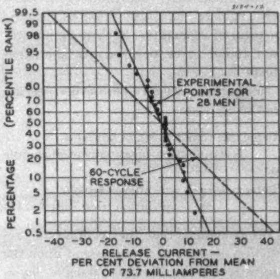

Figure 12. D-c deviation curve for men

Table I. Let-Go Currents for Men Versus Frequency

Frequency	Mean of Sample	Mean 60-Cycle for Group	Corrected Mean	Percentile Rank ½	1	25	50	75	99	99½
Deviation from mean (from Figure 5)				0.432	0.390	0.115	0.00	−0.115	−0.390	−0.432
60	15.87	15.87	15.87	22.7	22.1	17.7	15.87	14.0	9.7	9.0
5	23.4	14.56	25.5	36.5	35.5	28.4	25.5	22.6	15.5	14.5
10	16.27	14.94	17.3	24.8	24.0	19.3	17.3	15.3	10.5	9.8
25	15.14	15.11	15.9	22.7	22.1	17.7	15.9	14.1	9.7	9.1
180	17.8	15.42	18.3	26.2	25.4	20.4	18.3	16.2	11.2	10.4
500	18.9	15.53	19.3	27.6	26.8	21.5	19.3	17.1	11.8	11.0
1,000	24.2	15.84	24.2	34.7	33.7	27.0	24.2	21.5	14.8	13.7
2,500	34.6	15.57	35.2	50.4	48.9	39.3	35.2	31.2	21.5	20.0
5,000	49.3	15.17	51.6	73.9	71.7	57.8	51.6	45.7	31.5	29.3
Deviation from mean (from Figure 11)				0.260	0.234	0.068	0.00	−0.068	−0.234	−0.260
10,000	73.0	15.48	74.8	94.3	92.3	79.9	74.8	69.7	57.3	55.3
Deviation from mean (from Figure 12)				0.185	0.167	0.049	0.00	−0.049	−0.167	−0.185
D-c	73.7	15.37	76.1	90.3	88.9	79.9	76.1	72.5	63.5	62.1

quencies. It is believed that discrepancies due to sex differences are too small to affect practical conclusions with regard to electrical safety.

Sensations at the higher frequencies, especially those on 10,000 cycles or higher, were less painful, and the severity of the muscular contractions was less than that on the lower frequencies. Alarming sensations of internal heating were produced, muscular control was sluggish, and the time to let go the conductor increased considerably. These differences in physiological phenomena were associated with a deviation curve having a greater slope (see Figure 11). A straight line was drawn through the majority of the points to determine the deviation curve. The 60-cycle response was sketched on the curve for comparison. Although the experimental points fell close to the deviation curve, the accuracy of predicting the response for a large group is less than that for the lower frequencies, since it depends upon the accuracy of the small amount of data available on this frequency alone. However, the data were analyzed in a manner similar to that used for the other frequencies and are included in Table I.

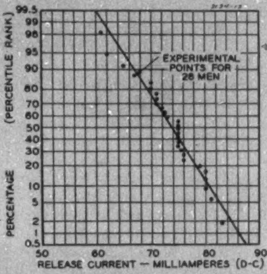

Figure 13. D-c release-current distribution curve for men

Tests on steady or gradually increasing direct currents produced sensations of internal heating rather than muscular contractions. Sudden changes in current magnitudes produced muscular contractions, and interruption of the current produced a very severe shock. The muscular reactions when the test electrode was released at the higher values were objectionable, and sooner or later all subjects declined to take more punishment. Tests were made on 28 men, and in each case little difficulty was experienced in releasing the electrode. The maximum a subject would take and release was termed the release current. It represents the limit of voluntary endurance rather than the let-go limit. The deviation curve for these tests is shown in Figure 12 and the release curve in Figure 13. At the conclusion of the 60-cycle let-go-current tests on 27 women, the experiments were terminated with one or two release tests using direct current. After one or two preliminary trials, the current was increased to a maximum of 35 milliamperes. Each subject released the electrode without complaint or difficulty. As previously reported,[1] a single woman was tested at the time the d-c tests were made on the men. She released 56 milliamperes direct current before refusing more.

The data for the d-c tests were analyzed in the same way as those for alternating currents but with less certain justification:

$$\text{Corrected mean of sample} = \frac{15.87}{15.37} \times 73.7$$

$$= 76.1 \text{ milliamperes direct current (from equation 2)}$$

$$\text{Deviation from mean for } 99^1/_2 \text{ percentile rank} = -0.185 \text{ (from Figure 12)}$$

$$99^1/_2 \text{ percentile rank for men}$$
$$= 76.1(1-0.185) = 62.0 \text{ milliamperes direct current (from equation 3)}$$

$$99^1/_2 \text{ percentile rank for women}$$
$$= \frac{10.5}{15.87} \times 76.1(1-0.185) = 41.0 \text{ milliamperes direct current (from equation 4)}$$

Based on the preceding data it is concluded that 62 and 41 milliamperes is a reasonably direct current for men and women respectively. Undoubtedly the maximum direct currents which are reasonably safe are in excess of these values. However, it would be unwise to suggest higher values as safe until additional experimental research is available.

Electrical Safety

Although current and *not* voltage is the proper criterion of shock intensity, the danger of accidental electric shock on commercial circuits is due almost entirely to the voltage of the circuit. Rather than that the layman or the public should be confused with a technical argument, he must be warned that all power circuits are dangerous. Many deaths are caused each year from accidental contacts with ordinary 120-volt lighting circuits. Contacts with house circuits are especially hazardous in moist or wet locations.

Conclusions

1. A reasonably safe electric current for normal healthy adults is the let-go current which $99^1/_2$ per cent of a large group can release by using muscles directly affected by that current.

2. The reasonably safe 60-cycle current for most normal healthy adult men is about nine milliamperes; the reasonably safe 60-cycle current for most normal healthy adult women is about six milliamperes.

3. Let-go currents are affected by fre-

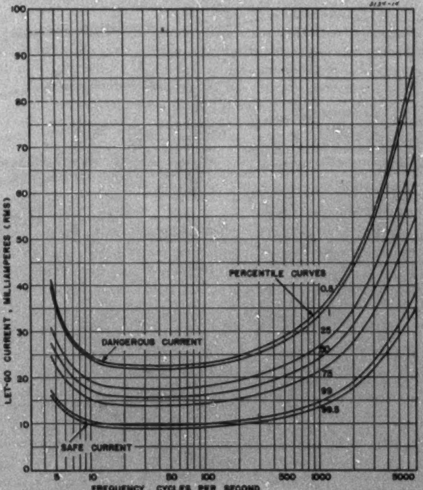

Figure 14. Sine-wave let-go current for men versus frequency

Current values become dangerous progressively to an increasing number of persons, as indicated by the percentile values on the right-hand side of the curves

Values for women are approximately 66 per cent of the current values shown on the curves quency. Unfortunately, the power frequencies appear to be the most dangerous. Larger currents may be tolerated for both the higher and the lower frequencies and for direct current.

4. The public must be warned continually of the danger of accidental contact with power circuits and against using defective home appliances.

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Some Measures of Electrical-Brush Disintegration

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Synopsis: So far as can be determined, the development of the concepts of wear resistance, wear resistivity, wear susceptance, and wear susceptibility as given in this paper is thoroughly new and original. These concepts are believed to be significant and useful tools that may further an understanding of the basic problems of sliding electrical contacts. For example, they offer means for making a valid comparison of brushes and brush materials insofar as wearing qualities are concerned. Moreover, they provide a clue to the proper consideration and evaluation of the large number of variables that are at work as brush disintegration takes place.

THE subject of wear itself is beset by many complications arising from the fact that the subject, at best, is largely statistical. It is accepted generally among authorities that there is no such thing as a perfect sliding contact, in which all sliding phenomena are continuous and uniform. Only grossly macroscopic data appear to be continuous, while data approaching microscopic proportions reveal discontinuities that are more or less periodic. It is conceded generally that a sliding contact consists of a number of minute point contacts, whose behavior is extremely individualistic. All of the characteristics of the macroscopic contact originate at these minute points; and it is logical, therefore, to infer that extreme conditions, such as excessive pressures, excessive current densities, excessive temperatures, and others, exist at these points. It is exceedingly difficult, therefore, to obtain a true picture of a sliding contact by means of the macroscopic data that are available to most experimenters and engineers. This paper is intended as an aid in the efforts to study sliding-contact materials by means of these macroscopic data.

Further complications arise when the term *wear* is applied to the undesirable destruction of all surfaces, whatever the manner in which the destruction may occur. The wear of shoe soles and heels, the wear of piston rings and cylinder walls, the wear of bearings, the wear of cutting edges, and many other types of wear, each involves a peculiar and, perhaps, unique combination of forces that provide the mechanism of wear. Therefore, when discussing wear, it is necessary to specify the particular kind that is being considered as well as the multitude of conditions that influence it.

This paper is intended to include only the type of wear that is encountered in the use of continuously sliding contacts, particularly the end wear of brushes riding on collector rings. Even in this simple case, however, it is impossible to comprehend the true mechanism of the wear of a sliding contact in all its details. As evidence of the great complexities that exist in a sliding contact, an abbreviated chart of the known variables that enter into the phenomenon of the wear of electrical brushes is shown in Figure 1. It is beyond the scope of this paper to discuss the part that each variable has in the whole process of brush wear, but it is not difficult to conceive by means of this chart the truth that the whole process is highly involved and cannot be disentangled very easily, if at all.

Comparative

Nevertheless, it is desirable that electrical brushes be compared on the basis of relative wearing qualities as well as on the bases of contact resistance, current-carrying capacity, and other performance characteristics. V. P. Hessler has made a successful attempt within limitations to study the effects of various operating conditions upon electrical-brush wear as well as on brush contact drop.[1–3] He considered chiefly the effects of current density, ring speed, brush pressure, and relative humidity. That material has been distinctly useful in advancing toward a better understanding of basic relationships.

However, further progress could be made if a concept of wearing quality were developed that would apply strictly to the brush and not include the properties of the external system of which the brush is a part. No fair comparison can be made of brushes at the present time without testing all of them on the same test equipment and under identical conditions. Since many different grades of brushes are made for use under widely different conditions, testing them under like conditions is not always useful. Even though they be tested under like conditions, there is yet the problem of distinguishing between the effect of the conditions imposed on the interface by the whole sliding system, including the brush, and that of the structural properties of the contact faces. It is the intended purpose of this paper to develop a means whereby this distinction between the conditions responsible for producing wear and those responsible for resisting wear can be made. The latter conditions shall be implied in this paper by the term *wear resistance*.

The wear of a contact face is caused by the frictional forces that are present when sliding occurs. The structural characteristics of the contact specimen determine the resistance of the latter to wear when sliding occurs. Thus, the numerous variables of wear as evidenced by Figure 1 may be classified into two important groups—those which affect the frictional forces in one way or another, and those which affect the wear resistance of the contact specimen. The first group is associated with the whole sliding system while the second group concerns only the contact specimen itself. Of course, a particular variable may belong in both groups, as, for example, brush current. It is the means for measuring the effects of the second group on wear that are to be considered.

There is a philosophical truism that

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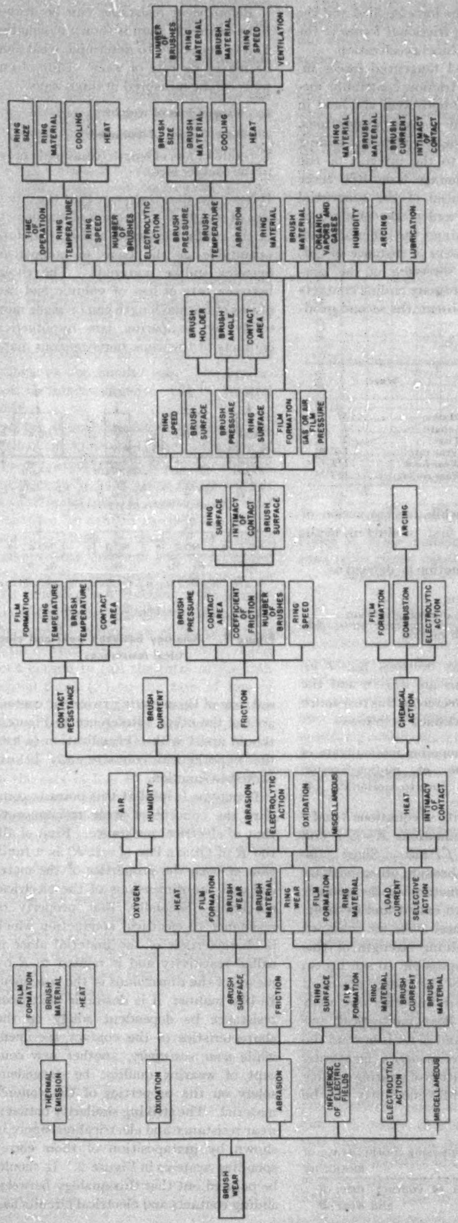
NUMBER OF BRUSHES
RING MATERIAL
BRUSH MATERIAL
RING SPEED
VENTILATION
RING SIZE
RING MATERIAL
COOLING
HEAT
BRUSH SIZE
BRUSH MATERIAL
COOLING
HEAT
RING MATERIAL
BRUSH MATERIAL
BRUSH CURRENT
INTIMACY OF CONTACT
TIME OF OPERATION
RING TEMPERATURE
RING SPEED
NUMBER OF BRUSHES
ELECTROLYTIC ACTION
BRUSH PRESSURE
BRUSH TEMPERATURE
ABRASION
RING MATERIAL
BRUSH MATERIAL
ORGANIC VAPORS AND GASES
HUMIDITY
ARCING
LUBRICATION
BRUSH HOLDER
BRUSH ANGLE
CONTACT AREA
RING SPEED
BRUSH SURFACE
BRUSH PRESSURE
RING SURFACE
FILM FORMATION
GAS OR AIR FILM PRESSURE
RING SURFACE
INTIMACY OF CONTACT
BRUSH SURFACE
ARCING
FILM FORMATION
RING TEMPERATURE
BRUSH TEMPERATURE
CONTACT AREA
BRUSH PRESSURE
CONTACT AREA
COEFFICIENT OF FRICTION
NUMBER OF BRUSHES
RING SPEED
FILM FORMATION
COMBUSTION
ELECTROLYTIC ACTION
CONTACT RESISTANCE
BRUSH CURRENT
FRICTION
CHEMICAL ACTION
AIR
HUMIDITY
ABRASION
ELECTROLYTIC ACTION
OXIDATION
MISCELLANEOUS
HEAT
INTIMACY OF CONTACT
OXYGEN
HEAT
FILM FORMATION
BRUSH WEAR
BRUSH MATERIAL
RING WEAR
FILM FORMATION
RING MATERIAL
LOAD CURRENT
SELECTIVE ACTION
FILM FORMATION
BRUSH MATERIAL
HEAT
BRUSH SURFACE
FRICTION
RING SURFACE
FILM FORMATION
RING MATERIAL
BRUSH CURRENT
BRUSH MATERIAL
THERMAL EMISSION
OXIDATION
ABRASION
INFLUENCE OF ELECTRIC FIELDS
ELECTROLYTIC ACTION
MISCELLANEOUS
BRUSH WEAR

Figure 1. Elements of electrical-brush wear

asserts that the magnitude of any phenomenon is directly proportional to the magnitude of its cause and inversely proportional to the magnitude of that which resists or impedes the occurrence of the phemonenon. This truism is the basis for Ohm's law in the electrical system and for the analogous relationships in the magnetic and mechanical systems, respectively. It is proposed that it be applied to the phenomenon of wear. Since frictional force is the direct cause of wear, and the amount of wear is best measured as a time rate, it is suggested that that property whereby wear is resisted be defined so that the following relation will hold:

$$\text{Rate of wear} = \frac{\text{frictional force}}{\text{wear resistance}} \tag{1}$$

It is believed that this equation represents a relationship that is just as fundamental and just as basic as is Ohm's law. In the light of this belief the efforts found in the literature to date to define wear resistance will be considered next.

It is interesting to note that the subject of wear has been studied from many different viewpoints, particularly in the field of metallurgy, and that many ingenious machines and other devices have been designed and built to test the wearing qualities of a variety of materials under controlled conditions that were intended to simulate actual working conditions. Only two papers have been found in the literature to date in which an attempt is made to define wear resistance by a mathematical expression. Neither of these expressions excludes wholly the properties of the external system. The first expression is defined by R. B. Freeman[4] as "unit wear," which he set equal to the expression obtained by "dividing the loss of weight experienced by the specimen during the run by the projected area of contact and the length of travel." This unit wear is measured in grams per square inch per foot and is expressed dimensionally as $ML^{-3}T^{-3}$. The chief disadvantage to this concept of unit wear is that it is a function of the density of the contact material being studied. Thus, if two specimens of differing densities should be tested under identical conditions and found to have the same volume of wear, the preceding expression would show the heavier specimen as having greater unit wear. It might be suggested that this definition be revised by substituting the loss of volume for the loss of weight. Such a ratio would then be equivalent to the ratio of length of end wear to the relative distance traveled by the contact faces and would be dimensionless. Both definitions, however, are functions of the coefficient of friction and, therefore, do not provide a true comparison between different specimens for even the same contact area and contact travel. Thus, if one brush wears twice as much as another for the same ring travel and brush size, its unit of wear would be double that of the other, regardless of unequal surface conditions and other unlike conditions that influence the coefficient of friction.

The second expression that was found for wearing quality is defined by D. S. Clark and R. B. Freeman[5] as the energy required to overcome resistance to motion per unit volume of wear and is expressed dimensionally as $ML^{-1}T^{-2}$. The energy required to overcome resistance to motion, however, is a function of the coefficient of sliding friction as well as other properties that depend on the whole sliding system. The concepts of resistance to motion and of the energy required to overcome this resistance are treated later in this paper.

Table I

Electrical		Mechanical		Wear	
Inductance	L	Mass	M	Mass	M
Charge	Q	Length	L	Length	L
Time	T	Time	T	Time	T
Current	QT^{-1}	Velocity	LT^{-1}	Wear rate	LT^{-1}
Voltage	LQT^{-2}	Force	MLT^{-2}	Wear force	MLT^{-2}
Resistance	LT^{-1}	Resistance to motion	MT^{-1}	Wear resistance	MT^{-1}

Analytical

The frictional forces available for measurement are the tangential and normal forces, respectively, and the work done by (or against) each is the product of that force by the component of the relative motion of the contact member in the same (or opposite) direction of that force. Whenever force is applied to produce relative motion, the discrepancy between the applied force and the reactive forces due to the acceleration of the body is determined by frictional forces that produce heat and wear.

This statement may be represented by the following equation:

$$F = m\frac{dv}{dt} + K_d v \qquad (2)$$

where F is the applied force, m is the mass of the system to which the force is applied, v is the velocity of the system, and K_d is the resistance to motion.

This may be illustrated by the motion of a streetcar on tangent level track during the accelerating period with constant torque output from the motor. The difference between the force applied and the summation of the frictional forces is the net force that produces acceleration.

The normal and tangential forces involved in sliding friction contribute mechanical energy to both wear and heat in unknown and quite variable proportions. The total energy delivered to the sliding contact is equal to the integral of the product of the average tangential force times the differential of the tangential distance traveled and of the product of the normal force times the differential of the depth of the wear or distance of cut, measured in the direction of the normal force. For ordinary sliding contacts where gouging is absent, the second product is negligible, while for the action of cutting tools the first product is negligible.

If resistance to motion be defined as

$$\text{Resistance to motion} = \frac{\text{frictional force opposing motion}}{\text{rate of motion}} \qquad (3)$$

(or simplified this becomes $K_d = F/v$), then its dimensions are MT^{-1}, and the energy needed to overcome this resistance to motion may be defined as follows:

$$\int(\text{frictional force opposing motion})(\text{rate of motion})dt = \int(\text{rate of motion})^2(\text{resistance to motion})dt \qquad (4)$$

Note the similarity of equations 3 and 4 to the electrical equations $R = E/I$ and energy $= \int EIdt = \int I^2Rdt$. Since the process of wear is brought about both by weakening the structure of the contact face to an unknown extent and by overcoming through sheer force an unknown part of the remaining strength of this structure, it is impossible to set up an exact expression for the energy absorbed by wear. To state it in another way, wear energy consists of heat, mechanical, and chemical energies and so far is beyond the reach of the experimenter's measuring devices. The definition of wearing quality given by Clark and Freeman may now be expressed as

$$\frac{\int(\text{frictional force opposing motion})(\text{rate of motion})dt}{\int(\text{projected area of contact})(\text{rate of end wear})dt}$$

Before wear resistance can be defined more explicitly than is shown in equation 1, it is necessary to determine what shall be meant by rate of wear. Rate of end wear can be measured in three ways:

1. Rate of loss of weight.
2. Rate of loss of volume.
3. Rate of loss of length (measured normal to the contact face).

It has been shown already that the rate of loss of weight is a function of the density of the contact material and is, therefore, unsuitable as the basis of comparison between unlike materials. The choice between rate of loss of volume and rate of loss of normal length can be made more easily by comparing two hypothetical contacts of the same homogeneous material, one of them having twice the contact area of the other. Reference to Figure 2 should assist with a visualization of how the hypothetical contacts may be expected to function.

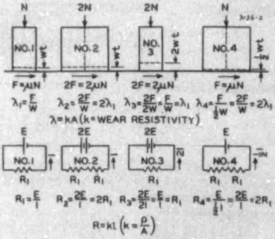
N
2N
2N
N
NO.1
NO.2
NO.3
NO.4
wt
wt
2wt
wt
$\frac{1}{2}$W
F=μN
2F=2μN
2F=2μN
F=μN
$\lambda_1=\frac{F}{W}$
$\lambda_2=\frac{2F}{W}=2\lambda_1$
$\lambda_3=\frac{2F}{2W}=\frac{F}{W}=\lambda_1$
$\lambda_4=\frac{F}{\frac{1}{2}W}=\frac{2F}{W}=2\lambda_1$
λ=kA(k=WEAR RESISTIVITY)
E
2E
2E
E
R₁
R₁ R₁
R₁
R₁ R₁
$R_1=\frac{E}{I}$
$R_2=\frac{2E}{I}=2R_1$
$R_3=\frac{2E}{2I}=\frac{E}{I}=R_1$
$R_4=\frac{E}{\frac{1}{2}I}=\frac{2E}{I}=2R_1$
$R=kl\left(k=\frac{\rho}{A}\right)$

Figure 2. Analogy between wear and electrical resistances

It may be helpful at this point to compare the concept of wear resistance to that of electrical resistance. First of all, the R of Ohm's law ($I = E/R$) is a function of both the properties of the material and the dimensions of the electrical specimen. Secondly, that property of resistance to electrical conduction which is characteristic of the material alone is called resistivity and is related to R by means of the dimensions of the specimen. In like manner, it is desirable that wear resistance be dependent solely on the characteristics of the contact specimen, while *wear resistivity*, another new concept of wearing quality, be dependent solely on the properties of the contact material. The striking similarity between wear resistance and electrical resistance is shown by juxtaposition of their corresponding systems in Figure 2. It should be pointed out that this analogy between sliding contacts and electrical circuits has the same limitation that all analogies have—that it can be carried too far, yielding some absurd results.

The requirement of identical conditions for these two hypothetical contacts implies that both of them must have the same normal pressure (say, pounds per square inch) and, hence, that the larger contact have twice the normal force of the smaller. Since both contacts are of the same material, their coefficients of friction (μ) may be assumed equal; therefore, the tangential force of the larger contact will be twice that of the smaller contact. Since the larger contact may be considered as two contacts like the smaller one fastened together, it may be assumed that both hypothetical contacts will have the same depth of end wear. Accordingly, the larger contact will lose twice as much volume as the smaller one. The limitation of this assumption will be treated later.

If the wear resistance be defined as the ratio of the tangential force to the rate of loss of normal length, the larger contact will appear to have twice the resistance of the smaller one. If, however, the wear resistance be defined as the ratio of the tangential force to the rate of loss of volume, both contacts will be indicated as having the same wear resistance. The first definition is a function of the properties of the whole contact specimen, while the second is a function of the properties of the contact material. Therefore, it has been chosen to call the ratio of the tangential force (F) to the rate of loss of normal length (W) the wear resistance (λ), whose dimensions are now MT^{-1}. Likewise, wear resistivity (k) is to be defined as the ratio of the tangential force to the rate of loss of volume (WA) and is expressed dimensionally as $ML^{-2}T^{-1}$. Therefore, it follows that

$$\text{Wear resistance} = (\text{wear resistivity})\ (\text{contact area}) \qquad (5)$$

This can be clarified readily by reference to the four cases illustrated in Figure 2, in which the expressions for wear resistance are given. The expressions for wear resistivity for these four cases are, respectively, as follows:

$$k_1 = \frac{F}{WA} = \frac{\lambda_1}{A}$$

$$k_2 = \frac{2F}{W \cdot 2A} = k_1 = \frac{\lambda_2}{2A}$$

$$k_3 = \frac{2F}{2W \cdot A} = k_1 = \frac{\lambda_3}{A}$$

$$k_4 = \frac{F}{(W/2)2A} = k_1 = \frac{\lambda_4}{2A}$$

The analogy to a parallel-resistance electric circuit is a multiple sliding-contact system and will not be treated in this paper.

It is proper at this point to consider the limitations of the hypothetical contact when applied to actual cases. It being assumed that the foregoing developments are logical, it is considered perfectly valid to let these concepts of wear resistance and wear resistivity absorb all of the variations in the physical properties of the contact member just as electrical resistance and electrical resistivity do for the case of electric conduction through any conductor. Thus, just as R under operating conditions is equal to

$$\frac{\rho_0 l}{A} \times K(\text{temperature}) \times K(\text{stranding}) \times K(\text{skin effect}) \times K(\text{etc.})$$

so wear resistance may be set equal to

$$(\text{Wear resistivity})_0 \times (\text{contact area}) \times K(\text{contact temperature}) \times K(\text{contact dimensions}) \times K(\text{contact pressure}) \times K(\text{sliding system}) \times K(\text{ambient conditions}) \times K(\text{etc.})$$

It should be pointed out that the adaptation coefficients for wear resistance as already given are not as simple as are those for the electric circuit. In the latter case these coefficients are both fairly simple and quite dependable. For wear phenomena, however, these coefficients are extremely variable and interdependent. A study of Figure 1 will verify this statement to a large extent.

The reciprocal of wear resistance, which may be called *wear susceptance*, has been found, in the case of electrical brushes, to be more easily understood than wear resistance. Since wear is the result of friction, it is more intelligible to think of the rate of wear per unit of friction than of the amount of friction per unit of wear rate. Likewise, the reciprocal of wear resistivity, which may be called *wear susceptibility*, is more useful than is wear resistivity.

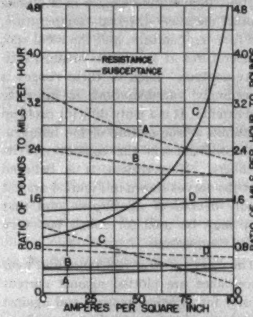

Figure 3. Wear resistance and wear susceptance versus current density for metal-graphite brushes—types *A, B, C,* and *D*

Experimental

The curves shown in Figures 3 and 4 for four kinds of metal-graphite brushes were calculated from experimental data obtained at the Iowa Engineering Experiment Station and represent the average of repeated tests at each current density between 0 and 100 amperes per square inch at intervals of 20 each. The current used was 60-cycle alternating current. The measured data included frictional torque of the test brushes, their wear, and the spot temperature in a representative brush of each group tested. The wear is measured in mils and the wear rate in mils per hour.

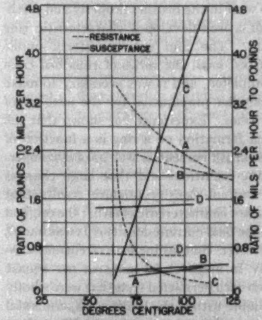

Figure 4. Wear resistance and wear susceptance versus spot temperature for metal-graphite brushes—types *A, B, C,* and *D*

The test equipment consisted of two units of similar construction, each having a slip ring of standard material driven by a separate motor. Each slip ring, together with its brushes, was enclosed so as to permit the control of atmospheric conditions. Eight brushes were placed radially on each ring with a normal pressure of three pounds per square inch of contact area and were tested at an average speed of 4,200 feet per minute.

The duration of each test run was the same, and data were recorded at regular intervals throughout each run. All values except brush-wear data used in the calculations of the curves of Figures 3 and 4 represent the averages of these recorded data for a set of runs at each current density except for that obtained during the first hour of each run. This first hour's data were influenced by transient conditions that accompanied the start of every test run, but the rest of the data for each run were, for the most part, steady state. Both test units, which had previously been compared and checked for similarity of results on like brushes, were operated simultaneously in all runs. The brushes were tested as follows:

Types *A* and *B* simultaneously on units 1 and 2, respectively.

Types *C* and *D* simultaneously on units 1 and 2, respectively.

Each series of tests was preceded by a process of stoning and sanding followed by an operating period of sufficient length to wear in the brushes properly and establish a well-developed characteristic ring film. The film was henceforth undisturbed until the series of runs had been completed, and it was necessary to make a similar preparation for the next set of brushes. Experience has shown that the film formation on a ring is a function of the brush current as well as of other operating conditions and that large changes of current density produce results that are not characteristic of either the original or the final current density. It was found, however, that an increase of current density in steps of 20 amperes per square inch each permitted tests that were wholly reliable within the limits of experimental error. Several runs were conducted at each current density before proceeding to the next.

The frictional force, which is measured in pounds, was obtained by converting the electrical input to the driving motor into the torque required by the brushes. The efficiency data required for these conversions were revised for every test run, so as to prevent, insofar as possible, the errors due to the variable losses of the test equipment.

The brush-wear data were obtained by making four micrometer measurements, each to the nearest 0.0001 inch, on each of the eight brushes, before and after each run. The difference between successive readings for each of the 32 points helped to determine the average brush wear for that set of brushes.

The spot temperature was measured by a thermocouple junction placed in a hole drilled in the top of one brush in the group being tested. It has been found that consistent use of the same point in all tests gives approximately the same comparative results as a group of thermocouples placed at various accessible points on a brush. This temperature, which is plotted as abscissa in Figure 4, does not, however, represent the true temperature of the contact face. There are three important temperature ranges in a sliding contact member—the body temperature, the face temperature, and the local spot temperatures at the points of most intimate contact. Each of these ranges is exceedingly variable, both in intensity and extent. It is a truly difficult problem to obtain a true picture of the temperature conditions in the contact and its members. Nevertheless, it is believed that the curves shown in Figure 4 are not totally devoid of significance and that they may be used for rough but very useful comparison.

The values of wear resistance and wear susceptance are plotted against current density for practical reasons and against temperature for theoretical reasons. Practically, the similarity of the curves of wear susceptance and wear resistance in Figure 3 to those obtained for rate of brush wear and its reciprocal, brush life, respectively, indicates that friction alone was not responsible for the variations in wear rates. The phenomenon of wear is a function of both its cause, which is friction, and the binding strength of the material, which restrains it to a varying degree. The binding strength is, in turn, a function of the temperature of the brush material, weakening as the temperature increases. Since the brush temperature increases with an increase in current density, it is reasonable to expect the ratio of the wear to the friction to increase, likewise.

Theoretically, the curves of Figure 4 indicate the relative strength or weakness of the binding materials of the respective brushes for similar ranges of temperature. It is easy to recognize the superiority of the binding materials in brushes *A* and *B* and the great inferiority of the binder in brush *C*. It should be noted again that this ratio of wear to friction is almost completely independent of the external system and is almost wholly a property of the brush itself.

Conclusions

The development of concepts of wearing quality based on the truism that the magnitude of a phenomenon is proportional to its cause and inversely proportional to that which resists it has been given, and two of these concepts are shown for the actual cases of four kinds of metal-graphite brushes. Since electrical resistance, resistance to motion, and wear resistance are based on the above truism, it is no surprise that electrical, mechanical, and wear phenomena should be analogous to some extent. The table shown below illustrates the extent of the analogy for all three phenomena insofar as can be determined at present.

It is believed that these concepts, together with the chart of variables affecting brush wear shown herein, provide a more solid foundation for the furtherance of the study of wear of electrical brushes. It is recognized that a subject so inherently complex as an electrical sliding contact cannot be made simple. It is possible, however, to analyze the total phenomenon into its component parts and, where these component parts are also exceedingly complex, to analyze them further into simpler components, repeating the process until components that can be determined readily are obtained. The first step in this process of analysis has been suggested in this paper by the separation of the characteristics of the contact specimen itself from those of the outside system.

Apart from abstract analysis, however, wear resistance, wear susceptance, wear resistivity, and wear susceptibility are believed to provide new and important tools for the comparison of brushes or other materials used in sliding contacts.

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Parallel Operation of Airplane Alternators

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Synopsis: The increase in airplane electrical loads has made necessary an increase in the distribution voltage to avoid excessive copper weight. Difficulties in the use of higher d-c voltages are mentioned, and past experience with a 400-cycle three-phase system is outlined. In the light of this experience and of new developments in progress, requirements are outlined for a larger 400-cycle system with multiple generating units operating in parallel.

ELECTRICAL loads on military airplanes have increased tremendously in the last three years because of the substitution of electric power for hydraulic power to drive auxiliaries, or the use of motor-driven instead of engine-driven hydraulic pumps, and because of the development of new flight and combat devices.

The Trend Toward Higher Voltage

First in use because of its simplicity, the d-c system progressed through the 6- and 12-volt stages until, in 1939, new airplanes were designed with a 24-volt system.[1] As the loads increased, the weight of wiring required for the lower-voltage systems became prohibitive. Now, in 1943, the economical limit of the 24-volt system has been exceeded, with new airplanes demanding 1,600- to 2,000-ampere peak loads. A further increase in voltage is therefore necessary. Because of the continued upward trend of the load-demand curve, and because of the increasing transmission distances in the larger airplanes, the next step will be above 100 volts.

Unfortunately a change to 115 volts direct current on high-altitude airplanes introduces at least one problem to which a completely satisfactory answer has not yet been found. This is the difficulty of arc rupturing in the switching equipment at present and contemplated flight-altitude maxima for combat airplanes. Fault currents may be higher than for a-c equipment of equivalent rating. On small motors at this voltage, commutation at high altitudes is another problem. Partly for these reasons, the United States Army Air Force has selected a 208/120-volt 400-cycle three-phase four-wire system for the large combat airplanes now under development.

The XB-19 Installation

Early experiments by the Air Corps with a-c auxiliary power on the *XB*-15 resulted in procurement of auxiliary-engine-driven 400-cycle three-phase power plants for the *XB*-19. Figure 1 is a schematic view showing the circuit arrangement. The design of the engine and alternator was co-ordinated to result in the package unit shown in Figure 2. The pancake proportions of the alternator do not give the optimum weight of electrical parts, but, in conjunction with the aluminum fan, provide the necessary flywheel effect for the engine. The use of 14 poles gives a synchronous speed of 3,430 rpm. The short-circuit ratio is 0.60, a figure which may seem low to a designer of 60-cycle machines, but which has proved satisfactory in operation. Heavy dampers with continuous end rings limit oscillation caused by engine-torque pulsation. They also help support the voltage until the regulator responds to a load increment. With a self-excited system, this materially assists in prompt recovery of voltage if the regulator is fast enough to take advantage of it.

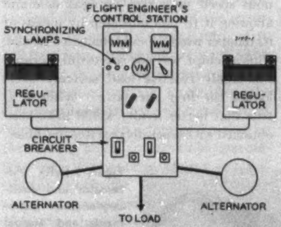

Figure 1. Schematic view of the auxiliary power plants used on the *XB*-19 airplane

The design rating of the alternator is 120 volts, 12.5 kva, 0.8 power factor, although, to conform to the Air Corps specification, the name-plate rating is 10 kva, 0.75 power factor. As the system is operated ungrounded, the neutral is not brought out.

The rotating field of the alternator is designed for overhung mounting on the engine crankshaft, which makes the use of a d-c exciter difficult. Chiefly for this reason, it was decided to use self-excitation. An electronic voltage regulator, using *RO*-585 diodes to control a three-phase half-wave thyratron rectifier, provides the fast-acting field supply so necessary in a self-excited system.

The *RO*-585 filaments are operated from a positive-phase-sequence network which delivers a single-phase voltage proportional to the average of the root-mean-square values of the three line voltages. As the regulating action is a function of the filament temperature in these tubes, the regulator is responsive to the average of the three root-mean-square phase voltages. An incidental advantage in using the positive-sequence network is the accomplishment of excellent temperature compensation by using in the resistance element of the network a material having a positive temperature coefficient of resistance.[2]

The flight engineer's panel, the installation of which is shown in Figure 3, contains the instrumentation and manual controls. The panel compartment houses the voltage build-up and reactive-load-equalizer circuits, the wattmeter current transformers, and the manual circuit breakers. Voltage is built up initially from an alternator residual voltage of approximately nine volts by means of a copper-oxide rectifier and a small multicontact Silverstat voltage regulator, both of which are intermittently rated to supply no-load excitation. A small high-reactance step-up transformer overcomes the initially high resistance of the rectifier without resulting in excessive excitation at normal voltage.[3] By this means the voltage overswing is reduced. For each alternator a three-position multicontact manual cam switch controls the starting sequence. This switch must be in the off position before the auxiliary-engine-starter circuit can be completed. With the engine at normal speed, the switch may be turned to the warm-up position, which allows the voltage to build up and be maintained under the control of the auxiliary regulator. After 45 seconds a thermal relay energizes a tripping magnet on the cam switch, automatically advancing the switch to the run position, although it may be returned to the off position at any time. The two cam switches are mutually interlocked to prevent an attempt to build up both alter-

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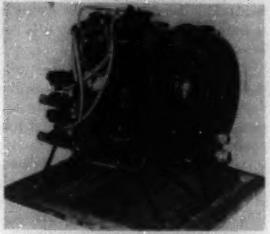


Figure 2. The 12.5-kva alternator assembled on the Ruckstell-Burkhardt auxiliary engine

nators at the same time, as only one build-up circuit is provided. As soon as one alternator is transferred to electronic regulation, it may be connected to the load bus by closing its manual circuit breaker, although, particularly at low ambient temperature, it is desirable to operate at no load for a few minutes to allow the thyratrons to reach normal operating temperature.

Operating Experience on the XB-19

With no load on the first alternator, and with the governors of the two engines closely matched, it has been found fairly easy to synchronize manually at full voltage on every attempt when the synchronizing lamps are flashing not faster than three cycles per second. At rates of slip up to six or eight cycles per second, it is possible to synchronize on approximately 75 per cent of the attempts with random closure of the breaker. With more than two or three kilowatts of load on one alternator, it is necessary to close while the lamps are dim or dark. Test experience with these power plants indicated the necessity of manual control of the engine governors, and this feature was added when the equipment was installed in the airplane. After some practice, and with the engines in smooth running condition, it was then possible for the operator to synchronize with full load on one alternator, although he would occasionally miss. When the engines have been run for a number of hours after an overhaul, momentary speed changes during attempts to synchronize make it difficult to do so with more than six kilowatts of load.

During the development stage, tests were made to determine if it were practical to synchronize by bringing the incoming alternator up to approximately synchronous speed with zero or weak field and allowing it to pull into step by means of its damper winding and reluctance torque. This attempt was not successful, because the highly reactive load severely overloaded the first alternator. While it is true that better results would have been obtained with separately excited machines, the shock would still be prohibitive when attempting to synchronize under load.

When paralleled units are driven by auxiliary engines, it is necessary to cut off promptly an alternator whose engine has failed to carry load. Magneto trouble, fouled plugs, and vapor lock, or other fuel-system faults, will occasionally cause a short-time or sustained power failure. Any tripping means for isolating such a unit should have its action delayed sufficiently to prevent operation on a momentary misfire which can be sustained by the inertia of the machine. On this equipment, protection is afforded by a reverse-power contact on the wattmeter which actuates a shunt trip magnet on the circuit breaker. This contact is set at three kilowatts, and the damping of the instrument gives the necessary delay. A small pilot relay inside the instrument case relieves the pointer contact of the tripping current.

Experience with the system has demonstrated the desirability of a uniform response rate of the several governors. If the rates differ materially, one engine tries to drive the other on the sudden removal of a large load, and faulty tripping of the reverse-power device may result. A particularly bad performer is a governor which has sluggish response in correcting overspeed. Although the condition was reproduced during preliminary testing, no serious trouble has been experienced in service operation.

Experience with the scheme for automatically building up the alternator from its residual voltage has been generally good but subject to occasional failure caused by a reduction of normal residual flux. This usually follows a shutdown caused by loss of voltage resulting from a highly inductive overload, and may occur after an engine failure has transferred all

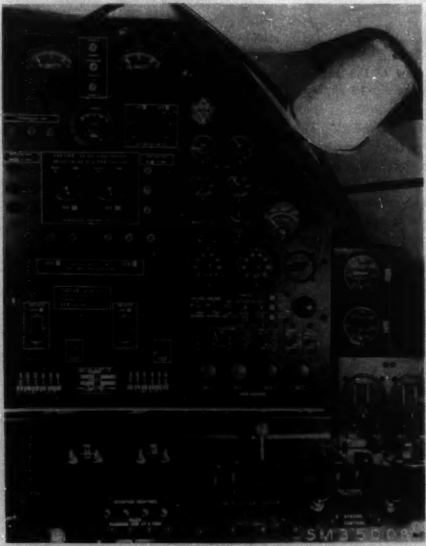


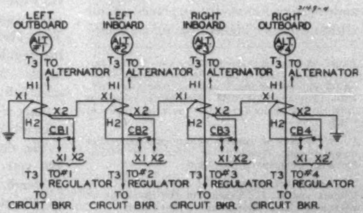
Figure 3 (left). Alternator and auxiliary-engine instruments and manual controls at the flight engineer's position of the *XB-19* airplane

The two plunger knobs in the lower right-hand corner of the picture permit manual control of engine speed while synchronizing

Photo courtesy of Douglas Aircraft Company

Figure 4 (below). Standardized current-transformer interconnections for feeding the reactive load-equalizer networks of the voltage regulators

Agreement on polarities is necessary to permit interchanging regulators of different manufacture



of the load to the remaining alternator. It may also be discovered after the flight engineer has mistakenly shut down an engine without first opening the alternator circuit breaker. To overcome this condition, a circuit has been added which permits the operator to "flash" the field with battery voltage if necessary.

Requirements for a Modern A-C Installation

Having examined an existing a-c installation, we may proceed to lay down the requirements of an airplane power system which will take advantage of the experience gained and the newer developments which are available. It cannot well be denied that a successful system must involve parallel operation of a number of generating units. It is not acceptable that the loss of a single unit can shut down the whole system. The loss of one unit out of two is nearly as bad, unless the essential load is within the rating of one generating unit. This condition might exist in a noncombat airplane, but the greater hazards of combat operation dictate the use of at least three and preferably four units where the size of the airplane permits. Nonparalleled or isolated operation of the several generating units is a makeshift which does not use to the full the power capabilities of the system in starting heavy motor loads and self-clearing of faults; that is, the load factor with separate units is poor. Loss of a generating unit results in a delay before the load can be reapportioned among the other units.

The ability to parallel alternators depends first on the characteristics of the prime movers. The earlier a-c aircraft systems used auxiliary gasoline engines governed to run at essentially constant speed. The governors are designed to droop the speed slightly as load increases, which characteristic automatically gives acceptable division of kilowatt load between units. As the capacity of the prime mover is comparable to that of the alternator, and the inertia is relatively low, no difficulty has been experienced in maintaining synchronism under all conditions, including load transients. In the *XB*-19 equipment, the use of low-resistance damper windings effectively prevents hunting caused by engine-torque pulsations. Whereas the use of auxiliary engine drive may be satisfactory for medium-altitude transport and cargo airplanes, at the present time it seems to be impractical to supercharge the smaller engines to a critical altitude sufficiently high to permit their use on high-altitude bombers.

Constant-Speed Drives

Until recently, no suitable means was available for driving alternators from the main engines and at the same time permitting parallel operation. Now several so-called "constant-speed" drives are under intensive development and promise to have the necessary performance. The input-output speed ratio is continuously variable, so that a constant output speed may be maintained over a wide range of input speed. This development gives the a-c electric-system prime movers of proved reliability and high-altitude performance.

A number of difficulties must be overcome in adapting constant-speed drives to this application. Because of the large size of the main engine in proportion to the rating of the alternator, the engine is a rock-steady source whose speed is practically unaffected by alternator loading. On the other hand the engine speed may suddenly be changed because of flight requirements; yet the alternator should not perceive this change. As a result, the drive or its governor must incorporate the load-dividing feature, and the governor must be quick in coming into action and be able to change the drive ratio at a rate faster than the engine can accelerate. In a speed-converting mechanism the difference between the input and output torques appears as a torque reaction on the housing, and it appears to be unavoidable that this reaction torque is transmitted principally through the ratio-changing mechanism. Consequently at high ratios the ratio-changing device must develop considerable power to alter the ratio.

The necessary rate of change of ratio is determined by the maximum possible rate of acceleration or deceleration of the engine. Information from one of the engine manufacturers states that an acceleration rate of 3,000 rpm per second may be found in normal operation, although it is considered detrimental to engine life, and that a rate of 3,500 rpm per second is possible under some combat conditions with hand control of the propeller pitch. The drive should be capable of responding at a rate somewhat faster than this to have a reasonable factor of safety. It seems probable that the engine deceleration rate will be appreciably less than 3,000 rpm per second, although no confirming data are known to the author.

Because of the rigidity of the prime movers, it is essential that the drives incorporate overrunning clutches to permit the alternators to remain in synchronism in spite of momentary deviations in drive ratios or failure of the drives to compensate promptly for rapid changes in engine speed. Partly for the same reason, it is desirable that the drive have some internal slip, either inherent or introduced by the use of a slip coupling such as a fluid flywheel.

Unless the drive has an inherent slip which is consistently reproducible and is independent of the ratio setting, it is necessary that the governor be made load-responsive to provide the speed droop which is required for division of kilowatt load between paralleled alternators. Methods are known for obtaining load division by controlling the relative phase position of the alternator rotors without producing a change in the steady-state speed, but they lack the simplicity and reliability of the speed-droop method. Though the governor may be the droop-determining means, it will have some delay in adjusting itself to a new load level. For this reason some inherent slip in the drive will assist in the maintenance of synchronism during sudden large load transients. Internal slip will also reduce the effect of engine and drive torque pulsations.

While the governor must have a rapid response rate, it must also be well damped to obtain system stability during load and fault transients. Keeping the force-inertia quotient of the ratio-changing device high will make the damping problem easier.

Alternator Design

Experience with the self-excited a-c system has demonstrated a degree of overload capacity and system stability inferior to that which may be expected with separate excitation. The load characteristic of the self-excited machine has a definite knee beyond which the voltage is not self-supporting, with the result that a suddenly applied heavy load may cause loss of voltage, whereas the same load gradually applied may be sustained. Synchronizing difficulty is increased, and the ability to burn minor faults clear or blow limiters is distinctly reduced. Alternators of current design have integral d-c exciters to overcome these handicaps.

Low-resistance damper windings are necessary to ensure freedom from oscillation excited by driving torque pulsation or load transients. Some assistance is given in synchronizing by the full-voltage method because of the damping effect on oscillations caused by closing the circuit breaker at inexact synchronous speed and phase position. When synchronizing is performed by the synchronous motor method, that is, bringing the incoming machine up to approximate speed with no field or with weak field and closing the breaker, low-resistance damper windings are essential, and some benefit is obtained by using continuous end rings. The latter feature will also increase the ability of the alternator to pull back into step automatically under some conditions of out-of-step operation.

It has been suggested that aircraft alternators should be designed with a short-circuit ratio around unity because 60-cycle practice has shown that this gives high inherent stability which will be a benefit in parallel operation and in starting heavy motor loads. The suggestion has merit, but unfortunately it results in a heavy machine. Much of the benefit may be obtained with lighter over-all weight by increasing the capacity of the exciter to supply heavy excitation when needed and by making the regulator as fast as possible. Satisfactory experience with self-excited alternators having a short-circuit ratio of 0.60 discounts the necessity of accepting the higher weight of the design which has a higher ratio.

The Voltage Regulator

The use of an exciter brings the regulator duty within the capacity of existing designs of aircraft d-c regulators which have undergone extensive development and combat service during the past two years. What modifications are necessary can readily be made to adapt them to the requirements of the a-c system. If the operating coil is supplied from a three-phase dry-plate rectifier, the regulator will respond to the average of the three average phase voltages. The fact that this is an average rather than a root-mean-square response is not expected to be objectionable with alternators whose harmonic voltage output is within the limits set by the Army Air Forces tentative specifications. The tentative specification for the voltage regulators recognizes the necessity for this average response by stating that the voltage will be checked on type test with a rectifier-type voltmeter calibrated to read root-mean-square values of sine-wave voltages.

The regulator includes a network designed for use in a series-type differential reactive load-equalizer circuit for causing the regulators to equalize the circulating reactive current between alternators operated in parallel. The network is connected to a 125/1-ratio current transformer in the *T*3 lead from the alternator. Zero-power-factor lagging current from the transformer develops across the network a voltage which is in phase and in series with the line voltage applied to the regulator-coil-circuit rectifier. Since this increases the voltage applied to the regulator coil, the regulator acts to decrease the alternator excitation. Conversely, the presence of zero-power-factor leading current will cause the regulator to increase the excitation. Inphase line current produces a network voltage at a right angle to the voltage applied to the regulator and therefore does not affect the operation. Figure 4 illustrates the differential equalizer circuit which has been selected as standard by the interested manufacturers to ensure interchangeability, and this has been incorporated in the tentative specification by the Army Air Forces. With this connection, if the line currents of the respective alternators are equal in magnitude and phase angle, the transformer secondary current circulates solely through the secondary circuits and produces no voltage drop across any of the regulator networks. If one machine delivers more than its share of current, the secondary difference current divides proportionately in the parallel impedance paths formed by the regulator network of the overloaded machine as one branch and the other regulator networks in series as the other branch. With an excess of lagging current delivered by the first alternator, the phase angle of the network voltages in the two branches is such that the excitation of the overloaded machine is reduced and that of the others is increased, with the result that the reactive load balance is restored without affecting the voltage of the system. This lack of effect on the system voltage while correcting a reactive-load unbalance is the advantage which the differential method holds over the somewhat simpler scheme in which balance is achieved by drooping the voltage of each alternator in proportion to the lagging reactive component of current which the machine is delivering, thus producing a droop in system voltage. The differential scheme is identical in principle with the load-equalizing method which is used in present aircraft d-c systems. An improvement over the d-c circuit is the use of a normally closed interlock on the alternator circuit breaker to short-circuit the secondary winding of its associated current transformer when the breaker is open, and so avoid a reaction of the idle alternator circuit upon the regulated voltage of the other loaded machines. To be effective always, this interlock should be designed with the knowledge that it must effectively short-circuit a low voltage. Proper choice of contact materials and contact pressure and the use of several contact points in parallel will avoid trouble caused by contact film and dirt.

Special antihunting or damping means are necessary in the a-c voltage regulator because of the additional field circuit delay which is introduced by the use of an exciter. A very effective method uses a transformer to introduce into the d-c circuit of the regulator coil a voltage proportional to the rate of change of exciter voltage or the rate of change of alternator field current. When introduced with the proper polarity, this voltage causes the regulator coil to anticipate the response of the line voltage, which effect tends to damp out a tendency to hunt or overshoot.[4]

Choosing a Synchronizing Method

The choice of a synchronizing method is somewhat dependent on the prime movers which are used. If the system is driven solely by constant-speed auxiliary engines, a manual or semiautomatic method may be acceptable, because synchronizing is performed infrequently and at a time when the flight engineer is less occupied with other duties. It may be permissible to drop all or part of the load before synchronizing. With main-engine drive, on the other hand, fully automatic synchronizing is definitely desirable, because the availability of electric power is dependent on engine speed conditions, which in turn are determined primarily by warm-up, ground-maneuvering, and flight requirements. When the several alternators come up to speed, the flight engineer may have other duties requiring his attention. With automatic synchronizing, each alternator becomes available for loading as soon as it reaches synchronous speed.

Automatic Synchronizing

Automatic synchronizing equipment has been used in unattended substations and small power stations for many years with excellent service records. For aircraft use the same principles can be applied, but many refinements may be eliminated because of the presence of supervision and the necessity of saving weight. The essential features may be stripped down to means for performing the following functions:

1. Determine when the alternator frequency has approached normal, and initiate the succeeding sequence.
2. Indicate whether the bus is dead or already energized. If it is dead, initiate circuit-breaker closure; or if it is alive,

initiate the synchronizing sequence as follows:

3. Act on the incoming governor to adjust the speed until the difference frequency approaches zero.

4. Close the circuit breaker when the difference frequency is less than about two cycles per second and the alternator and bus voltages are closely in phase.

5. Advance the governor to the normal load setting.

6. Open the circuit breaker at any time when the system frequency drops below the acceptable minimum.

Automatic synchronizing by this full-voltage method may be accomplished with a load on the system, because the human elements of indecision and reaction time are eliminated from the operation. Nevertheless manual controls are provided so that the flight engineer may select the units to be paralleled, or he may trip any alternator circuit breaker at any time. After a machine has been connected to the bus, the flight engineer may, at his leisure, correct any inequality in load division or error in frequency by a manual control which acts through the speed-matching device to adjust the governor setting. Thus he has full control over the operation of the system but is relieved of the necessity of performing the somewhat critical operation of simultaneously matching speed and phase position and closing the breaker at the correct instant. With four alternators he would otherwise have to go through this cycle three times in succession shortly before the time of take-off.

Manual Synchronizing

The weight of the control equipment can be reduced by using manual instead of automatic synchronizing methods, but the saving in weight must be balanced against the increased burden on the flight engineer, the increased chance of an outage caused by faulty manual technique, and the decreased availability of the generating equipment because of failure to connect it to the bus promptly. Perhaps the least objectionable in these respects is a semiautomatic system which differs from the one previously described in that the automatic speed-matching feature is replaced by a manual control. This arrangement requires the attention of the flight engineer at each synchronizing operation but greatly eliminates the factor of faulty manual technique, because the automatic means for determining when the breaker should be closed are retained. The weight saving in comparison with the fully automatic system is approximately two pounds per alternator.

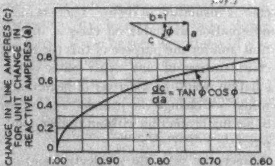


Figure 5. Rate of change of ammeter reading with respect to the reactive component of current plotted against power factor

At normal power factors the ammeter is an insensitive indicator of correct voltage-regulator equalization

Of the straight manual synchronizing schemes which have been proposed for airplane use, perhaps the most foolproof is the one in which the alternator is synchronized in a manner which is common for synchronous motors. The incoming machine is brought up to slightly below synchronous speed with zero or weak field, the circuit breaker is closed, and the machine accelerates toward synchronous speed because of the motoring effect produced by its damper winding. When it is close enough to synchronous speed, it will be pulled into step by its reluctance torque. If this is done with no field excitation, there is a 50–50 chance that it will pull into step with the wrong pole position and will have to slip a pole pitch when field is applied. If the breaker is closed with weak field on the machine, the pulsating current will be higher, but the machine will pull in with the correct pole position. An advantage in starting with weak field lies in the fact that synchronizing lamps may be used as an indication of slip speed while the speed is being raised by manual adjustment of the governor before the breaker is closed.

An important disadvantage of this synchronizing method is the high motoring current which is drawn even at a low slip. Calculations on the present design of airplane alternator indicate that this will equal rated full-load current at about one per cent slip and 160 per cent of full-load current at about five per cent slip. Furthermore, this current pulsates badly at slip frequency. The current calculations were made with the assumption of an infinite bus, and the actual current will be somewhat less. This will increase the time of the system disturbance. The nature of this effect is substantiated by tests which were made during the development of the equipment for the *XB-19* airplane. With one machine on the bus, synchronizing the second machine by this method will be difficult with a load of more than 50 per cent of the alternator rating. It may be found necessary in practice to remove the system load before synchronizing the second alternator.

Adjusting the Voltage Regulators

Experience has demonstrated the difficulty of convincing airplane crews that kilowatt load in an a-c system cannot be balanced between paralleled alternators by adjustment of the voltage regulators. Having had instruction and more or less extensive experience with d-c systems, they must be shown by example that the two systems are not alike in this respect. As serious misadjustment may be encountered, and as this will have an adverse effect on stability because of unequal field excitation, the process of regulator adjustment must be made as simple as possible.

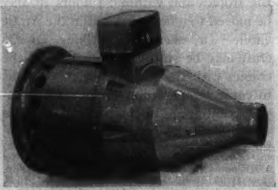


Figure 6. Mock-up model of 40-kva alternator of current design

The fact that ammeters are not good instruments to follow when adjusting the regulators is emphasized by Figure 5, which shows how the ammeter reading varies with the changing reactive current, the inphase current remaining constant. At 0.75 power factor the ammeter responds two thirds as fast as the reactive component, whereas at 0.87 power factor the response is only one half as fast as the change in the reactive component. The matter is further complicated at small loads by the fact that a-c ammeter scales are condensed in this region. At normal power factors the inphase component of current has a greater effect on the ammeter reading than the reactive component does, so that a change in power factor or a change in load balance caused by governor aberrations while the regulators are being adjusted will be extremely confusing.

Because regulator adjustment directly affects the reactive current balance between paralleled machines, varmeters (which read "volt-amperes reactive") give a positive indication of the condition of regulator equalization. Not being responsive to real or kilowatt load, they will not give confusing readings because of governor misadjustment. A varmeter is nothing but a wattmeter with a reactive compensator to shift the potential coil voltage 90 degrees from its true position. Since wattmeters are already necessary in the system to permit supervision of loading and adjustment of the governors, the addition of reactive compensators and a simple switching arrangement make them useful for regulator adjustment also.

Conclusions

A background of experience exists to guide the development of 400-cycle power systems for airplanes, and this knowledge is being used to advantage in the present program of expanding the installed capacity. To duplicate the reliability of the present d-c system, parallel operation of the alternators is required. Parallel operation of auxiliary-engine-driven units already has been accomplished, and, given constant-speed mechanical drives with the correct characteristics, this performance can be duplicated with main-engine-driven alternators. The numerous duties of a flight engineer on a multiengine airplane make it highly desirable to provide automatic synchronizing control to increase the availability of the power system and eliminate outages caused by man failure during the synchronizing operation. Equipment for doing this is available or is being developed.

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Principles of Aircraft Electric-Motor Protection

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NONMEMBER AIEE

Introduction

IN writing a paper of this kind for the AIEE, it is needless to say that one part of the aircraft electric-motor protection problem is protection from excessive temperatures. The major problem with the aircraft electric motor is to make available its maximum output before the protector disconnects the aircraft electric motor from the line.

The problem involved in applying an aircraft motor protector for maximum output under all conditions may be realized when it is considered that today an aircraft may be stationed on a field in the tropics and within 24 hours it may be moved to a field well within the Arctic Circle. Or, even more striking, an aircraft may be stationed on a field in the tropics with the sun beating down so as to cause temperatures as high as 70 degrees centigrade in compartments where electric motors are located. In a few minutes this aircraft may be flying at a high altitude where the ambient temperatures may be as low as −60 degrees centigrade.

In domestic, commercial, and industrial uses of electric motors, it has been the aim to protect electric motors so they would not be personal or fire hazards. In accomplishing this, it has been good engineering practice to provide an ample margin of safety. In October, 1942, C. P. Potter presented a paper before the AIEE entitled "The Inherent Overheating Protection of Single-Phase Motors" from which I quote his comments regarding the summary of the data for the tests under various loads, voltages, speeds, ambients, and amounts of ventilation:

> "Conclusions
>
> The variations in temperature shown in the table are small and lead to the conclusion that inherent overheating protective devices mounted inside fractional horsepower single-phase motors completely protect them against all abnormal operating conditions."

There are many engineers who believe that all motor protectors will limit the work output of electric motors, and they advocate that the operator should have the privilege of destroying the aircraft electric motor if by so doing he thinks he can accomplish his mission. It is questionable if any operator has the ability to judge properly during the stress of emergency as to when he should or should not destroy a motor.

A good automatic device will always repeat its performance, and in this paper it will be assumed that a properly designed and properly applied electric-motor protector must not place any limitations on the potential work output of the electric motor.

Stated in the simplest terms, the problem is to establish principles to follow in protecting aircraft electric motors from destructive temperatures without limiting output regardless of the loads or the ambient encountered.

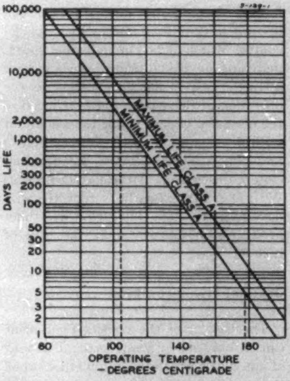

Figure 1. Effect of temperature on life of insulation

While there are many classes of insulation in use in aircraft electric motors, many of these electric motors have some class *A* insulation in them. There is more information available on class *A* insulation, and for the purpose of this paper the consideration of any other class of insulation would be a matter of degree and would not change the principles to follow in having protection without limiting output.

Since the first use of electric motors, there has always been this problem of temperature protection. First, fuses were used, then current-sensitive devices mounted remote from the electric motor, and since 1929 there has been an increasing use of built-in electric motor protectors. E. J. Schaefer, formerly of the

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fractional motor engineering department of the General Electric Company, in a paper entitled "Protection of Automatically Started Fractional Horsepower Motors," published in the September 1940 issue of the *News-Bulletin*, of the International Association of Electrical Inspectors, shows that only with a properly built-in protector can there be protection and at the same time approach maximum motor output under any possible load condition.

Many aircraft electric motors have intermittent-duty cycles and in order to conserve weight the motors are operated at high speeds, and these intermittent-duty motors are regularly operated at high overloads as compared to a continuous running rating. This results in motors of small size which presents a space limitation for the location of the protector.

Location of Protector

It takes no data or lengthy discussion to convince one that theoretically the proper location for the protector would be at the danger hot spot where it would be responsive to the danger hot-spot temperature. Because of physical limitations, this is impossible; so, there must be a compromise by preparing a location when designing the motor body that will give the thermostatic element the best possible opportunity of absorbing heat from the danger hot spot.

Temperature at Which to Protect

Regardless of the kind of insulation used, insulation life is dependent on both time and temperature. This is best illustrated by the life-temperature curve for class *A* insulation, Figure 1 of this paper, copied from page 434 of the book "Insulation of Electrical Apparatus," by Douglas Miner.

Class *A* insulation is listed by AIEE as good for a maximum safe operating temperature of 105 degrees centigrade. From the curve, it can be seen that this will give a minimum expected life of 2,000 days. The tentative Army-Navy (AN-M-10) electric motor specifications require that the protectors provide a minimum life of five continuous hours of operation under locked rotor conditions without damage to the motor. To provide sufficient manufacturing tolerance, it would be desirable to select a temperature corresponding to a minimum life of five days. This gives a temperature of 178 degrees centigrade.

Even with the selection of this high maximum allowable operating temperature for five days' life of the insulation, it should be remembered that in normal applications the actual operating temperature of the motor will be far below this value. This is best illustrated by Figure 2.

Line *A* represents room ambient of 25 degrees centigrade.

Line *B* represents the maximum winding operating temperature of 105 degrees centigrade under normal loads and 25 degrees centigrade ambient temperatures.

Line *D* represents the maximum winding temperature at which there is a tendency for most engineers to protect the motor. If this temperature of 120 degrees centigrade were selected, it is obvious that a small overload or an abnormally high ambient would cause nuisance tripping of the protector as there is very little winding temperature difference between full load and the point where the protector would open.

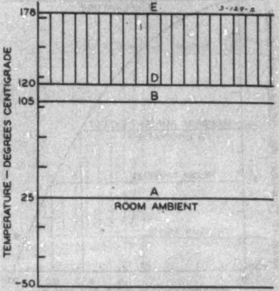


Figure 2. Chart of temperature levels involved in aircraft electric-motor protection

Line *E* is the 178 degree centigrade temperature selected for a minimum of five days' life from Figure 1.

To get protection without limiting the work output of the motor, the protector must control the motor winding temperature at 178 degrees centigrade, line *E*. The shaded area between the curves *D* and *E* represents the thermal capacity which most motor designers would not use. Thus, if protection were applied on the basis of line *D*, representing maximum winding temperature, it would lead to the condition where the aircraft designers (and others) would know that there was additional capacity available in the motors as indicated by the shaded area, and he (or they) would then rather have the operator run the risk of burning out the motor than be limited in power output as indicated by line *D*. The values used in this paper are on the basis that the winding temperature is the limiting hot spot; other hot spots would have different hot-spot maximum temperatures.

The Danger Hot-Spot Temperature

The temperature that will first cause damage to an aircraft electric motor need not be the winding temperature as indicated heretofore, or even the highest temperature in the motor. The first damage may be at the brushes, the solder, or the shunt leads to the brush-holder, and so forth. It is necessary to determine where the danger hot spot is and how hot it gets. This may be estimated at the time of designing, but the actual data can be obtained only by test with thermocouples and other temperature-measuring devices located at every point of possible damage.

Figure 3 is a photograph of such a test setup with eight thermocouples and four voltage-drop leads mounted in the motor. To be complete, these data should be obtained not only at room ambient but also at the maximum and minimum ambients at which the motor is expected to operate, and with various loads from the maximum continuous load the motor can carry without exceeding the maximum allowable hot-spot temperature through and including locked rotor. This is necessary because, for example, under locked rotor the armature windings may be the danger spot, and during running the danger spot may be shifted to the solder on the commutator.

Thermal-Element Operating Temperature

We know that the heat dissipation, or in other words the capacity of a motor, will vary with ambient. The accurate calculation of the capacity at different ambients would involve the use of a number of empirical values based on test data which are not available today. As the major portion of the heat is generated in the copper windings, a simple method of indicating the trend would be to use the following formula:

$$R_x = R_n\sqrt{\frac{T_m - T_x}{T_m - T_n}}$$

R_x is the current rating at a given ambient.

R_n is the current rating at room temperature (25 degrees centigrade).

T_m is the maximum allowable temperature (178 degrees centigrade).

T_n is the room temperature (25 degrees centigrade).

T_x is the given ambient temperature.

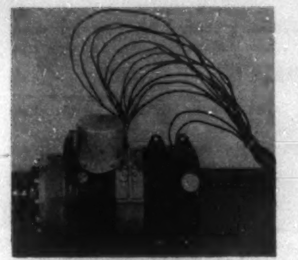


Figure 3. Aircraft electric motor equipped with thermocouples and resistance leads

The maximum allowable winding temperature previously adopted in this paper was 178 degrees centigrade. It is assumed that an intermittent-duty motor when carrying 80 per cent of its rating continuously at normal room ambient (25 degrees centigrade) will have a temperature rise of 153 degrees centigrade, resulting in an operating temperature of 178 degrees centigrade.

The curve in Figure 4 is obtained by using these values in the formula. It has been assumed that this intermittent-duty motor when running continuously would only carry 80 per cent of its rating at 25 degrees centigrade ambient. It would carry more load at lower ambients as indicated by the curve at −50 degrees centigrade, where it will carry 97 per cent of its rating continuously. At high ambients the motor output would be limited below the 80 per cent. At 70 degrees centigrade ambient the curve shows the motor will operate continuously at 67.5 per cent of rating. At an ambient of 178 degrees centigrade, the motor should not operate at all because the hot spot is already up to the maximum allowable temperature.

The information in this curve may be stated as follows: At any ambient there is one value of continuous load current that will bring the danger hot-spot temperature up to the maximum allowable value of 178 degrees centigrade (line *E*). If the protector is going to allow the motor to attain this temperature (178 degrees centigrade) at all ambients before removing the motor from the line, a protector will be needed that will have the same ambient characteristics as the motor it is to protect. As the operating temperature of the thermal element must be a fixed value, and if it is to match curve Figure 4, the thermal element must operate at 178 degrees centigrade when carrying zero load. This then establishes the operating temperature of the thermal element at 178 degrees centigrade. It can be stated that theoretically for continuous running loads the operating temperature of the thermal element of the protector should be the same temperature as the temperature of the hot spot to be protected.

Function of the Heat Generated in the Protector

When discussing the location of the protector, it was stated that it was physically impossible to locate the protector at the hot spot. With the protector located in a compromise position, it cannot be expected that the protector will be heated by the temperature of the danger hot spot up to the hot-spot temperature. This is illustrated graphically in Figure 5.

Curve *M*, the ambient temperature versus motor continuous carrying capacity, is the calculated curve from Figure 4, and with a protector mounted in this motor, the curve of the protector temperature, that is, the temperature of the metal parts of the motor which surround the protector, would fall between curve *M* and curve *E*. The exact location of curve *P* would depend on how sensitive the protector was to the hot-spot temperature it is to protect. As the protector is a part of the motor assembly, the shape of the protector temperature curve *P* will be similar to curve *M*. But, regardless of where the protector is mounted, the protector location temperature under continuous operation would be greater than the ambient temperature curve *M* and less than the operating temperature of the thermal element curve *E*.

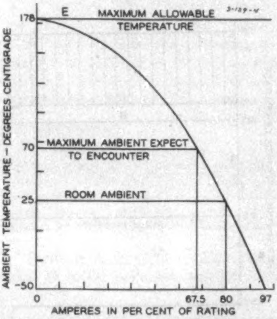


Figure 4. Ambient versus per cent load curve

In order to use a thermal element operating temperature of 178 degrees centigrade, some method must be used to raise the thermal element temperature from that temperature surrounding the protector, curve *P*, up to 178 degrees centigrade. This can be accomplished by generating heat in the protector. Figure 6 is a schematic diagram of a protector with a heater built in for this purpose. This heater is connected so that the load current passes through it and the thermal element, and the heat generated in the protector is proportional to the current flowing at various loads, thus furnishing a variable temperature rise to the thermal element depending on the load current. Therefore, at any ambient there is a per cent of motor load current that will not only cause the hot-spot temperature to reach 178 degrees centigrade, but that same value of current can be used to cause the thermal element to reach the same temperature.

To illustrate, take the point at 70 degrees centigrade ambient. From curve 4, 67.5 per cent rated current will raise the danger hot-spot temperature to 178 degrees centigrade and from curve Figure 5 at this same 70 degrees centigrade ambient, the motor heats the protector to 125 degrees centigrade and the heat generated in the protector must raise the thermal element up to 178 degrees centigrade. Therefore, it can be seen that the horizontal crosshatched area indicates the temperature rise imparted to the protector by the heat of the motor and the vertical crosshatch indicates the temperature rise that must be imparted to the thermal element by the heater.

Ultimate Trip

So far, continuous performance of the motor has been discussed, and, if, after running sufficiently long to reach maximum stable conditions for any given

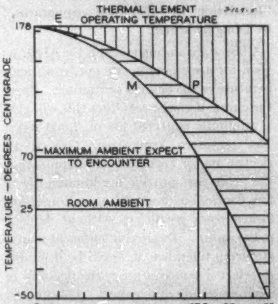


Figure 5. Motor ambient and protector temperature curves

ambient, there would be some change in these stable conditions, such as a rise in the ambient temperature or a slight increase in load current, both the hot-spot and the thermal-element temperature would rise, but as the protector would operate at any temperature in excess of 178 degrees centigrade, the motor would have reached an *ultimate* running condition as the protector would remove the motor from the line. This is called *ultimate trip* and may be stated as that value of load current which at a given ambient will cause tripping of the protector when the motor has reached a stable condition.

Locked Rotor

Locked rotor is the most severe load condition that can be imposed on the motor. Therefore, it is necessary that the protector take care of this condition in a satisfactory manner. For continuous running, the temperature of the protector would not follow the hot-spot temperature. So, under locked rotor, the protector temperature will lag still further behind the hot-spot temperature. This is illustrated in Figure 7.

F is the time-temperature curve of the danger hot spot for locked rotor. Since the locked-rotor current is high, the temperature will rise so rapidly that the protector (curve *G*) absorbs very little temperature from the motor before the hot spot has reached a dangerous temperature. This means that when the rotor is locked, the heat generated in the protector must furnish all of the temperature rise to the thermal element to bring it up to 178 degrees centigrade. The shaded area of Figure 7 shows this temperature rise that must be furnished by heat generated in the protector for 25 degrees centigrade ambient.

Some engineers think that locked-rotor protection is all that is needed for d-c aircraft electric motors. This thinking is based on the fact that the duty cycle of most aircraft electric motors is very short, and the major troubles come from jammed mechanisms which cause stalling of the rotor. Then, as soon as the rotor stops, it heats rapidly as shown in curve *F* of Figure 7. With a constant voltage supply, the current falls off because of the increased resistance of the windings, and the motor starts losing torque.

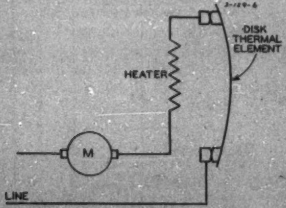


Figure 6. Schematic diagram of heater-type motor protector

If protecting the motor from destructive temperatures was all there was to consider, a protector that provided locked-rotor protection would be a satisfactory protector, and holding the motor on the line for only one second would be sufficient time to let the motor start if free to do so. In aircraft there are many times when mechanisms are not jammed but are damaged sufficiently to introduce loads approaching blocked conditions, and at the same time the motor is able to turn over slowly. While it may take a relatively long time for it to complete its work, the chances are that the motor will do some good if it can be kept operating without burning out. This means that for locked-rotor conditions, the motor should not be taken off the line until the danger hot spot has reached its maximum allowable temperature. In providing locked-rotor protection without regard to the maximum output, it is possible either to overprotect, so as to cause nuisance trip-outs, or to underprotect so that the motor will burn out at normal loads or running overloads.

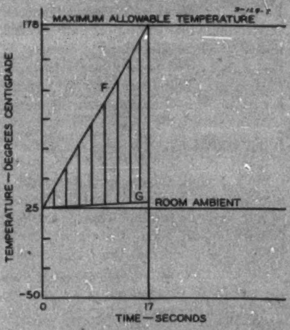


Figure 7. Heating curve for locked rotor

Overprotection is illustrated in Figure 8. Here the motor is carrying an excessive load but has not reached a stalled-rotor condition before the danger hot spot reached the maximum allowable temperature. The curve *J* is the time-temperature curve under a high running overload, and *K* is the time-temperature curve of the protector location. If this protector were one that took the motor with locked rotor off the line in one second, then under this extreme running condition the protector would have taken the motor off the line in probably four seconds, and the motor output would be limited to a small amount of work, and the danger hot spot would have been only 35 degrees centigrade. This kind of locked-rotor protection would not provide the maximum possible output of the motor. By proper design of the protector as to the heat generated in the thermal element, the operating temperature of the heater, the amount of heat, and its location with respect to the thermal element, it is possible to have some control over the rate of temperature rise of the thermal element so that regardless of the load conditions the protector will let the danger hot spot approach its maximum allowable temperature before disconnecting the motor from the line. Under these conditions, the motor can give maximum work output with protection from destructive temperatures regardless of load current or ambient.

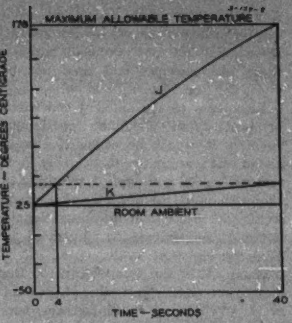


Figure 8. Heating curve at high overloads

Loads Less Than Maximum Continuous Running

Any load below the continuous value that will not heat the danger hot-spot temperature up to its maximum allowable temperature has not been considered in this paper, for, obviously, no protection would be needed for this condition.

Intermittent Loads

Up to this point, the two extremes of load that would call for protector operation to protect the winding hot spot have been discussed:

1. The maximum continuous running load.
2. Locked rotor.

Any value of intermittent load that would call for protector operation in protecting the winding hot spot would fall between these two extremes and need not be given any special consideration.

Regardless of the ambient conditions or load above maximum continuous running, it would be expected that a motor with an inherent overheat protector designed and applied according to the principles laid down in this paper would give a constant maximum danger hot-spot temperature as illustrated in Figure 9.

Curve *N* is the temperature of the danger hot spot for any value of overload or locked rotor and shows the same hot-spot temperature for all ambients.

In following out these principles, protector and motor designers are confronted with limitations, such as

1. Applying protectors to existing motors.
2. Limitations as to materials for the insulation of the protector.
3. Space limitations.
4. Physical properties of materials for the thermal elements and for the heaters.
5. Manufacturing tolerance more particularly of the motor than the protector.

In view of these limitations, some deviation would be expected from the theoretical curve *N* in Figure 9.

The curves in Figure 10 (results of tests on a protected aircraft electric motor) show how surprisingly close a protected motor can come to the theoretical conditions when a protector is designed and applied in line with the principles of this paper.

Conclusions

In conclusion, these principles can be summarized as follows:

1. Prepare a protector location when designing the motor.
2. Select those points in the motor which are likely to be the danger spots and assign to each such point the maximum temperature possible.
3. Locate the danger hot spot by test with thermocouples and resistance rise measurements.
4. Adopt a thermal-element temperature as close as possible to the protection temperature of the hot spot to be protected.
5. Do not overprotect under any load condition; let tolerance be on the side of a possible loss of a motor now and then rather than rob all motors of a given design of any potential capacity.
6. Supply the difference in temperature between protector location and the thermal-element operating temperature by a heater built into the protector.

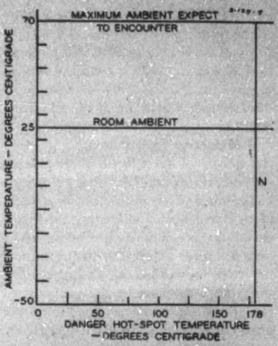


Figure 9. (Theoretical inherent protected motor) ambient versus hot-spot temperature for maximum safe load or overload

7. When using a protected motor, do not undermotor an application.

Men's lives depend on the continued functioning of aircraft electric motors. Restricting the output of an electric motor is poor engineering; a burned-out motor is a total loss; protect them so that all allowable output of aircraft electric motors is available to the crews.

Supplement

Since writing this paper, I have had a number of discussions with various people, and as a result of these discussions I have made some theoretical curve sheets which are in this supplement. These curves have particular reference to the results of overprotection and to the results that are likely to occur when one makes an application for locked-rotor protection only.

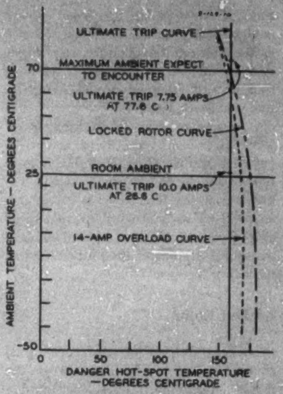
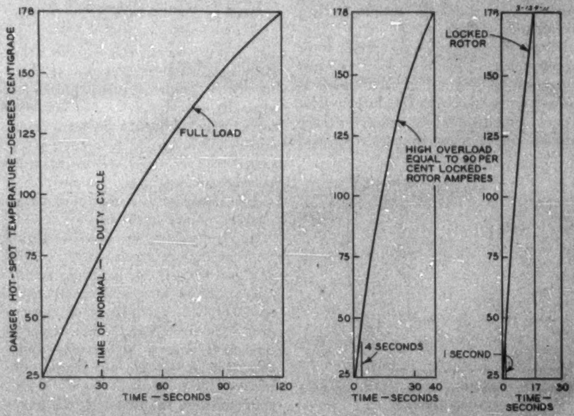


Figure 10. Actual results—inherent protected motor—ambient versus hot-spot temperature for any maximum safe load or overload

On curve sheet Figure 11, at the left, is the theoretical time-temperature curve for an intermittent-duty 24-volt d-c motor, run at an ambient of 25 degrees centigrade and with full load current. It is to be noted that the duty cycle is 30 seconds and that it takes 120 seconds for the hot spot to get up to the assumed dangerous temperature of 178 degrees centigrade. From the data on this curve, it is evident that as long as everything

Figure 11. Theoretical time-temperature curves for intermittent-duty 24-volt d-c motor at 25 degrees centigrade ambient temperature



runs normally no protection is required. The motor cannot be overloaded, and it cannot get too hot.

At the extreme right is the theoretical time-temperature curve for locked rotor. Here, we see that the motor will be on the line 17 seconds before the hot spot reaches the assumed dangerous temperature of 178 degrees centigrade.

When a protector is applied to give locked rotor protection only, there is a tendency to set the protector down to as short a time as possible.

In these d-c motors it only takes a fraction of a second for them to come up to speed so that a protector which cuts off at one second would allow ample time for the motor to start and come up to speed, providing there was no excessive load on the motor. A protector with this type of characteristic would give protection for locked rotor, and, if the rotor was definitely locked because of jamming in the mechanism, there is nothing to be gained by leaving the motor on longer than the one second.

In the center curve of Figure 11 we have a theoretical time-temperature curve of this motor when the load is such that the current is approximately 90 per cent of the locked-rotor amperes. Here the motor is running and doing some work, although the speed may be very slow, yet, if kept on the line long enough, the duty cycle could be completed. This would be particularly true if the motor under this running condition were allowed by the protector to run up to its maximum assumed safe temperature of 178 degrees centigrade. In this case, you will note that it would take 40 seconds to attain this temperature. The protector would cut "on" and "off" and the motor would run for a while, and then stand stationary for a while, and then run, and so forth, until the unit had an opportunity to complete the cycle.

On the other hand, if this motor were protected with a protector that cut off at one second under locked conditions, then four seconds would be the maximum that we could expect the protector to let the motor run with 90 per cent of locked amperes, and here we would have such a short "on" cycle that the length of time required for the unit to complete its duty cycle would be so great that it is extremely doubtful if any useful work could be obtained from the motor at this or any other high overloads.

On curve sheet Figure 12, these two types of protector performances are illustrated by the "on" and "off" cycles. In the curve at the top of the page, the protector allowed maximum output of the motor and in the one at the bottom of the page, the protector was applied for locked-rotor protection only (one-second cutoff). By accumulating all of the "on" times, we see that at *A* we have an accumulated running time shown as the result for six cycles of protector operation, whereas at *B* we have the accumulated time for 15 cycles, the 15 cycles having taken place in the same length of time as the six cycles shown.

From experimental work done so far, it appears as if there is considerable likelihood of obtaining the poor performance indicated by applying thermal protectors for locked-rotor protection only. Therefore, it is believed that one of the most important considerations in connection with aircraft electric-motor protection is to make sure that the protector allows the motor to give its maximum capacity under all conditions of load and ambients.

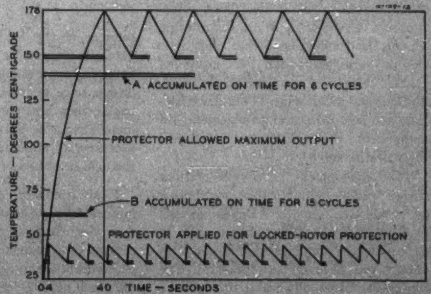


Figure 12. Theoretical time-temperature curves for intermittent-duty 24-volt d-c motor run at 90 per cent locked-rotor amperes

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The Testing of Mercury-Arc Rectifiers

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THE design of mercury-arc rectifiers is based, as is that of all electrical apparatus, upon knowledge gained from fundamental principles, test data, and operating experience. Our knowledge of the physical action occurring within the rectifier is very limited. While the fundamental processes which take place in an arc discharge have been quite completely described by physicists,[1] their complexity has prevented their expression in a form usable by the engineer. For this reason rectifier design depends, to a larger degree than does the design of most electrical apparatus, upon experimental data obtained from tests.

Of the actions occurring within the rectifier, the phenomenon of arc-back is particularly important as it plays a dominant role in the testing, rating, and performance of mercury-arc rectifiers. Various theories have been proposed in an endeavor to account for arc-backs, and the more recent of these appear to agree quite well with observations and experience.[2,3] Despite these advances, the causes of arc-back are not yet sufficiently well understood so that the effect of specific modifications in the construction may be reliably predicted.

Special testing procedures are required to meet the unique conditions encountered in rectifier operation. Arc-back behavior is a primary consideration in the formulation of load tests involving the determination of arc-back rate. Some correlation has been observed between duty on the rectifier, condition and construction of the rectifier, and frequency of arc-back, but the occurrence of arc-back is essentially random in nature, not only under definite limiting conditions, but under all conditions of rectifier loading. The determination of arc-back rate from load tests is, therefore, a statistical problem. The magnitude of the problem may be appreciated from the fact that one arc-back per month on a six-anode rec-

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tifier operating at 60 cycles is but one failure per billion cycles. It is obviously impractical to check such performance by operation under normal conditions, so special testing methods must be devised.[4]

The primary purpose of this paper is to describe the procedure used in testing mercury-arc rectifiers during both development and manufacture. Tentative standards for testing rectifiers are covered by report on AIEE Standard 6 for "Acceptance Tests for Metal-Tank Mercury-Arc Rectifiers," issued in 1934. However, these standards do not cover recent developments in testing technique.

The nature of the electrical and mechanical limitations of the rectifier is discussed in an endeavor to provide a basis for the evaluation of results of tests in terms of expected operation in normal service. The basis for the rating of rectifiers is also discussed and data are presented showing the results of tests on standard rectifiers.

Purpose and Scope of Tests

Tests on mercury-arc rectifiers may be classified under three general heads, depending upon the purpose for which they are made; namely, developmental, commercial, and field tests.

Developmental Tests

Since the fundamental knowledge is not sufficiently complete to provide a satisfactory design basis, new rectifier designs are based upon experience gained from previous designs supplemented whenever necessary by tests bearing directly on the problem at hand. Developmental tests may range from general studies of the physical action to specific tests on a full-size sample of the new design.

Examples of such general studies are: heat transfer and the distribution of losses; vapor pressure, and vapor flow; ionization during the inverse cycle; inverse current to the anode; phase occurrence of arc-back; current distribution during the conduction period; magnetic effects; and so forth. Examples of specific tests are: mechanical, electrical, and thermal measurements on new materials and new structural arrangements for component parts, load tests, control tests, and so forth.

The development of a new rectifier design usually requires the construction of a full-size sample. Developmental tests are made on this sample in order to determine the modifications required to obtain the desired performance. The complexity of the physical action and the lack of complete knowledge of the fundamental processes preclude the use of scale models in making rectifier tests.

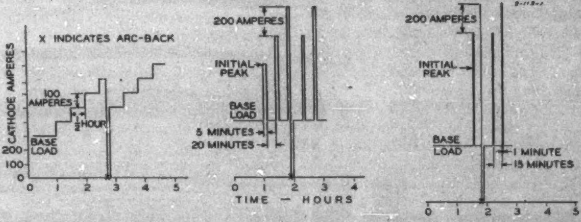
Commercial Tests

These are tests made on commercial rectifiers during manufacture for the purpose of

1. Checking the quality of manufacture.
2. Determining the characteristics of the rectifier equipment.
3. Checking the over-all performance.

Field Tests

Despite the steady accumulation of rectifier knowledge and experience, it is still not possible to predict or guarantee in advance the exact performance of a rectifier in the field, particularly as regards the frequency of arc-back. Rectifiers of proved ample capacity and good design may operate with excessive arc-backs when first put into service in new installations, and it is sometimes found necessary to make minor adjustments in the field to obtain proper performance.



A. Continuous load limit
1. Start at base load specified
2. Increment, 100 amperes every half-hour, approximately five per cent of basic load
3. Increase load until arc-back occurs
4. Reduce load 200 amperes and continue as before
5. Continue until three successive arc-backs are obtained at substantially equal loads, indicating that limit has been reached

B. Five-minute load limit
1. Start at base load two thirds of continuous load limit
2. Initial peak, 200 amperes greater than continuous load limit
3. Duty cycle—20-minute base load, five-minute peak, repeat
4. Increment, 200 amperes on successive peaks
5. If arc-back occurs, reduce load 200 amperes and continue as before until three arc-backs are obtained at limit

C. One-minute load limit
1. Start at base load two thirds of continuous load limit
2. Initial peak—200 amperes greater than five-minute load limit
3. Duty cycle, 15-minute base load, one-minute peak, repeat
4. Increment, 200 amperes on successive peaks
5. If arc-back occurs, reduce load 200 amperes and continue as before until three arc-backs are obtained at limit

Figure 1. Procedure for load-limit tests

Field tests are usually directed at factors not covered by the developmental tests, for example, the effects of longer periods of continuous loading than can be undertaken in the factory tests or different service conditions. Furthermore, field tests must be arranged so that they may be carried on concurrently during normal operation.

List of Tests

A list of the more important tests used in the development and manufacture of mercury-arc rectifiers follows:

1. Shop tests.
Vacuum tightness.
Degassing.
Dielectric tests.

2. Load tests.
Load-limit tests.
Load runs.
Duty cycle.

3. Short-circuit tests.
Current limit.
D-c short circuit.
Simulated arc-back.

4. Control tests.
Pickup action.
Blocking action.
Phase control.

5. Performance characteristics.
Efficiency.
Voltage regulation.
Power factor.
Wave form.

Shop Tests

In the manufacture of pumped rectifiers the first tests made upon the completion of assembly are vacuum tightness and degassing. These tests provide a check on the basic structural quality and condition the rectifier for full voltage operation. Together with the dielectric tests they comprise the ordinary short commercial tests.

VACUUM-TIGHTNESS

The vacuum tightness is checked by taking seepage tests. These tests provide a measure of the tightness of the tank materials, welds, insulating seals, and gasket joints and are usually made both before and after degassing. The test procedure consists in evacuating the rectifier, then shutting off the vacuum pumps with the rectifier at room temperature and noting the increase in pressure over a definite period, usually six hours.

DEGASSING

While the rectifier degassing may be considered an essential part of the manufacturing process, inasmuch as the rectifier must be conditioned before it will operate at full voltage, the degassing operation also serves as a test of various features of the rectifier. For example, it provides a further check on the soundness of the welds, as the steel parts of the rectifier vacuum tank are heated to operating levels for the first time. A weak weld which appeared tight on the first seepage test may show a leak on the second seepage test. The time required for degassing is a measure of the pumping speed of the vacuum pumps. Some indication of the current capacity of the component parts of the rectifier assembly may be obtained by observing the temperatures of the various parts during degassing. The operation of the cooling system may also be checked during degassing since the rectifier losses at any specified current are substantially the same as during full voltage operation.

DIELECTRIC TESTS

High-voltage tests are made in the same manner as for most electrical apparatus using a 60-cycle alternating voltage. Such tests provide a check on the correctness of assembly and the soundness of the solid insulation.

Present AIEE Standards[3] call for the application of high-voltage tests after the rectifier has been thoroughly evacuated and degassed. It is not always possible to make tests under this condition because of the occurrence of breakdowns resulting from glow discharge. The voltage required for breakdown between electrodes in a vacuum may differ considerably from that required for breakdown at atmospheric pressure. Where difficulty of this kind is experienced, it has been the usual practice to make dielectric tests with the rectifier filled with air at atmospheric pressure.

Numerous attempts have been made to correlate the high-voltage break-down characteristics of a rectifier with the occurrence of arc-backs. Tests have been made with an alternating voltage and also with a negative direct voltage on the anode, increasing the voltage until breakdown occurred. Measurements of insulation resistance have also been made. While it is possible to determine the insulation strength and other characteristics of the solid insulation by such tests, they give no indication of the rectifier performance as regards the occurrence of arc-backs.

Load Tests

The purpose of load test on rectifiers is to determine their quality in terms of capacity and reliability. The simple structural abilities are readily evaluated by loading the rectifier and making thermal and mechanical measurements. But the arc-back rate cannot be determined by operating at normal loading because of time limitations. Theory indicates and experience has shown that the performance of a rectifier under normal conditions may be gauged from tests made under more severe conditions of loading.

There are two general types of load tests. One is the accelerated load test, or "load-limit test" as it has been termed, which is used primarily to determine the capacity of the rectifier. The other is the "load-run" or duty-cycle test, which is used for checking the reliability and quality of manufacture. The inherent difference between these tests lies in the method of loading. The procedure in the case of the accelerated test is to increase the loading gradually until failure occurs, either structural or arc-back, while that for the load-run test is to maintain a predetermined loading for a definite period and observe the operation.

The loading or duty on a rectifier involves a number of factors, among which are the following:

1. D-c voltage.
2. Load current.
3. Amount of phase control.
4. Type of circuit and circuit reactance.
5. Duration of loading.

All of these factors must be considered in the application of load tests. However, the usual procedure is to make tests at a given d-c voltage, such as 600 volts, and vary the loading by changing the load current. The amount of phase control may also be varied. Tests are usually limited to a single d-c voltage and one transformer circuit.

A further factor which is usually varied during load tests is the rectifier control temperature as the rectifier performance is a function of its temperature.

LOAD-LIMIT TESTS

Load-limit tests are made by applying a gradually increasing load on the rectifier until failure occurs, either structurally or because of arc-back. A typical loading procedure which has been used successfully in the development of a number of new rectifiers is shown in Figure 1.

Several factors must be considered in the choice of loading procedure. One of the more important of these is the duration of loading. The thermal storage capacity of a rectifier is small relative to its losses and the thermal time constant is about 1/2 to 1 hour. A one-half-hour interval was, therefore, selected for the continuous test. Shorter intervals were chosen for the five-minute and one-minute tests on the basis that substantially normal temperatures may be obtained by operating at reduced load between peaks. A set of continuous five-minute and one-minute load-limit tests using these time intervals may usually be made in an elapsed time of two or three days.

Another factor is the effect of aging upon rectifier performance. It is well known that the arc-back rate on a rectifier, during initial operation at full voltage immediately following degassing, may be greater than that obtained subsequently after several weeks' or months' operation. This improvement in operation is due to further conditioning occurring under load. This conditioning effect is hastened by operation at heavy loads such as are applied during load-limit tests, and it is, therefore, desirable to obtain several arc-backs at approximately the same load values in order to insure that the load limit has been reached.

It is not always possible to increase the rectifier loading sufficiently to obtain arc-backs. In some types of construction, parts of the rectifier, particularly those in the arc path, may be permanently damaged by excess loading, and this damage may occur before arc-back is obtained. In multianode rectifiers the cathode construction is frequently limiting. However, it has been found desirable, wherever practical, to design the

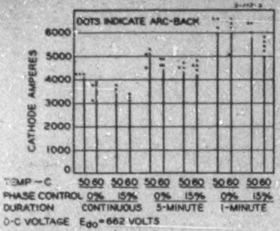


Figure 2. Results of load-limit tests on three ignitron tanks

rectifier so that damage of this kind will not occur, and the load limit is fixed by arc-back. A sturdy construction, capable of operation until arc-back occurs, facilitates testing considerably and permits full attention to the problem of arc-back.

The results of load-limit tests on three ignitron tanks are shown in Figure 2. The standard rating for a rectifier unit consisting of six of these tanks is 2,500 kw at 625 volts. The procedure described in Figure 1 was used in making these tests.

Single anode rectifiers like the ignitron or excitron, may be tested in sets of three since each tank is separate and operates independently. The circuit action, insofar as the voltage and current waves impressed on the anodes are concerned, is substantially the same when three tanks are operating on a single wye as when they are operating as a part of a multiple rectifier, for example, in a double-wye circuit. Three-anode operation facilitates rectifier testing considerably and provides a substantial reduction in time, labor, and power requirements. A large amount of the developmental work on the ignitron has been carried out on three tank units.

The results of load-limit tests on a six-anode multianode rectifier are shown on Figure 3. This rectifier has a standard rating of 1,500 kw at 3,000 volts. Although differing in detail, the procedure used for this test was similar to that already described.

In the case of multianode rectifiers, it is necessary to load the complete rectifier, inasmuch as the cathode and tank are common to all the anodes, and the general physical action in the tank is a function of the cathode current. Also, the ionization attributable to current in one anode affects the neighboring anodes. However, a reduction in the power capacity required for testing a multianode rectifier may be effected by passing current through some of the anodes at low voltage on degassing connection.

The effect of temperature upon rectifier capacity is clearly shown by the load-limit tests for the multianode rectifier, Figure 3. These curves indicate that there is an optimum temperature at which the rectifier capacity is a maximum and that the capacity decreases at higher or lower temperatures. Research[8] has shown that in the lower part of the temperature range the rectifier capacity is defined by a "current limitation" and the maximum current which the rectifier can carry is that which utilizes all the molecules in the mercury vapor as positive ions to neutralize the space charge. When this current is exceeded, the arc drop increases abruptly and arc-back ensues. In the upper part of the temperature range arc-backs occur as a result of voltage breakdown because of high mercury-vapor pressure. The voltage which the rectifier will withstand without arc-back decreases as the vapor pressure increases with rise in temperature. At the higher temperature the rectifier capacity is defined by a "voltage limitation." It is generally desirable to operate a rectifier at as high a temperature as possible without incurring arc-backs.

Load Runs

Load runs are made by applying a predetermined loading for a definite period and observing the operation, for example, counting the number of arc-backs which occur. Such runs are usually made with a constant loading applied for a period of 12 or 24 hours. Load runs at rated load or at loadings in excess of rating provide a check on rectifier performance under conditions approximating those encountered in service. While the period of loading is too short to determine the arc-back frequency on a successful rectifier and the service conditions may not be fully duplicated, such tests do indicate the maximum arc-back rate likely to be incurred in service.

In cases where load-limit tests cannot be applied because of structural or thermal limitations, lack of testing facilities, and so forth, the rectifier capacity may be determined by means of load runs. The procedure consists in making a series of load runs, increasing the loading on successive runs and determining the arc-back rate for each.

Commercial tests on rectifiers include load runs when complete tests are required. They may be made either with the transformer being furnished for installation with the rectifier, or with a test transformer. However, such tests are costly and furthermore do not assure successful operation as regards arc-backs. For this reason, when a rectifier of a proved design or a large number of rectifiers of the same design are furnished and only short commercial tests are required, load runs may safely be omitted. Often load runs are made on a sampling basis, for instance, by testing every tenth rectifier.

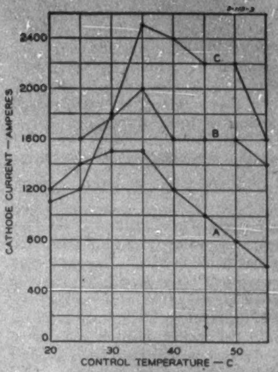


Figure 3. Results of load-limit tests on 3,000-volt multianode rectifier

Curve *A*. Continuous load limit with current increased 100 amperes every two hours

Curve *B*. 20-minute load-limit with current increased 200 amperes on successive trials; 40-minute operation at 500 amperes between peaks

Curve *C*. Five-minute load limit with current increased 200 amperes on successive trials; 25-minute operation at 500 amperes between peaks

Duty Cycles

The operating conditions incurred in electrochemical service are essentially the same as those obtained during load runs. In cases where the rectifier will operate under variable loads (as for example, railway or steel mill service), it is frequently desirable to make tests, applying a duty cycle simulating the actual operating conditions. This is particularly true where service conditions depart considerably from normal.

Faulty operation may be encountered in rectifiers at light loads as well as under overload conditions. One example of such trouble is mercury condensation on the rectifier anodes at low ambient temperatures with exposure to a cold air flow. Such condensation causes arc-backs on starting. Failures may also arise from the varying temperatures with changing load, which result in mechanical failure attributable to the movement of parts during expansion and contraction. Another difficulty which may be encountered is failure to obtain stable or constant temperature control with varying loads. Duty cycles simulating the conditions obtained in service provide a further check on the rectifier operation prior to installation, and assist in the elimination of such minor difficulties.

Determination of Rectifier Capacity

Many types of electrical apparatus are designed to meet definite temperature rise limits, for example, a 40 degree centigrade rise.[7] A principal determining factor in establishing temperature rise limits is the thermal endurance of the insulating materials, although other factors must also be considered. Since the physical deterioration of insulation under the influence of time and temperature increases very rapidly with temperature, the choice of temperature rise limit bears directly upon the probable life and reliability of the apparatus. The concept of temperature rise as a basis for rating is a convenient and useful aid in the design of most electrical apparatus.

In the case of mercury-arc rectifiers the construction is usually such that time and temperature produce no appreciable deterioration and the life is so long that replacement will probably not be required before obsolescence occurs. The principal economic considerations are operating cost and reliability. These factors cannot be gauged from any measurement of temperature rise. However, the reliability may be expressed directly in terms of the frequency of arc-back while the cost factors, such as rectifier power losses and maintenance, are also a function of the arc-back rate. Thus, it follows that rectifiers should be rated on a basis of arc-back frequency.

A high arc-back rate results in increased maintenance on protective switchgear, increased strain on transformer insulation, added hazards to operation, and interruption in service. The allowable arc-back rate depends upon the class of service. Many rectifier applications are of such character that an arc-back frequency of the order of one per month is acceptable, and 10 to 30 times this frequency for short periods does not exact any considerable economic penalty. Most railway, electrochemical, and industrial installations fall in this class. There are other applications where continuity of service is so important that an arc-back rate of more than one per year is not acceptable. Public utility installation involving essential service and industrial installations supplying power to gear cutting machines are examples of such applications. The two classes of service appear to necessitate different standards of performance.

Rectifiers having an arc-back rate of the order of one per year or less are usual at the present time for 250-volt service. Experience has shown that such performance is usually obtained when a 600-volt rectifier is operated at 250 volts. While it is possible to build rectifiers for 600-volt service having an arc-back rate not exceeding one per year, it appears that there would be certain economic penalties since the construction will entail considerable improvement in baffling with resultant arc losses, reduction in rating for a given size, and increased complexity of control.

An important consideration in the operation of a rectifier is its stability as regards arc-back. It must be recognized that an acceptable arc-back rate is not always obtained when the rectifier is first put into service. When trouble is experienced on a rectifier installation because of high arc-back frequency, the question is immediately raised, will there be progressive deterioration with an increase in arc-back frequency until the equipment becomes inoperative? Experience to date indicates that while the arc-back frequency may exceed the acceptable or desired value resulting from a large variety of causes, stable operation is generally obtained at a definite arc-back rate.

Short-Circuit Tests

Knowledge of the short-time current capacity of a rectifier is essential to the proper application of protective switchgear, and the choice of system reactance to limit fault currents. Short-circuit tests are of three general types: namely, single-cycle current limit, d-c short circuit, and simulated arc-back tests.

Current-Limit Tests

Basic data regarding the rectifier fault current capacity may be obtained by passing a single cycle of current through one anode with the d-c circuit shorted and increasing the magnitude of this current on successive trials until failure occurs. The behavior of the rectifier under these conditions is determined from oscillographic records.

The specific procedure for making current-limit tests is as follows: A single anode of the rectifier is connected to one phase of the rectifier transformer with the d-c leads short-circuited. The usual protective switchgear in a-c and d-c circuits may be provided but is arranged for delayed operation. The transformer is energized, but conduction is prevented either by negative grid voltage in the case of the multianode rectifier or by absence of ignitor excitation in the case of the ignitron. A single cycle of fault current may be caused to flow by energizing the grid or ignitor for one cycle by means of a welder control. The magnitude of the fault current is controlled by varying the phase of firing or by placing a reactance in series with the transformer circuit. In making these tests, the magnitude of fault current is gradually increased until arc-back occurs. Tests are usually made at several temperatures. Tests may also be made with more than one cycle of fault current.

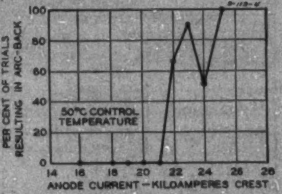


Figure 4. Results of current-limit tests on ignitron rectifier

The results of current-limit tests on a standard ignitron tank are shown in Figure 4, while Figure 5 is a typical oscillogram of a successful single cycle current-limit test.

Failure of control may occur in the course of current-limit tests as well as arc-backs. Control failures may be of various types. For example, in the case of ignitron rectifiers, the anode may fire during the cycle succeeding the conducting cycle without the ignitor being reenergized. In multianode rectifiers where voltage is applied to all the anodes but only one anode is permitted to fire, the other anodes may also fire because of failure of their grids to block. The control characteristics will be more fully described in the section on control tests.

D-C Short Circuit

D-c short-circuit tests are made for the purpose of checking

1. The ability of the rectifier to withstand the fault currents.
2. The operation of the protective switchgear.

The testing procedure consists in connecting a definite low resistance across the d-c leads of the rectifier by means of a contactor or high capacity switch. The resulting short-circuit current may be interrupted by the rectifier cathode breaker, anode breaker, or the oil circuit breaker in the transformer primary circuit. Or alternatively, the current flow may be suppressed by action of control grids or ignitors. The magnitude and duration of the short-circuit current as well as the operation of the protective switchgear may be checked by oscillograph.

The high currents flowing through the rectifier under short-circuit conditions cause the liberation of gas from the anode and other parts in the arc path. A measure of the speed of the vacuum pumps may be obtained by noting the time required to remove the gases liberated during the short circuit. Short-circuit tests also provide a check on the sturdiness of the rectifier tank and the external bus structure and their ability to withstand short-circuit stresses. While the rectifier must be able to withstand repeated short circuit without damage, it is not essential that it be able to operate without arc-back under the most severe conditions.

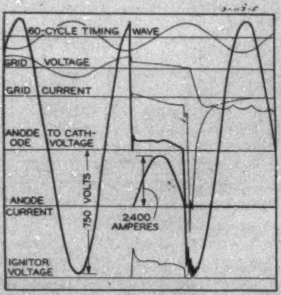


Figure 5. Typical oscillogram of successful single-cycle current-limit test (with negative voltage applied to grid to block firing on succeeding cycle)

Simulated Arc-Back

Since the switchgear furnished with rectifiers is specially selected to provide protection during arc-back it is often desirable to check its effectiveness by direct tests. This may be done by applying a short circuit between one anode of the rectifier and its cathode by means of a contactor or other shorting device, while the rectifier is operating under load. Such a short circuit simulates the conditions obtained when one anode of the rectifier arcs back. The resulting operation of the rectifier and its protective equipment is determined by taking oscillographic records.

Simulated arc-back tests may be made with the rectifier operating on either a water-box load or on a live load such as a motor generator set. In the latter case the load will feed current into the fault. The same is true where two or more rectifiers operate in parallel. Simulated arc-back tests are usually made only where complete tests are required. Because of the limitations of factory facilities, such tests are frequently made in the field.

Control Tests

The starting of anode conduction in a rectifier is controlled by the auxiliary electrodes, such as starting and excitation anodes, grids, and ignitors. In general, these may be arranged to perform the following three functions:

1. To provide the conditions required for anode conduction; namely, a source of electrons (cathode spot) at the cathode, the propagation of ionization in the mercury vapor in the arc path, and a flow of electrons to the anode. These conditions determine the pickup action.
2. To prevent the firing for one or more cycles by exerting a blocking action.
3. To delay the starting of conduction for a definite time each cycle so as to obtain phase control.

Rectifier control requirements vary from those of the simple shunt rectifier without control to those of a fully controlled rectifier with phase control for varying the output voltage and firing control for starting and stopping. The purpose of control tests is to obtain data regarding the various factors entering into the control action, and to check the effectiveness of the control means under the various conditions of operation.

Pickup Tests

Pickup tests are primarily of value in determining the excitation requirements of the rectifier and checking its light load operation. Pickup tests may be made by operating the rectifier at full voltage on a high resistance or counter electromotive force load. The pickup voltage is determined by varying the load resistance or the value of counter electromotive force voltage and observing the d-c volt-ampere characteristics of the rectifier.

In multianode rectifiers the arc is established at the beginning of operation by means of the starting anode and maintained during operation by the excitation anodes which are usually continuously excited. The excitation arc must provide sufficient ionization to assure pickup of each of the main anodes when its voltage becomes positive. When the rectifier is equipped with grids, the grid may also be excited so as to assist anode pickup. In ignitron rectifiers the arc is established during each cycle by means of the ignitor. The ignition current must both initiate the cathode spot and provide sufficient ionization to pick up the anode. As in the case of multianode rectifiers, the pickup characteristics are dependent upon the grid excitation.

The voltage range of the auxiliary power supply is an important consideration in the determination of rectifier excitation requirements. Low voltage is frequently encountered in normal operating service. Sufficient excitation must be provided to obtain satisfactory pickup at the minimum expected auxiliary voltage.

Poor pickup characteristics may result in a variety of troubles. Where several rectifiers operate in parallel and one rectifier is taken out of service, difficulty may be experienced in causing it to pick up and share load with the other rectifiers when it is reconnected to the bus. When rectifiers are installed for automatic operation, the control relay is sometimes arranged so that its action is dependent upon the pickup of the rectifier. In supervisory operation reliable pickup is essential to good operation.

The pickup characteristics of a rectifier may be influenced considerably by the condition of the rectifier tank. For example, it has been found that rectifiers which picked up satisfactorily when new gave trouble as operation was continued and the tanks became freer from gas because of aging. Also, the control temperature and the temperature distribution over the rectifier tank may have an important bearing on the pickup behavior. In multianode rectifiers, poor pickup may result from leakage resistance between rectifier tank and negative ground since the rectifier tank acts as a grid and tends to block the firing of the main anode.

Blocking Tests

Blocking action may be applied under two conditions. In one case it is used to prevent conduction while the rectifier is being placed in service. In the other case it is used to interrupt the rectifying action either at normal loads or under fault conditions. Effective blocking action is more difficult to obtain in the latter case, as the ionization carries over from the conducting cycle.

The minimum blocking voltage required on the grids of a rectifier in order to prevent conduction during starting may be measured by applying a positive d-c voltage to the anodes and a variable negative d-c voltage to the grids. The voltage on the grid is varied until the anode fires. This type of test may be made on either single-anode or multianode rectifiers. A typical grid characteristic for an ignitron is shown on Figure 6.

The effectiveness of the blocking action in interrupting the rectifier current under fault conditions may be determined by means of the same procedure used in making current-limit tests.

Phase-Control Tests

The output voltage of a rectifier may be controlled by varying the phase of the voltage applied to the grids or ignitors. For proper performance of this control function it is essential that a definite relation be accurately maintained between the control voltage applied to grid or ignitor and the anode firing.

There are two possible causes of control failure, namely, loss of control or early firing of the anode, and delay in anode pickup. Some of the factors which may cause control failure are: high load current, high or low temperature, foreign gases, faulty construction. In order to determine the grid or ignitor control characteristics it is necessary to operate the rectifier on load under the various conditions and observe or record the voltage and current characteristics of the control electrode and the anode by means of an oscillograph.

Loss of control is incurred when the anode fires either before or without the application of a control impulse. This may occur in a multianode rectifier when the grid does not prevent ionization reaching the anode and so permits the anode to fire when it becomes positive. In ignitron rectifiers the positive anode voltage acting on the residual ionization may initiate a cathode spot before the ignitor is energized.

A variable time may exist between the firing of the grid and the main anode under certain conditions. This phenomenon has been noted on both multianode and ignitron rectifiers. The difference in time of firing may be reduced to a negligible amount or eliminated by proper choice of firing circuit constants and operating conditions.

The control characteristics of the rectifier unit may be checked by making voltage-regulation tests with specified phase-control settings and comparing the measured output voltages against the calculated values.

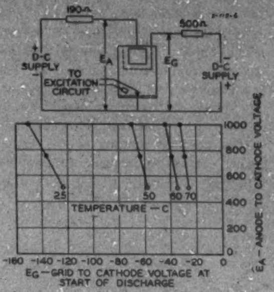


Figure 6. Grid-blocking characteristic of ignitron rectifier

Performance Characteristics

The performance characteristics of a rectifier unit may be determined by direct measurement. However, such tests require the assembly of the complete equipment and present other practical difficulties. The performance characteristics are, therefore, ordinarily determined from calculations based on measurements of rectifier and transformer losses and transformer reactance.

Efficiency

The "directly measured efficiency" is obtained from simultaneous measurements of input and output power. Elaborate precautions must be taken in order to obtain accurate results using this method.[8]

The "conventional efficiency" is obtained from the component losses determined from loss tests. Since most rectifiers have a high efficiency and the losses are only a small part of the total power, a given error in the measurement of the losses does not affect the efficiency as much as the same percentage error in the measurement of total input and output. For this reason the conventional efficiency method is preferred.

The losses incurred in a rectifier equipment are classified as follows:

(*a*). Rectifier arc-drop loss.

(*b*). Rectifier auxiliary losses.

(*c*). Rectifier transformer losses.

Methods for measuring these losses follow:

Arc-Drop Loss

Various methods have been proposed for the measurement of the rectifier arc-drop loss. See the report on AIEE Standard 6.[5] Of these, the oscillographic method has found widest acceptance. However, this method is open to the objection that the rectifier arc losses when operating at low voltage differ from those obtained in full voltage operation since the anode current wave forms are not the same. In order to overcome this objection an improved oscillographic method has been devised which permits operation of the rectifier at full voltage during arc-drop measurements.

The improved oscillographic method employs a noninductive external resistor connected in series with the oscillograph element and a small mercury-arc-rectifier tube (or dry-type rectifier) in shunt with the oscillograph element as shown in Figure 7. The shunting rectifier is connected so as to limit the voltage across the oscillograph element during the inverse period. This voltage may be further reduced by means of a battery connected in series with the shunting rectifier.

A typical arc-drop oscillogram of a large ignitron is shown in Figure 8. This oscillogram shows the arc-drop voltage and the anode current. The anode current wave is used to determine the limits of the conduction period. On large pumped rectifiers, there is considerable inductive effect in the rectifier tank itself which results in an inductive voltage during the commutation periods. This inductive voltage is superimposed on the actual arc-drop voltage so that the limits of the conduction period cannot be determined directly from the arc-drop voltage wave.

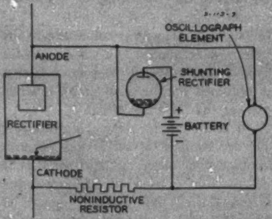


Figure 7. Schematic diagram of oscillograph circuit for measuring rectifier arc drop

The average arc-drop voltage is obtained from the oscillogram by determining the average ordinate of the segment of the arc-drop voltage wave within the limits of the conduction period by means of a planimeter. The arc-drop loss is equal to the product of this voltage multiplied by the total cathode current measured simultaneously with the taking of the oscillogram.

On small rectifiers where inductive effects are negligible and the arc-drop voltage wave is essentially flat, the average arc-drop voltage may be measured directly on a d-c voltmeter by using a comparison method. In this method the arc-drop voltage wave is observed on a cathode-ray oscillograph, and the amplification is adjusted so as to obtain a suitable deflection on the screen. The oscillograph element is then switched to a calibrating circuit consisting of a potentiometer with a d-c voltmeter. The oscillograph element is connected to the voltmeter terminals, and the d-c voltage drop is adjusted until the same deflection is obtained on the oscillograph as when viewing the arc-drop voltage.

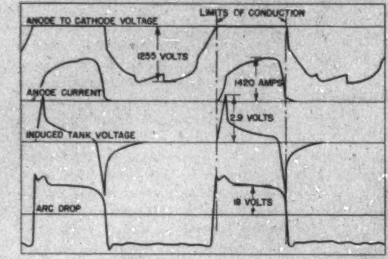


Figure 8. Typical oscillogram of arc drop in ignitron rectifier

Inductive voltage measured by passing anode current through duplicate tank having copper bus connecting anode direct to bottom of tank in mercury pool

The oscillographic method of measuring the arc drop in rectifiers has one advantage over other methods, in that it permits observation of the rectifying action during full voltage operation. This is useful in the study of rectifier action in the course of developmental or field tests.

AUXILIARY LOSSES

The auxiliary losses are determined by measuring the input power to the auxiliaries.

RECTIFIER TRANSFORMER LOSSES

No-Load Loss Tests. Tests for no-load losses may be made in accordance with the standard methods used for other transformers.

Load Loss Tests. The load losses for rectifier transformers may be determined by several methods. One of these is described in the report on AIEE Standard 6.[5] This method requires tests with two or more connections for some circuits. An alternative method which requires only a single connection is described in Appendix A.

VOLTAGE REGULATION

As in the case of efficiency, the voltage regulation of a rectifier unit may be determined from direct measurement. The procedure consists in measuring the output voltage at various values of output current with a constant voltage applied to the primary winding of the transformer. Since the reactance of the supply system effects the apparent regulation of the rectifier unit, the regulation cannot be determined accurately by direct measurement. Therefore, the regulation is ordinarily determined from values of output voltage calculated by subtracting the voltage drops in the rectifier and transformer from the rectifier no-load voltage. The method of calculating the output voltage is described in Appendix B.

POWER FACTOR

The power factor of a rectifier unit may be determined from simultaneous measurements of the input power and input volt-amperes. The reactance of the supply system affects the power factor in the same manner as it does the regulation. For this reason direct measurement of power factor under test conditions must be considered approximate. The power factor is generally determined by calculations based on separately measured characteristics of the rectifier and transformer such as the transformer reactance and magnetizing current.

WAVE FORM

The harmonic composition of the rectifier input and output waves may be determined either by direct harmonic measurements or from analyses of oscillograms. The over-all effect of all the harmonics may also be determined from telephone-influence-factor measurements The wave form of a rectifier is of importance primarily as one of the factors of which a knowledge is desirable in evaluating the influence of the power system on neighboring communication circuits. Since the harmonics produced by a rectifier are affected by the reactance of both the a-c and d-c power circuits to which the rectifier is connected, measurements made under test conditions in the factory may differ from those obtained in the field under actual operating conditions because of the different constants in the two circuits. Methods are available for calculating with reasonable accuracy the wave shape of the voltage and the current in both the a-c and d-c sides of a given rectifier installation when the circuit constants are known.[9]

Testing Facilities

Extensive facilities are required for the testing of mercury-arc rectifiers. There must be provided a complete power system capable of supplying and loading the rectifier to the limit of its capacity, an auxiliary power supply together with the essential rectifier auxiliaries, and an extensive assortment of both standard and special instruments. An arrangement of the rectifier test equipment is shown in Figure 9.

The a-c power system used for testing rectifiers must be of sufficient capacity to maintain substantially sinusoidal wave shape at full rectifier loading and also to give short-circuit currents approximating those obtained in service. Inasmuch as rectifiers must be operated at and above their rated loads for considerable periods during the course of tests, motor generator sets arranged to pump back into the

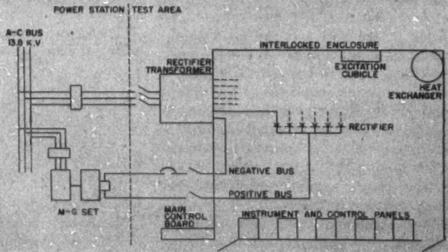


Figure 9. Arrangement of rectifier test equipment

a-c system are used for absorbing the rectifier output.

The generous use of oscillographic instruments is essential in rectifier testing. Oscillographic equipment must include both magnetic and cathode-ray types and must provide facilities for both visual observation and photographic recording. Special instruments and special instrument techniques have been developed for use in rectifier testing. Of these perhaps the most important are the arc-back indicators, the memory oscillograph, and the technique for measuring arc drop. The full use of all available instruments and the development of new instruments is essential to further progress in the analysis of rectifier action.

Appendix A. Transformer Load Loss

The transformer losses may be determined from measurements made by short-circuiting all secondary windings and passing rated sinusoidal current at rated frequency into the primary windings. The losses absorbed by the transformer during this test represent the full-load copper losses when the current is sinusoidal. The total copper losses when operating with nonsinusoidal current in rectifier service may be calculated from this test value as follows:

Symbols

The symbols used in the formulas are defined as follows:

I_p = rated primary current
I_s = rated secondary current

(These primary and secondary currents are the rms values of current for a flat top wave without overlapping.)

R_p = ohmic resistance of primary winding
R_s = ohmic resistance of secondary winding

(These resistances are for copper temperature of 75 degrees centigrade.)

K_p = rated kilovolt-amperes of primary windings
K_s = rated kilovolt-amperes of secondary windings
n = turn ratio
P_1 = losses absorbed by transformer during test
P_2 = ohmic losses for sinusoidal current
P_3 = stray load losses for sinusoidal current
P_4 = ohmic losses for rectangular current
P_5 = total load losses for rectangular current

Calculation of Stray Load Losses

The losses absorbed by the transformer during the test, P_1, should be corrected to a temperature of 75 degrees centigrade.

The current in the short-circuited secondary winding of the transformer during the test will be $I_s' = nI_p$ with rated sinusoidal current of value I_p flowing in the primary winding. The ohmic losses for sinusoidal currents of these values, P_2, are:

$$P_2 = (I_p)^2R_p + (I_s')^2R_s = I_p^2(R_p + n^2R_s)$$

The stray load losses for sinusoidal currents, P_3, are then:

$$P_3 = P_1 - P_2$$

It is assumed that these stray load losses are the same whether the current of the transformer is sinusoidal or has the rectangular wave form obtained during the normal operation of the rectifier. This assumption is justified by a large number of tests carried out by different methods, the variations not being larger with this method than with other methods. With this method, however, it is assumed that the requirements of the various windings of the transformers as to symmetry are entirely fulfilled.

Calculation of Total Load Losses

The ohmic losses for rectangular current, P_4, which are obtained during normal service of the rectifier are:

$$P_4 = I_p^2R_p + I_s^2R_s$$

The total load losses of the transformer during normal service of the rectifier, P_5, are equal to the sum of the ohmic and stray load losses and:

$$P_5 = P_3 + P_4 = P_1 + I_s^2R_s\left[1 - \left(\frac{I_s'}{I_s}\right)^2\right]$$
$$= P_1 + I_s^2R_s\left[1 - \left(\frac{K_p}{K_s}\right)^2\right]$$

When making load-loss tests on the rectifier transformer, the interphase transformers and anode reactors should be disconnected.

For interphase transformers in which the load current is almost entirely direct current, the d-c resistance of the winding may be used in computing the load loss.

Appendix B. Rectifier Output Voltage

The formula for the calculation of the d-c voltage of the rectifier unit at a specified load current is:

$$E_d = E_{d0} - E_x - E_r - E_a$$

where

E_d = d-c voltage at the specified load current
E_{d0} = theoretical d-c voltage (This is the voltage which would be obtained at no-load with no phase control. It is the voltage obtained at the intersection of the regulation curve projected to the zero load line, neglecting the increase in voltage at light load.)
E_x = commutating reactance voltage drop
E_r = resistance voltage drop
E_a = arc voltage drop

The theoretical d-c voltage and the voltage drops may be calculated by using the following formulas:

Theoretical d-c voltage $E_{d0} = \sqrt{2}E_s\frac{P}{\pi}\sin\frac{\pi}{P}$

Commutating reactance voltage drop

$$E_x = \frac{I_cX_cP}{2\pi}$$

Resistance voltage drop $E_r = \frac{W}{I_d}$

in which

E_s = secondary line to neutral voltage of rectifier transformer (rms value)
P = number of phases in simple rectifier
I_c = direct current commutated
X_c = commutating reactance in ohms (line to neutral)
W = load losses of main and interphase transformers
I_d = d-c load current

The commutating reactance X_c, attributable to the main transformer, may be measured by short-circuiting the primary windings and passing sinusoidal current at rated frequency between two secondary anode leads between which a transfer of current occurs during commutation. The commutating reactance anode-to-neutral is obtained by dividing one half of the measured impedance voltage by the applied current.

The transformer load loss W may be determined by the method described in Appendix A.

The method for determining the arc-drop voltage E_a is described in the section on efficiency tests.

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A New-Type Carrier Relay Protection

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THE Hydro-Electric Power Commission of Ontario and the Beauharnois Light, Heat, and Power Company have placed in service recently a new type of power-line-carrier equipment. Channels for high-class interterminal communication are combined with other channels which provide a transfer of impulses for use with the relaying on transmission lines, giving a desired improvement in the speed of operation.

Stability Problem

The 220-kv 25-cycle system of the Hydro-Electric Power Commission is shown in Figure 1, and has been described in the AIEE Transactions.[1] In the earlier paper a record of experience as to line faults was presented and analyzed. The problem of relaying in relation to transient stability was discussed. It was pointed out that studies on an a-c network calculator and operating experience indicated that, in order to maintain stability at the normal line loadings of 150,000 to 170,000 kw per main line, it would be necessary not only to clear two-phase-to-ground and three-phase faults at the line terminal nearest to the fault in 0.2 second approximately but also to open the breakers at the remote terminal simultaneously or in 0.25 or 0.3 second at the most. If this could be accomplished, transient stability would not be a problem on this system which carries an important industrial load of 600,000 to 650,000 kw in large centers of population.

The original basic scheme of phase relaying on the four main 220-kv trunk lines, as stated in the earlier paper, consists of directioned two-stage impedance relays of a conventional type. The first stage operates in one cycle (0.04 second) or less for all faults within the 80 to 90 per cent of the line adjacent to the relays. The second range, covering the whole line and overlapping into the contiguous line sections, is set to operate in 12 to 20 cycles (0.5 to 0.8 second), thus being time selective with the instantaneous relays on these sections. This basic relaying scheme has proved very successful during the development period of the system when loads were lighter; but with present loadings it will be seen that it does not meet the requirements for stability, because clearance is delayed too long at the remote terminal for faults located within the 10 to 20 per cent section at the end of a line. It will be noted also that the line from Beauharnois to Chats Falls is tapped for the infeed from the MacLaren-Quebec Power Company, thus presenting a problem in distance relaying. Moreover, there are two short line sections which have impedances at 25 cycles that are too low for best results with plain distance relaying.

Regarding the long lines, there was doubt as to the desirability of depending on the transmission of a blocking signal to prevent incorrect tripping. Therefore, a carrier relaying system was proposed which could be applied to any or all types of impedance relays without changing their normal use. Since the carrier relaying feature should be supplementary to the original relaying system, the failure of the carrier link should not in any way affect the normal operation of the distance impedance relays which use time delay to select the faulted line. The carrier feature should provide only a speed-up in the relay action.

When the first stage distance relays are set to operate for faults which are within the 80 to 90 per cent of the line adjacent to the relays, the carrier protection is unnecessary except for faults which are in the 10 to 20 per cent sections at the ends of the line. The latter are seldom more than 20 per cent of the total number of line faults. Therefore, the addition of carrier as a protection or improvement in the protection on any transmission system should be balanced against this 20 per cent of the faults. If the carrier should fail to operate for any cause whatsoever, only 20 per cent of the total system faults would be affected adversely, and in no case would the incorrect operation or failure in the carrier system be detrimental to the relay system because failure only increases the clearing time for faults in the end sections of the line.

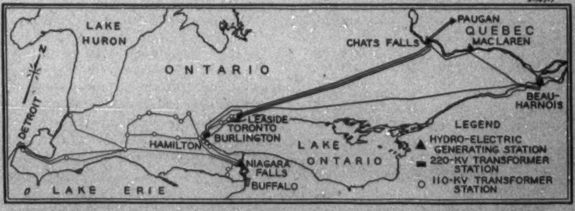
There existed also the problem of communication between the Beauharnois terminal and the commission's plants at Chats Falls and Leaside. Paugan, Chats Falls, Leaside, and stations of the 110-kv system had been interlinked by the commission's overhead telephone lines, but no such provision had been made for MacLaren or Beauharnois. It was desired to link these terminals, particularly the large source at Beauharnois, with the rest of the system by a suitable communication channel which could be of the power-line-carrier type.

Requirements of Carrier System

The relay system should use the directioned instantaneous relay, which protects most of the line, to set up the trip circuit for its own breakers. At the same time this operation would cause an impulse to be sent to the remote end to by-pass the timer on the second impedance range, thereby giving clearances at both ends of the line which are sufficiently near to simultaneous operation to meet the stability requirements. In this scheme, it is not necessary to depend on the passage of the carrier impulse to prevent tripping on external faults; therefore, it would be inherently safer and preferable to the blocking scheme.

The impulse for relaying purposes on one line might be produced by a single frequency or tone in the audio range,

Figure 1. High-voltage lines in the 25-cycle system



Paper 43-147, recommended by the AIEE committee on protective devices and joint subcommittee on power system applications of carrier current for presentation at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943. Manuscript submitted June 16, 1943; made available for printing July 16, 1943.

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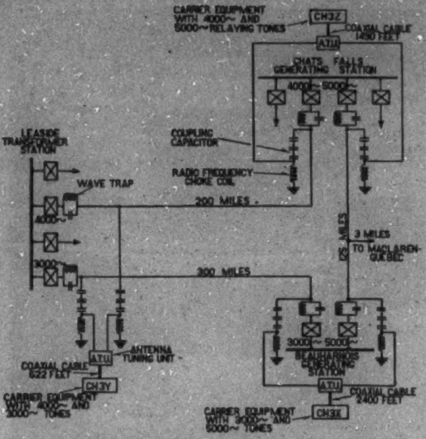
CARRIER EQUIPMENT WITH 4000~ AND 5000~ RELAYING TONES
CH3Z
COAXIAL CABLE 1450 FEET
ATU
CHATS FALLS GENERATING STATION
4000~
5000~
COUPLING CAPACITOR
RADIO FREQUENCY CHOKE COIL
LEASIDE TRANSFORMER STATION
WAVE TRAP
4000~
200 MILES
3000~
300 MILES
125 MILES
3 MILES TO MACLAREN-QUEBEC
ANTENNA TUNING UNIT
COAXIAL CABLE 622 FEET
ATU
CH3Y
CARRIER EQUIPMENT WITH 4000~ AND 3000~ TONES
3000~
5000~
BEAUHARNOIS GENERATING STATION
ATU
COAXIAL CABLE 2400 FEET
CH3X
CARRIER EQUIPMENT WITH 3000~ AND 5000~ TONES

Figure 2. Arrangement of carrier equipment and tones for relaying

which is used for amplitude modulation of the carrier. A separate tone would be assigned to each high-voltage line. One tone generator for transmitting and one tone filter for receiving are required at each end of a protected line, because the impulse for use with the protective relays might originate at either end of the line. An economical design is possible, when only the tone generators and filters which are required are installed at each station. The number of tones which can be used and their place in the frequency spectrum are determined by the sharpness of cutoff in the band-pass filters. Three tones of 3,000, 4,000, and 5,000 cycles were chosen and assigned to the lines, as shown in Figure 2.

The usefulness of this method of carrier relaying depends on whether or not the impulse can be transmitted past the line fault. Tests on lines with grounded conductors showed that, with suitable carrier sets, the impulse could be sent past a ground on one or two wires of the circuit and probably past arcing grounds on all three wires.

Investigation showed that equipment could be obtained wherein both the desired functions of communication and relaying could be combined. Moreover, one set could be coupled to two transmission lines at each terminal, so that one set could be used to serve two lines. It was decided to proceed with a trial installation which would provide communication with Beauharnois and an improvement in the relaying on three lines shown in Figure 2, with the intention that if operation is successful, similar equipment will be installed on the other long lines. The sets and the installation are described in more detail in a later part of this paper.

Auxiliary and Out-of-Step Relays

A schematic diagram of the relay system at one end of the transmission system is shown in Figure 3. A description of the standard relay system for protection of a transmission line is unnecessary. However, the additional relays which are shown in this diagram will be described in the paragraphs hereinafter.

The out-of-step relays are standard "slug" relays of the telephone type, which are equipped with a sealed mercury contact for opening a circuit after a time delay. These operate in the following man-

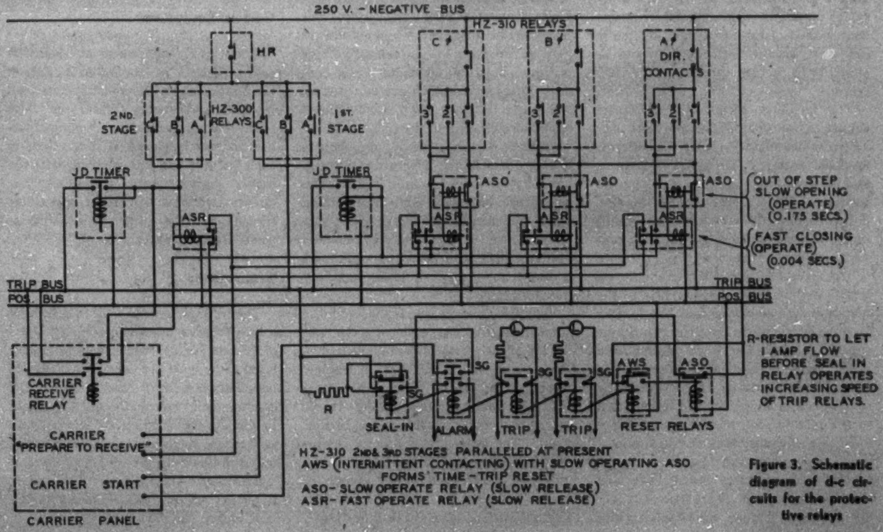
250 V. - NEGATIVE BUS
HR
HZ-310 RELAYS
C
B
A
DIR. CONTACTS
3
2
1
2ND. STAGE
HZ-300 RELAYS
1ST. STAGE
J.D TIMER
ASR
J.D TIMER
ASO
ASR
OUT OF STEP SLOW OPENING (OPERATE) (0.175 SECS.)
FAST CLOSING (OPERATE) (0.004 SECS.)
TRIP BUS
POS. BUS
R-RESISTOR TO LET 1 AMP FLOW BEFORE SEAL IN RELAY OPERATES INCREASING SPEED OF TRIP RELAYS.
CARRIER RECEIVE RELAY
R
SG
SEAL-IN
ALARM
TRIP
TRIP
AWS
ASO
RESET RELAYS
CARRIER "PREPARE TO RECEIVE"
CARRIER START
CARRIER PANEL
HZ-310 2ND & 3RD STAGES PARALLELED AT PRESENT
AWS (INTERMITTENT CONTACTING) WITH SLOW OPERATING ASO FORMS TIME-TRIP RESET
ASO- SLOW OPERATE RELAY (SLOW RELEASE)
ASR- FAST OPERATE RELAY (SLOW RELEASE)

Figure 3. Schematic diagram of d-c circuits for the protective relays

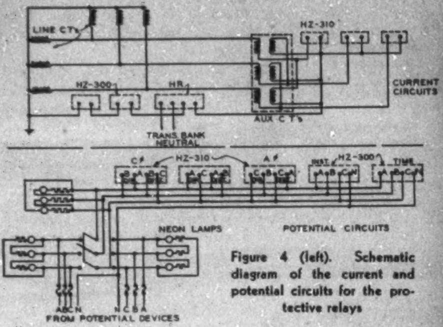

Figure 4 (left). Schematic diagram of the current and potential circuits for the protective relays

ner: If the system should become unstable, all three of the second-stage impedance relays will operate during the swing, and open the trip circuit of the first-stage impedance relays. If the system should remain out of step, the second-stage relays would permit tripping ultimately through the contacts of the *JD* timer. However, if the system should swing back, the first-stage impedance relays will reset, followed by the second-stage impedance relays and the type *ASO* relays, provided the *JD* timer has not closed its contacts. If the out-of-step condition progresses or revolves faster than the time setting of the *ASO* relays, the trip circuit will be closed through the contacts of the first-stage impedance relays. The operation of the first-stage relays on faults within the 80 to 90 per cent section of the line is not affected in any way, as the trip circuit would be closed prior to the operation of the *ASO* relays.

The auxiliary relays type *ASR* are similar to the type *ASO*, except that their action is not delayed. Their function is twofold: first, they operate the *JD* timer, and second, they prepare the carrier set for reception of the tone or impulse, if the carrier equipment happens to be in use for transmitting a telephone conversation at the time.

The type *ASO* and *AWS* relays for resetting the trip circuit are similar to the relays described heretofore, and their function is merely to keep the trip-circuit type *SG* relays in the closed position for a predetermined time, during which the carrier transmitter is started and the appropriate relaying tone is transmitted to the remote station. This time delay must be sufficiently long to trip the local breakers properly and to permit the reception of the relaying tone at the far end of the line where the contacts on the timer for the second-stage impedance relays must be by-passed long enough to permit tripping the breakers.

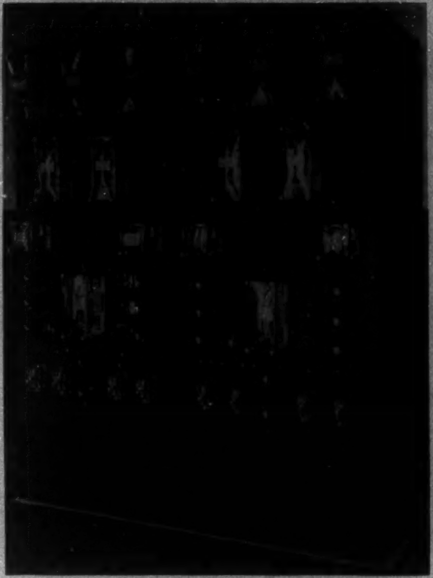

Figure 5. Front view of panel showing the protective relays

The current and potential circuits for the protective relays are shown in Figure 4, which contains some points of interest. Delta connection of the neon lamps was necessary because they would not always "strike" on the phase-to-neutral voltage of approximately 60 volts. Star-connected current transformers are used as the source for both the phase-to-neutral impedance relays and the phase-to-phase impedance relays. The zig-zag connection of the auxiliary current transformers has some advantages: first, it allows the use of ground relays while maintaining delta equivalent currents in the phase relays, and second, the volt-ampere burden on the main current transformers is less than straight isolating auxiliary transformers.

Figure 5 shows a general view of the power-line protective-relay panels. At the top of the panels are the phase relays. The ground relays are next, and below these are the timing relays. The type *HR* directioned ground-current relays are in the center of the panels farther down. The type *ASO* relays for out-of-step operation are on the right of the type *HR* relay, and the "prepare-to-receive" relays type *ASR* are at the left. The bottom line of relays are the "trip", "carrier start", "alarm," and "seal-in" relays. The neon lamps are without covers. The telephone-type lamps are used for trip-circuit supervision.

Carrier Equipment

The carrier equipment at each terminal consists of the following units: the carrier transmitter which is connected by a carrier frequency transmission cable to the line tuning unit, the coupling capacitor by which carrier is impressed on each line, the line trap which isolates the carrier currents from the power frequency switching equipment and transformers, and the control unit which includes the carrier receivers.

Transmitter

The transmitter is capable of delivering a maximum of approximately 400 watts of carrier energy, which can be modulated at least 90 per cent, into a load of nominal value of 70 ohms. This is the characteristic impedance of the carrier frequency transmission cable employed to transmit the energy to the line tuning unit and thence to the coupling capacitor and the high-voltage line. The carrier frequency range is 145 to 205 kilocycles. Low temperature-drift quartz crystals in duplicate are employed as the primary source of carrier energy.

The carrier circuits consist essentially of a crystal oscillator followed by a low-power buffer amplifier, a driver amplifier, an intermediate power amplifier, and a final power amplifier. All circuits are tuned with the exception of the buffer amplifier, which is impedance coupled to the driver. The driver may be connected as a master oscillator by a single switching operation. The output circuits are designed to couple to a transmission cable or other nonreactive load of approximately 70 ohms and to pass the sidebands represented by modulating frequencies up to 6,000 cycles per second without serious attenuation.

The power output of the transmitter may be reduced to a minimum of approximately 15 watts. Power reduction is accomplished in three coarse steps corresponding to approximately 165, 50, and 15 watts. The method used consists essentially of inserting resistance in the plate circuit of the final amplifier stage, thus reducing the net plate voltage. The primary reason for thus reducing the power is to minimize interference on other services, since, under normal conditions and for short distances, the full power is not required for telephony. However, the power must be raised to its full value when a signalling tone is to be transmitted, so a contactor is employed to shunt the resistance out when signalling is required.

The audio-frequency system consists of an input amplifier stage, a driver stage, and a push-pull modulator stage. The system was specially designed to give an audio characteristic which is substantially uniform from 100 to 6,000 cycles. The total harmonic distortion with full carrier power and 90 per cent modulation depth is less than five per cent. The carrier noise is more than 45 decibels below 90 per cent modulation.

Included in the transmitter is a changeover relay actuated primarily by the voice-operated devices and the tone signalling circuits. This relay transfers the output circuits from "received" to "transmit", besides controlling the carrier. The carrier transmission is started by applying plate voltage to one of the lower stages. In the "stand-by" condition, all other plate voltages remain on, and the anode currents are reduced by appropriate grid bias voltages. The crystal stage is allowed to oscillate continuously, as crystals on these carrier frequencies are often slow in starting. By the use of suitable shields and filters, any pickup in the associated receiver is avoided.

Control Unit

The control cabinet includes the following units: two carrier receivers, a receiving amplifier containing the voice-operated disabling device, and an amplifier for the calling loudspeaker, a transmitting amplifier containing the voice-operated carrier-control device and calling oscillator, two low-pass filters, two tone generators, two filter amplifiers, and a calling unit containing the hybrid coil, auxiliary relays, and microphone supply.

Each of the carrier receivers consists of a seven-tube unit, designed for the reception of frequencies between 140 and 200 kilocycles, and comprising a tuned radio-frequency amplifier controlled by an amplified automatic volume control circuit. The input circuits to the receivers include two wave traps and an input attenuator, together with a muting relay which blocks the receiver while the transmitter is operating. This relay is controlled by contacts on the antenna changeover relay, in such a way as to close the contacts on the muting relay before the transmission of carrier begins, and thereby eliminate at the receiver output any pickup from the carrier. This pickup is quite appreciable and is due to a large extent to the capacity effects at the antenna changeover relay. The wave traps are incorporated to provide mitigation of adjacent channel interference. The input attenuator provides a coarse adjustment of carrier input level to ensure that the normal incoming carrier will work the *AVC* circuits properly and to assist in overcoming the effects of noise. Since the noise level is relatively high, the receiver is relatively insensitive, requiring a minimum input of 45 millivolts

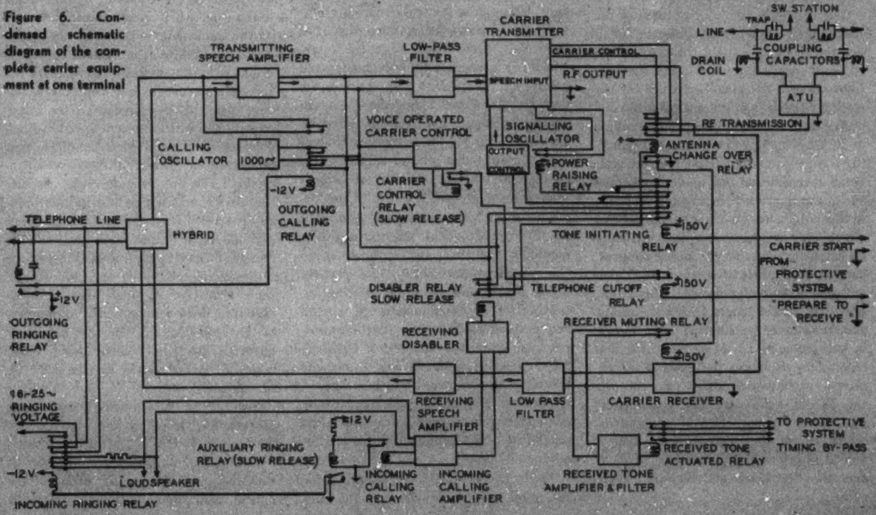
CARRIER TRANSMITTER
SW. STATION
TRAP
LINE
TRANSMITTING SPEECH AMPLIFIER
LOW-PASS FILTER
CARRIER CONTROL
SPEECH INPUT
R.F. OUTPUT
DRAIN COIL
COUPLING CAPACITORS
A.T.U.
RF TRANSMISSION
VOICE OPERATED CARRIER CONTROL
SIGNALLING OSCILLATOR
OUTPUT CONTROL
CALLING OSCILLATOR
1000~
ANTENNA CHANGE OVER RELAY
POWER RAISING RELAY
-12V
CARRIER CONTROL RELAY (SLOW RELEASE)
OUTGOING CALLING RELAY
TELEPHONE LINE
HYBRID
150V
TONE INITIATING RELAY
CARRIER START
FROM PROTECTIVE SYSTEM
DISABLER RELAY SLOW RELEASE
TELEPHONE CUTOFF RELAY
150V
"PREPARE TO RECEIVE"
12V
OUTGOING RINGING RELAY
RECEIVING DISABLER
RECEIVER MUTING RELAY
150V
16-25~ RINGING VOLTAGE
RECEIVING SPEECH AMPLIFIER
LOW PASS FILTER
CARRIER RECEIVER
12V
TO PROTECTIVE SYSTEM
TIMING BY-PASS
AUXILIARY RINGING RELAY (SLOW RELEASE)
RECEIVED TONE ACTUATED RELAY
-12V
LOUDSPEAKER
INCOMING CALLING RELAY
INCOMING CALLING AMPLIFIER
RECEIVED TONE AMPLIFIER & FILTER
INCOMING RINGING RELAY

Figure 6. Condensed schematic diagram of the complete carrier equipment at one terminal

Figure 7. Front view of carrier transmitter and control cabinet

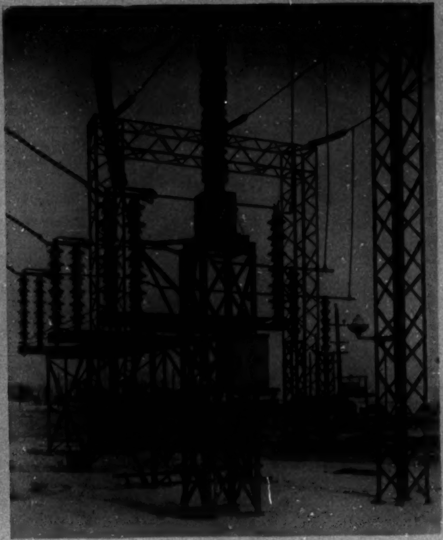

Figure 8. Antenna tuning unit and 220-kv coupling capacitor

for 110 milliwatts ouput. The normal input to the receiver may be as much as one or two volts, depending on transmitter power and attenuation between sending and receiving stations.

The receiving amplifier consists of the following: a single-stage speech amplifier to raise and control the level of the received speech prior to feeding the telephone line, a two-stage amplifier for the calling circuits, and a voice-operated disabling device. The output of the calling amplifier is divided two ways, part operating a conventional loudspeaker, and part being rectified for the operation of the various calling and ringing relays. The disabler includes gas-filled rectifier tubes which are triggered by the incoming speech from the radio frequency receiver, causing the closure of a relay which acts to prevent the operation of the voice-operated carrier control if an attempt should be made to transmit while speech is being received. Moreover, the disabler acts to block the passage of received speech which might pass through the hybrid and enter the transmitter. The relay is a slow-release type and remains closed for approximately half a second after cessation of speech, to avoid operation between syllables or short pauses.

The transmitting amplifier is similar to the receiving amplifier and includes the following: a single-stage speech amplifier to raise and control the level of the speech appearing on the telephone line before it enters the transmitter, a voice-operated carrier-control device and a 1000-cycle calling oscillator with amplifier. The carrier control circuit is essentially the same as the disabler which has been described, except that the relay, which is governed by the speech-triggered gas tubes, is arranged to operate the carrier control and changeover relay in the transmitter. The relay actuated by speech is arranged for a releasing time of approximately one quarter second, so as to hold in between syllables and short pauses in the speech.

One of the low-pass filters is connected in the output from the transmitting amplifier, and the other in the input to the receiving amplifier. The purpose of the former is to avoid modulating the transmitter by components of speech above about 2,500 cycles. If these components lie in the vicinity of 3,000 to 5,000 cycles, they would tend to operate the relaying equipment which is responsive to these frequencies. The purpose of the latter is to exclude the signalling tones from the telephone line at the receiving end.

The tone generators are identical for the three frequencies in use, the only difference being in the constants of the oscillating circuit. The oscillator functions at all times, and the amplifier following it is blocked off at all times except when it is required. This blocking is removed when a tone initiating relay on the transmitter is closed by the operation of the 25-cycle protective system.

The filter amplifiers are identical for the three frequencies in use, the only difference being in the elements of the band-pass filters which are employed. Each consists of a two stage amplifier that is bridged on the output of the radio receiver. The first stage serves to isolate the filter from the line and to allow control of level. The second stage is a power pentode whose output is rectified, and the resultant direct current is used to actuate a contactor. Between the two stages, there is inserted a single section band-pass filter with a band width of approximately 400 cycles. The mid-band frequencies are 3,000, 4,000, and 5,000 cycles. The contactor is energized by the rectified output of the amplifier following the filter, and its contacts operate into the 25-cycle protective system.

The calling unit is a combination of subsidiary apparatus. It includes the hybrid coil and balancing network which couple between the two-wire telephone line and the four-wire communication channel represented by the transmitter input and the receiver output. A small rectifier of the dry-disk type with a filtering system is the supply for the local hand-set. The unit also carries sundry relays associated with the calling system. The low-frequency ringing current on the telephone line is converted into 1,000-cycle current which modulates the carrier. At the receiving end, the 1,000-cycle current operates both the loudspeaker and the relay which impresses the low-frequency ringing current on the called telephone line. The loudspeaker is cut off normally, being in operation only when a call is coming in.

Line Tuning Unit

This unit is mounted out of doors adjacent to the coupling capacitors. It contains the necessary coupling devices to transfer energy from the carrier frequency transmission cable to the 25 cycle high-voltage lines. Also it contains the loading inductances to tune out the reactance of the coupling capacitors. Two separate coupling and loading systems are contained in the unit, so that energy can be fed into two lines in any proportion. As the carrier frequencies are relatively high, the various coils are wound of stranded high-frequency cable. Air core coupling transformers are employed.

Operation

A condensed schematic diagram of the complete system for one terminal is shown in Figure 6.

(*a*) Signalling Tone Transmission

When a fault occurs near one end of the line which is 300 miles long, an impedance relay at the terminal near the fault closes its contact instantaneously. This operation trips the breaker at the near end, and at the same time starts the transmission of carrier modulated at 3,000 cycles by energizing the appropriate auxiliary tone-initiating relay which takes complete control of the carrier equipment at that terminal. This relay actuates the antenna changeover relay, which, in turn, mutes the receiver and starts both the carrier and the 3,000-cycle relaying or signalling oscillator. At the same time, the power-raising relay is actuated to increase the power of the transmitter to its full value. The transmitter then emits carrier, modulated only by the 3,000-cycle tone, for as long as the auxiliary tone-initiating relay is kept energized.

If the transmitter be in use at the time when a tone must be transmitted from the same terminal, the antenna changeover relay will be in the "transmit" position already, because of the action of the voice-operated carrier-control device. In this case, the tone-initiating relay overrides the talker, and the power is raised. The complete relaying operation will be as fast, or a little faster, than usual, because the carrier had been turned on previously. Moreover, the speech line is short-circuited to de-energize the voice-operated carrier control, and to avoid using up any of the modulation capability of the transmitter by the speech which is of secondary importance at the moment. As soon as the tone-initiating relay is de-energized, the system is restored to normal, and the remainder of the talker's conversation will be transmitted as usual, the power dropping back to its original level.

If two or three relaying tones are required, they can be modulated simultaneously at the expense of a small reduction in the modulation depth of each. This reduction is not so great as to impair the operation of the relays at the distant end.

(*b*) Signalling Tone Reception

The tones, demodulated from the carrier, appear at the output of the carrier receiver and enter the received tone amplifiers and filters. The low-pass filter blocks them from the receiving speech amplifier and prevents them from being heard by the listener if a conversation is in progress. The band-pass filters in the amplifiers separate the wanted tone from the others, or from speech, and the proper relay is actuated by the received tone. The contacts of this relay put a by-pass across the contacts of the timer on the second impedance range, making this range almost instantaneous.

If the equipment happens to be in use for transmission of one part of a telephone conversation, and a relaying tone is to be received, dependence is placed on the telephone cut-off relay, which is actuated by the second range impedance relays at the same time as the *JD* timer is started. This operation occurs at the same time as the relaying tone is started at the distant end. The telephone cut-off relay opens the circuit to the antenna changeover relay even though it has been closed by the talker. This relay is forced to go to the "receive" position, and the receiver is unblocked. The equipment is thus prepared to receive the tone at the same time as the carrier modulated by the tone arrives.

If the second range impedance relays energize the telephone cut-off relay at the same time as the tone-initiating relay is energized by the trip relay, then the tone-initiating relay overrides all else and the sequence proceeds correctly.

The time which is lost between energizing the tone-initiating relay at the end of the line nearer to the fault and the closure of the relay by the received tone at the distant end is in the order of 0.9 of a cycle on 25 cycles per second, if the equipment at both ends is in the stand-by condition. If the carrier equipment is in use for telephony, and the transmission is in the same direction as the relaying tone must be sent, the lost time is somewhat less, being about 0.5 of a cycle on 25 cycles per second. If the equipment is in use for telephony and the transmission is in the opposite direction to that which is required for the relaying tone, the total time lost is in the order of 1.5 cycles on 25 cycles per second. This is the slowest case because the system must be prepared to receive the tone as described previously.

A general view of the radio-frequency equipment is shown in Figure 7. At this station, these cabinets are 2,400 feet from the antenna tuning unit and about 2,000 feet from the panels on which are mounted the protective relays. The antenna tuning unit and a coupling capacitor are shown in Figure 8.

Conclusions

This carrier relay scheme which uses a tone as a transfer trip impulse, as described above, has several advantages. Standard protection relays of any type may be used. Only one carrier set is required at each station, coupled to as many lines as need this protection. The number of relaying tones or channels is limited only by selectivity of the band-pass filters. The carrier system operates as a telephone circuit normally, and only as a carrier relaying feature for an interval of one second for every line fault. A failure of the carrier increases the clearing time only for those faults which are in the end sections.

Reference

1. The 220,000-Volt System of the Hydro-Electric Power Commission of Ontario—II, A. H. Frampton, E. M. Wood. AIEE Transactions, volume 60, 1941, pages 1215-21.

Temperature Limits and Measurements for Rating of D-C Machines

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Synopsis: Existing industrial practice for determining the temperature rise of d-c machines is based on measurements with thermometers, placed on exposed or accessible surfaces of the machine. Tests using resistance measurements with improvements in technique have disclosed the existence of considerably higher internal temperatures than those indicated by thermometers, especially for short-time rated machines. Experience has indicated, nevertheless, that such machines have entirely satisfactory service records.

Greater accuracy and consistency are possible with the resistance method, with proper measurement techniques, and it affords a truer indication of winding temperatures. This paper, therefore, suggests that the resistance method should be more generally used and should ultimately be recognized in the Standards as is now the case for railway motors. With this in view, detailed recommendations for resistance-measurement technique are presented.

Correlation of the temperature rises determined by the resistance method with those measured by the thermometer method is necessary if resistance methods are to supplant the now standard thermometer methods. This paper shows the relation existing between temperature rises measured by thermometer and those measured by resistance on variously ventilated and insulated machines.

Values for temperature limits measured by resistance for continuous and short-time rated class-*A* and class-*B* insulated d-c machines are presented. These are consistent with the recently published AIEE Standard 1*A* "Report on General Principles for Rating of Electrical Apparatus for Short-time, Intermittent or Varying Duty." The information presented in this paper is intended to apply primarily to low-voltage (approximately 600 volts maximum) d-c machines with form-wound armature coils and conventional types of field coils.

Temperature Measurements

STANDARDS for rating d-c machines provide temperature limitations for the protection of winding insulation. Although there are three recognized methods of temperature measurement, namely,

1. Thermometer method
2. Resistance method
3. Embedded-detector method

the present AIEE practice is based on the thermometer method.

The embedded-detector method is used to determine interior temperatures at designated locations within windings. It is also used in small motor windings, with thermocouples, in specified locations inaccessible to mercury or alcohol thermometers.

The thermometer method is perhaps the easiest to apply, at least for open-type machines, and requires the least amount of equipment. Although the thermometer method has been used for many years with varying degrees of success, it indicates a temperature that is generally less than the internal temperature of the winding being measured. The thermometer may register a temperature very much less than the average copper temperature, depending on the size of motor, length of heat run, ventilation, internal temperature, and insulation. If within a motor one winding is 20 degrees centigrade hotter than the other winding, the thermometer on the cooler winding may actually register a temperature higher than the average copper temperature of that winding. In practically all cases, however, and especially for short-time ratings, the thermometer on the hottest winding will register a temperature less than the average copper temperature of that winding.

The thermometer method has met the needs of industry for many years, and is still adequate for practical purposes. However, it is difficult to obtain consistent results by the thermometer method, because of variations in surface conditions, thermometer locations, and ventilation. Quite often heat runs on duplicate machines or on the same machine

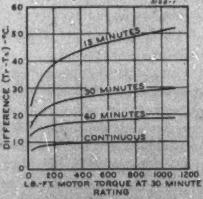

Figure 1. Temperature difference of resistance rise minus thermometer rise versus size of motor for various lengths of heat runs of class-*A*-insulated totally enclosed motors

T_r = Temperature rise by resistance, degrees centigrade

T_t = Temperature rise by thermometer, degrees centigrade

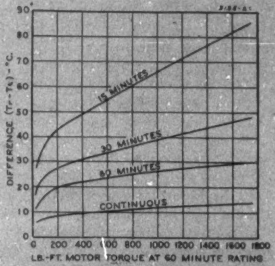

Figure 2. Temperature difference of resistance rise minus thermometer rise versus size of motor for various lengths of heat runs of class-*B*-insulated totally enclosed motors

T_r = Temperature rise by resistance, degrees centigrade

T_t = Temperature rise by thermometer, degrees centigrade

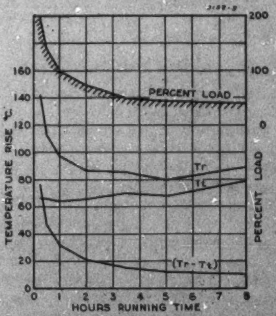

Figure 3. Temperature rise versus length of heat run on a totally enclosed motor

Shaded curve shows per cent load applied for each heat run

T_r = Temperature rise by resistance, degrees centigrade

T_t = Temperature rise by thermometer, degrees centigrade

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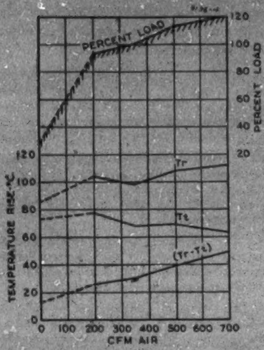

Figure 4. Temperature rise versus volume of ventilating air for continuous heat runs on a blown motor

Shaded curve shows per cent load applied for each heat run. Zero air heat run taken on totally enclosed motor

T_r—Temperature rise by resistance, degrees centigrade

T_t—Temperature rise by thermometer, degrees centigrade

will not check previous thermometer tests with a reasonable degree of accuracy. Another shortcoming of the thermometer method is that it does not indicate the internal temperature of the winding.

The resistance method gives the average temperature of the copper in the winding being measured. This method requires more equipment than the thermometer method, but, when it is applied with the proper technique, more accurate and consistent results may be expected. The main advantage of the resistance method is that it indicates the average copper temperature within the winding, which is generally closer to the hot-spot temperature in any part of the winding than would be indicated by the thermometer method.

The rise by resistance is, in general, higher than the rise by thermometer, and the former should, therefore, be a better indication of the temperature which limits insulation life.

Temperature Ratings

For many years it has been the accepted practice to limit the temperature rise on d-c machines to predetermined values, depending on the class of insulation, ambient temperature, and degree of enclosure. The allowable temperature rises were derived from hot-spot temperatures, which were established as being the maximum that a particular class of insulation could withstand continuously and give satisfactory winding life. AIEE Standards 5 (D-C Rotating Machines, Generators and Motors) and 45 (Electrical Installations on Shipboard) and American Standard *C*50 (Rotating Electrical Machinery) currently specify that the temperature rise be measured by thermometer, and that the limiting observable rises on insulated windings for a 40-degree-centigrade ambient temperature be as follows:

	Degrees Centigrade	
	Class-A Insulation	Class-B Insulation
Totally enclosed	55	75
Open and semienclosed	50	70

General-purpose machines with a service factor of 1.15 are rated 40 degrees centigrade rise for class-*A* insulation. Although the foregoing temperature-rise limits were established primarily for continuous-rated machines, they have been applied without change or allowance to short-time-rated motors. The most commonly used short-time ratings are 30 minutes and 60 minutes, but 15-minute ratings are also used. The majority of crane motors carry a 30-minute rating, although they may be used in continuously repeated duty-cycle service. Marine-type motors for deck machinery are rated on a short-time basis of 30 or 60 minutes, generally on the former of these two bases. Since the majority of crane motors, and practically all marine deck motors, are totally enclosed, they have been built with short-time horsepower ratings limited by temperature rises of 55 degrees centigrade for class-*A* insulation and 75 degrees centigrade for class-*B* insulation, as determined by the thermometer method of measuring temperature. Many thousands of such motors have been in service for years and have given satisfactory performance. The satisfactory life of the insulation in such short-time-rated motors is due as much to the careful applications of the motors as to the temperature rise on which their rating is based. These motors, although rated on a short-time basis, are generally applied on a duty-cycle basis. In applying such motors, it is common practice to calculate the root-mean-square load, or the average watts loss of the cycle, to make sure that the motor has sufficient thermal capacity, assuming that the duty cycle is to be repeated continuously or for a specified period, so that it can be operated without the allowable temperature rise being exceeded.

Thus it is seen that a short-time horsepower rating is merely a "gauge" which gives the user a conception of the physical size and torque capacity of the motor, but which conveys nothing concerning the continuous thermal capacity of the motor. Although the temperature rise measured by thermometer at the end of the short-time heat run is equal to or less than the allowable rise of 55 degrees centigrade for class-*A* or 75 degrees centigrade for class-*B* insulation, the rise indicated by resistance measurements is greater than these values.

In a previous paper[8] data were presented to show the difference between temperature rises of d-c machines when determined by the thermometer method as compared with those determined by the resistance method. These data were taken from tests on class-*B*-insulated totally enclosed motors. The present paper presents additional data on differences between rise by thermometer and rise by resistance. Information is given on totally enclosed, self-ventilated, and blown motors, with class-*A* and class-*B* insulation, and on various sizes of motors.

The amount by which the rise measured by resistance exceeds the rise measured by thermometer for various time ratings for various sizes of totally enclosed motors is shown in Figures 1 and 2. The temperature-rise differential (resistance rise–thermometer rise) is shown plotted as a

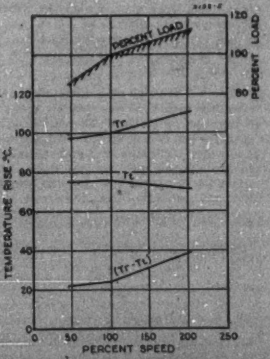

Figure 5. Temperature rise versus speed for continuous heat runs on a self-ventilated motor

Shaded curve shows per cent load applied for each heat run

T_r—Temperature rise by resistance, degrees centigrade

T_t—Temperature rise by thermometer, degrees centigrade function of the 30-minute rated torque for a line of class-*A* motors and as a function of the 60-minute rated torque for a line of class-*B* insulated motors. These curves are plotted in this manner to show the general relation that exists between the various time ratings for a given size of motor, as well as the relation for a given time rating for various sizes of motors, the torque rating being an indication of the physical size of the motor. All heat runs from which the curves of Figure 1 were derived were made to give approximately 55 degrees centigrade rise by thermometer on the hottest winding. Likewise the heat runs for Figure 2 were made to give approximately 75 degrees centigrade rise by thermometer. In practically all cases, the armature was the hottest winding. Hence, the curves of Figures 1 and 2 are all based on armature temperatures. All thermometer measurements were made with mercury thermometers.

From these curves, general relations with respect to temperature differential (resistance rise–thermometer rise) can be expressed as follows:

1. For a given size of motor and thermometer rise, the shorter the time rating the greater the temperature differential. The reason for this is that, the ratio "watt-hours absorbed to watt-hours dissipated" is greater, the shorter the time. The watt-hours absorbed by the windings raise the copper temperature (resistance rise) while those dissipated raise the surface temperature (thermometer rise).

2. For a given time rating and thermometer rise, the larger the machine, the greater the temperature differential. The reason for this is that, the larger the machine, the greater the ratio of watt-hours absorbed to watt-hours dissipated.

3. For a given size of machine and time rating, the higher the thermometer rise, the greater the temperature differential. The reason for this is that, for a given insulation, more watts per square inch are required to produce a higher thermometer rise on the surface of the insulation, and the greater the watts per square inch, the greater the temperature differential.

Figure 3 shows temperature rises both by the thermometer and by resistance obtained from a series of heat runs made on one totally enclosed motor. The shaded curve shows the value of load that was applied for each heat run, expressed as a percentage of the one-hour load.

Figure 4 shows the effect of ventilating air on the differential between resistance rise and thermometer rise for a blown continuous-rated constant-speed motor. Figure 5 shows a similar effect on a self-ventilated motor on which heat runs were taken at various speeds obtained by holding constant field strength and varying the armature voltage. From Figures 4 and 5, it can be seen that, for a given size of motor, the more the ventilating air, the greater will be the difference between the rise by resistance and the rise by thermometer for continuous heat runs with approximately the same thermometer rise.

Figures 1 to 5, inclusive, are based on an average of many tests and are intended to show some of the various factors that cause a variation in the difference between resistance and thermometer rises.

From the foregoing it can be seen that, although it has been standard accepted practice to rate machines 55 degrees centigrade (class *A*) or 75 degrees centigrade (class *B*) rise by thermometer, the rise by resistance exceeds that by thermometer even on totally enclosed continuous heat runs. AIEE Standard 1 now recognizes a temperature differential of ten degrees centigrade between resistance and thermometer measurements for continuous apparatus. This is very close to that obtained from tests on totally enclosed motors, as shown in Figures 1, 2, and 3. However, especially for short-time rated motors, the resistance rise often will exceed the thermometer rise by an amount considerably greater than ten degrees centigrade.

The question that immediately arises is how to reconcile the higher internal temperatures disclosed by this investigation and the satisfactory service experience recorded with the generally recognized temperature-rise limits of 60 degrees centigrade by resistance for class *A* and 80 degrees centigrade for class *B*, given in AIEE Standard 1 and various American Standard and International Electrotechnical Commission Standards. However, the temperature-rise value desirable for any given case depends on many factors besides the insulating materials themselves. The ultimate choice is largely a matter of economics, depending on the relative importance of weight, size, reliability, cost, and life expectancy.

For example, it is well known that 120 degrees centigrade rise is allowed for class-*B* insulated railway motors (with 25 degrees centigrade ambient temperature assumed), whereas only 60 degrees centigrade rise by resistance is normally allowed for the high-voltage armature windings of large alternators with class-*B* insulation (on the basis of 40 degrees centigrade ambient temperature). In the particular case of low-voltage d-c machines, with form-wound armature coils and the usual type of field structure that are alone considered in this paper, the following factors are favorable to the use of higher internal temperatures:

1. The low voltages (approximately 600 volts maximum), the mechanical requirements, and the form-wound construction necessarily result in extremely low dielectric stresses.

2. The low voltage and corresponding small insulation thickness considerably reduce difficulty from varnish-solvent expulsion and coil swelling that limit high-voltage coil temperatures.

3. The relatively short armature-core lengths customary for d-c machines minimize the difficulties from thermal expansion, which are serious in large a-c machines.

4. The type of construction of the d-c rotating armature, including the commutator and the end-binding bands, restricts the exposure of the windings, resulting in a relatively greater difference between internal and external winding temperatures than in the case for induction and synchronous machines. At the same time, the parts of the insulation subjected to the higher temperatures are less exposed to oxidation, moisture, and atmospheric impurities.

Conclusions

In view of the desirability of internal rather than external temperatures as a basis of rating, and the greater accuracy and consistency that may be obtained, it is recommended that improved methods of temperature measurement by resistance be included in the AIEE test code for d-c machines and considered for later adoption as a basis of rating in the Standards. For the latter purpose, the following values of limiting observable temperature rises for both fields and armature windings of d-c machines determined by the resistance method are suggested for consideration:

Clas of Insulation	Limits of Observable Temperature Rises Determined by the Resistance Method for 40 Degrees Centigrade Ambient (Degrees Centigrade)			
	Continuous	One Hour	30 Minutes	15 Minutes
A	65	75	85	95
B	100	110	125	140

1. The values agree with AIEE Standard 1*A*, Table II, short-time ratings, except for the 15-minute rating.

2. The values for class-*B* insulation agree with AIEE Standard 1 Table V, preferred values of standard observable temperature rise for short-time or other special ratings.

3. The values are somewhat more conservative than railway motor values AIEE Standard 11, Table II.

The suggested values are intended to apply to low-voltage (approximately 600 volts maximum,) d-c machines with form-wound armatures and conventional field coils. The values are not intended to apply to other classes of machines where the type of construction and nature of application differ.

Satisfactory experience has been obtained with the suggested values of rise by resistance in machines that meet present thermometer standards. For continuous-rated highly ventilated machines, these values will limit the rise by resistance to a value less than is permissible with the present thermometer standards.

Appendix I. Resistance Measurements

In order to promote the general acceptance of the resistance method of determining temperature rises, and thereby further improve the technique of this method, the following description of a method for determining temperature rise by resistance is offered.

The temperature by resistance method gives the average temperature of the winding being measured. This method consists of comparing the resistance at the end of a heat run (hot resistance) with the resistance at a known temperature (cold resistance). The temperature rise at the end of a heat run is determined by the following formula, which applies to copper windings only:

$$T-t_a=\frac{R_T-r_t}{r_t}(234.5+t)+t-t_a \qquad (1)$$

in which

R_T = resistance in ohms of the winding at temperature T degrees centigrade (this will be the hot resistance measured at an unknown temperature T at the end of a heat run)

r_t = resistance in ohms of the winding at temperature t degrees centigrade (this will be the cold resistance measured at a known temperature t)

t_a = the mean ambient air temperature in degrees centigrade during the last quarter of the duration of the test

$T-t_a$ = the temperature rise in degrees centigrade of the winding above the ambient temperature

A convenient method for determining temperature rise by resistance is as follows:

1. Plot the relation of per cent change in resistance as abscissa against the temperature in degrees centigrade as ordinate. This straight-line relationship is shown in Figure 6 in which a reference temperature of 25 degrees centigrade has been chosen. It is recommended that the scale for the per cent change in resistance be chosen large enough so that it can be read accurately to the first decimal point.

2. Measure the cold resistance of each winding in the motor, and record the temperature of the windings as determined by placing thermometers thereon. Correct the cold resistance from the measured temperature (thermometers on windings) to 25 degrees centigrade. A convenient table for this correction is Table I.

3. Measure the hot resistance at the end of the heat run, and from the percentage increase in resistance $100(R-r_{25})/r_{25}$ read from Figure 6 the corresponding temperature. Subtract the ambient or cooling-air temperature t_a to obtain the temperature rise.

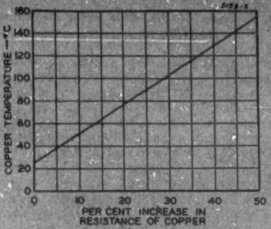

Figure 6. Temperature versus per cent increase in resistance of copper

4. To obtain the temperature rise at the instant of shutdown (at time the power was cut off), record the time at which each hot resistance was taken, the time being measured from the instant of shutdown. Take a series of hot-resistance readings at approximately two-minute intervals for approximately 20 minutes. These points can then be plotted as temperature rise (by resistance) as ordinates and time as abscissae. A curve drawn through these points and extrapolated back to zero time will give the temperature rise at the instant of shutdown. A typical cooling curve is shown in Figure 7.

In some cases, especially at the end of short-time heat runs, the slope of the cooling curve changes rapidly, and there is a large change in temperature during the first few minutes after power is off. It is therefore important to bring the machine to a standstill and take the first reading on each winding as quickly as possible. The cooling curve for the hottest winding generally has the greatest slope, and, since it is the limiting winding in determining the rating of the machine, care should be exercised in determining accurately the zero-time temperature rise on this winding.

There are two methods in common use for determining resistance

(*a*). The drop of potential, or voltmeter–ammeter method. This method is used to best advantage on resistance greater than five ohms.

(*b*). The comparison method in which the unknown resistance is compared with a known resistance by some suitable bridge. For measuring resistances less than five ohms, it is recommended that a Kelvin double bridge be used.

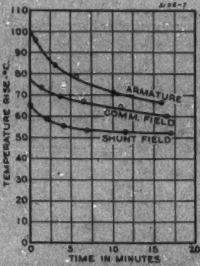

Figure 7. Typical cooling curves determined by the resistance method

Time is measured from instant of power off, taken after a continuous blown heat run

The success of measuring resistance by either the voltmeter–ammeter method or the bridge method depends to a great extent on the wiring and connections between the instruments or bridge and the machine windings being measured. Since for most d-c motors there are at least three elements to be measured—armature, commutating field, and main field—it is important that the wiring of the test table be so arranged that readings on all windings may be made at the end of a heat run in the minimum amount of time and by using only one set of instruments and/or one bridge. To accomplish this in the minimum time and with the least confusion, it is recommended that switches be used in the voltage circuits.

A convenient arrangement of test-table wiring for measuring cold and hot resistances is shown in Figure 8. Such an arrangement can be installed on individual test tables or on a portable resistance table which can be moved to any test stand.

Figure 8 shows the test-table wiring, the motor windings, and a Kelvin double bridge. The bridge is connected to the wiring by an ammeter jack and a voltmeter jack and can, therefore, be replaced readily by an ammeter and a voltmeter. In this diagram are included two extra sets of voltage leads, in addition to the three connected to the field windings which may be used for additional field windings, such as in a motor–generator set. The forks are used for measuring armature resistance. When field resistances are being measured, current is circulated through all field windings. By closing the proper switches, the voltage leads for each winding can be connected to the bridge individually.

Table I

Degrees Centigrade Temperature of Cold Resistance	K	Degrees Centigrade Temperature of Cold Resistance	K
15	1.040	26	0.996
16	1.036	27	0.992
17	1.032	28	0.989
18	1.028	29	0.985
19	1.024	30	0.981
20	1.020	31	0.977
21	1.016	32	0.974
22	1.012	33	0.970
23	1.008	34	0.967
24	1.004	35	0.963
25	1.000		

Multiply cold resistance by K to obtain resistance at 25 degrees centigrade.

The following precautions should be observed in wiring a table, as shown in Figure 8, and in taking resistance measurements to determine temperature rise. Although some of the precautions are not necessary for the voltmeter–ammeter method, they are necessary for the double-bridge method.

1. Each individual voltage lead between the bridge and the field windings should have approximately the same resistance as the other lead in the same pair and should be of relatively low resistance, preferably less than 0.1 ohm per lead.

2. All connections in the voltage leads must be clean and tight. All permanent connections should be soldered.

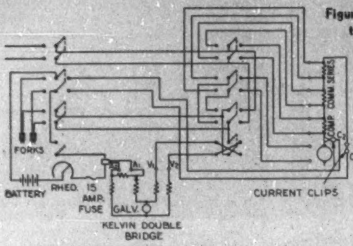

Figure 8. Schematic wiring diagram of test table for measuring resistance

3. All switches must be kept clean and free of dust or corrosion. The swivel connection on all switches between the handle and the base should be shunted with flexible copper shunts, one end of which is soldered to the switch blade and the other end to the connecting wire.

4. Pigtails should be soldered to the field-coil leads, so that the voltage leads can be bolted tightly to the pigtail and not connected at the joint where one field-coil cable connects to another cable.

5. For measuring armature resistance, the forks should be used. In each fork are a current and voltage lead soldered to brass pins ($^1/_8$ inch diameter), insulated from each other and held rigidly in the handle so that pressure can be applied without moving the pins. The forks should be applied to the ends of two commutator segments, securing the maximum span of segments that can be obtained between two adjacent brushholders. The armature should be so positioned that the brushes will not short-circuit armature coils between the span of the forks. The forks should be pressed firmly against the segments so as to break through any oxide film and make good contact with the copper.

6. Cold and hot resistances should be measured under as identical conditions as possible. For the armature, the bars used for determining cold resistance should be marked, and the same pair should be used in measuring hot resistance. In measuring the resistance of field coils, the voltage leads should be connected in such a manner that it will not be necessary to use the reversing switch after cold resistance is taken. The voltage leads should not be removed or changed between the times of taking cold and hot resistance. The current clips should be placed in the same relative position for both cold and hot resistance—this is very important, as will be explained later.

7. In order that errors in reading the double bridge may be reduced to a minimum, the lowest multiplying factor (see Appendix II) with which the bridge can be balanced should be employed. The measurement is independent of the current, but, the higher the value of current within the current capacity of the bridge, the greater will be the sensitivity of the bridge and, therefore, the accuracy of the readings.

8. Cold resistance should not be taken until all windings are within three degrees centigrade of room temperature.

9. As soon as power is cut off at the end of the heat run, the machine should be stopped as quickly as possible and all voltmeters disconnected, making certain that there are no paths in parallel with the windings to be measured.

10. The resistance of the current path between A_1 and the point where the current clip C_1 (Figure 8) connects to the series cable should be relatively low and of the same value for cold and hot resistance. The reason for this is explained as follows:

Refer to Figure 9 which shows the schematic connections of the Kelvin double bridge connected to an unknown resistance r_x.

The connection between A_1 and the point where V_1 lead is connected to r_x will be referred to as the d link. In Figure 9 the resistance of d may be zero or infinity, and as long as

$$(r_1+r_{v2})/r_2=(a+r_{v1})/b$$

then

$$r_x/r=(r_1+r_{v2})/r_2$$

where a, b, r_1, and r_2 are the resistances of the ratio arms of the bridge and r_{v1} and r_{v2} are the voltage lead resistances. For a ratio of 0.1, assume $r_1=100$, $r_2=1{,}000$, $a=100$, and $b=1{,}000$. When r_{v1} and r_{v2} are zero, then r_x (unknown) $=0.1r$ (standard). Assume $r_x=0.001$, and each voltage lead r_{v1} and r_{v2} to have one ohm resistance, then an error of one per cent will be introduced, because the standard resistance will have to be adjusted to 0.001 (1000/101) = 0.0099 in order to balance the bridge—that is, zero amperes through the galvanometer. Even in this case, as long as the ratio $(r_1+r_{v2})/r_2=(a+r_{v1})/b$ exactly, then the resistance of the d link, theoretically, has no effect.

Practically, it is not possible to maintain an exact ratio of $(r_1+r_{v2})/r_2=(a+r_{v1})/b$. Therefore, any resistance in the d link will introduce an error. The magnitude of this error will depend on the difference in ratios of the bridge arms, including leads, and the value of the d resistance.

When the bridge is balanced:

$$\frac{r_x}{r}=\frac{r_1+r_{v2}}{r_2}+\frac{d}{r}\left(\frac{b}{a+r_{v1}+b+d)}\right)\times\left(\frac{r_1+r_{v2}}{r_2}-\frac{a+r_{v1}}{b}\right) \qquad (2)$$

In comparing hot to cold resistance to determine temperature rise, approximately the same per cent error would be obtained in the hot resistance as in the cold resistance, thereby minimizing the error in temperature rise, provided that the resistances of the ratio arms, leads, and connections do not change between times of taking hot and cold resistance, and the d link resistance changes in proportion to the standard resistance r.

Since small differences in resistance occur in opening and closing switches, in contact joints, and with temperature change of wires, and the d link resistance does not change in proportion to the standard resistance, good practice dictates that the d link should have relatively low resistance, and the two voltage leads should have approximately the same resistance.

From Figure 8 it can be seen that when the resistance of the compensating field is measured, the commutating and series fields and connecting wires are in the d link. This same arrangement should be kept for both cold and hot resistance. Therefore, the current clips must be placed in the same relative position for both cold and hot resistance. For all practical purposes, the addition of this amount of resistance as compared to the winding being measured should introduce little or no measurable error in determining temperature rise, provided that the same relative connections are maintained for both hot- and cold-resistance measurements. The table wiring should be arranged so that it is not possible to connect the battery and rheostat into the d link.

The possible magnitude of error in resistance measurement caused by resistances in the d link and in the voltage leads is discussed in Appendix II.

The discussion in Appendix II is offered to point out the possibility of error in resistance measurement, and each person employing the double bridge will have to determine from actual experience and checking what refinements are necessary to give reasonably accurate results. If too many refinements are attempted, then the task of measuring resistance on heat runs becomes tedious, and the expense and trouble of such refinements may prove unwarranted. It must not be assumed mistakenly that a small amount of resistance in one of the voltage leads makes little or no difference, as is the case when using the voltmeter-ammeter method. Where a voltmeter may have a resistance of 1,000 ohms, 0.1 ohm inserted in one of the leads would give approximately 0.01 per cent error. The same value inserted in one of the voltage leads of the Kelvin double bridge may produce a considerable error, depending on the value of the unknown and the d link resistances.

The circuits shown in Figures 8 and 9 have been in use in one testing department for a number of years in determining temperature rise by resistance and have given reliable results. If such circuits are installed properly and operated with care, reliable results may be expected.

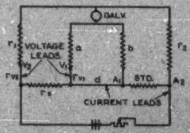

Figure 9. Schematic diagram of Kelvin double bridge

Appendix II. Kelvin Double Bridge

In the double bridge there are a number of adjustments provided in the ratio arms r_1, r_2, a, and b, in order to measure a large range of unknown resistances. For each adjustment $r_1=a$, $r_2=b$, and $r_1/r_2=a/b$. Although in portable forms of the Kelvin bridge $r_1=a$ and $r_2=b$ in most cases, it is not essential that this be so but only that the ratios be equal. The ratio of r_1/r_2 is the multiplying factor which, when applied to the value of observed resistance on the standard, r, gives the value of unknown resistance r_x.

For the purpose of discussion, it is assumed that the double bridge, Figure 9, has three multiplying factors to cover the range of 0.0001 ohm to 1.0 ohm resistance of r_x. It is also assumed that the various resistance arms within the bridge have the following values for each of the three multiplying factors:

Multiplying Factor	Resistance in Ohms		Range of Unknown (r_x) in Ohms
	r_1 and a	r_2 and b	
0.1	100	1,000	0.0001 to 0.01
1.0	500	500	0.001 to 0.1
10.0	1,000	100	0.01 to 1.0

The connecting wires between the bridge and the unknown resistance r_x (Figure 9) will introduce an error in the resistance reading. The magnitude of this error depends on the following factors:

(*a*). Resistance of the voltage leads r_{v1} and r_{v2}.

(*b*). Difference between the resistances of the two voltage leads $(r_{v1}-r_{v2})$.

(*c*). Resistance *d* introduced in the *d* link.

Two possible causes of error may be considered.

Case I where $d=0$. From equation 2 $r_x=r(r_1+r_{v2}/r_2)$. In this case an error will be introduced by the resistance of the voltage lead r_{v2} which will change the ratio of r_1/r_2 to $(r_1+r_{v2})/r_2$. Therefore, low-resistance voltage leads should be used, and it is preferable that each lead be less than 0.1 ohm. With a multiplying factor of 0.1 and $r_{v2}=0.1$ ohm, the ratio becomes 100.1/1,000 which gives an error of 0.1 per cent, a value that may be considered negligible. The resistance of the V_1 voltage lead r_{v1} will have no effect on the results so long as d is zero, a condition that may never be realized in actual practice.

Case II where $d>0$ and r_{v1} or $r_{v2} \gtreqless 0.1$ ohm. In this case a change in ratio of the first term on the right-hand side of equation 2 due to r_{v2} can be neglected, because the error will be 0.1 per cent or less, although the second term becomes significant. The error caused by the resistances of the voltage leads and the d link is directly proportional to the d-link resistance and to the difference $(r_{v1}-r_{v2})$ in resistance between the two voltage leads. Therefore, voltage leads of approximately equal resistance should be used as well as a low d-link resistance to minimize the error.

The following general relations apply when there is a difference in the voltage-lead resistance, $r_{v2}\neq r_{v1}$ and $d>0$.

1. For multiplying factors of 0.1 and 10:

$$\text{Approximate per cent error} = 0.091(r_{v1}-r_{v2})d/r_x$$

2. For a multiplying factor of 1.0:

$$\text{Approximate per cent error} = 0.1(r_{v1}-r_{v2})d/r_x$$

When $r_{v1}>r_{v2}$, the bridge will indicate a value greater than r_x.

When $r_{v1}<r_{v2}$ the bridge will indicate a value less than r_x.

From relations 1 and 2 it can be seen that, the lower the unknown resistance, the greater is the probability of error due to poor wiring and connections. These relations apply only to a limited range of resistance of $(r_{v1}-r_{v2})$ and d. They are, however, approximately correct for values greater than should be permitted in actual practice.

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A New Simple Calculator of Load Flow in A-C Networks

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LOAD-FLOW analysis in a-c networks is increasing in importance because of the greater use of loop circuits in transmission and distribution systems to obtain the advantages of network operation.

To make the best use of materials and man power and to keep system investment at a minimum require careful system planning in advance of working up the details of design. If a simple device is made available to engineers responsible for this system-planning work which will enable them to make more complete analysis of system load flow, voltages, losses, reactive requirements, and so forth, in less time than heretofore possible by analytical solutions, it should aid materially in improving the over-all effectiveness and efficiency of the engineers and the systems they design.

A-c boards, network analyzers, or calculators have been developed to a high degree of perfection and are used extensively by the larger operating companies for major problems involving system interconnections and extensive changes or additions. These boards are useful in many ways, and the only objection to them is that they are relatively large and expensive, and for economic reasons they are not available to the majority of engineers except at times when the larger studies are involved. Every power system has many problems arising from day to day which could be answered better and more completely in less time if there was a simple calculator at hand.

It is the purpose of this paper to set forth certain broad principles which form the basis for the design of entirely new and simpler types of calculators for load-flow analysis of a-c networks. Calculators based on these principles may prove to be the answer to system-planning engineers' requirements for small inexpensive units which will be on hand to solve these problems as they arise. The description of one particular form of calculator is given to illustrate the application of these principles to a specific and practical design. Briefly, this calculator consists of a multitap voltage transformer, a few multitap resistors, a measuring instrument which is similar to a three-phase, three-wire electrodynamometer type of wattmeter, and a number of multicontact multiposition bus switches. In addition to these main elements, there are multiconductor cords with plugs and jacks as required for the interconnection between the units. No generator or load units are required, and the only circuit elements needed are enough multitap resistor assemblies to set up the maximum number of circuits connected to any one bus of the network. For example, if the actual network under study has 100 circuits, and the maximum number of circuits connected to any one bus is six, only six resistor assemblies are required to obtain a complete network setup so that readings can be taken at every bus for generation or load and for load flow in everyone of the 100 circuits.

In references 1 and 2 a new method of a-c network analysis using resistance networks is described. It was shown how the method could be applied to the conventional d-c board by making four separate resistance-network setups to simulate the a-c impedance network. It was further indicated that, by use of a new type of resistance board in which the four resistance networks can be set up simultaneously and handled the same as a single network, it is possible to read directly real and reactive power, current and voltages at all points in the network by means of special metering equipment. This type of board referred to in these previous papers may be classified as a constant-current type, because the generators and loads are setup in terms of current components.

As an outgrowth of the study to obtain direct readings of real and reactive power for the constant-current type of board, the principles fundamental to the simple calculator described in this paper were developed. It may be classified as a constant-potential type, because voltage components are used to set up the generation and loads on the system. The advantage of the constant-potential over the constant-current type is that it requires much less equipment.

On existing a-c boards the load unit settings for real and reactive power are obtained by first assuming what the voltage at each load bus will be under the desired loading condition on the network under study and then computing the resistance and reactance combination which will give the approximate load at each bus. After generation has been set up on the board in approximately correct amounts, it is possible to readjust the loads and generators until the board is brought into correct adjustment for recording the readings. The generators are provided with phase-angle and voltage adjustment, so that the desired amount of real and reactive power can be established by a series of adjustments between generators and between generators and loads. Therefore, before good speed can be attained on setting up an a-c board for load-flow studies, the operator must first learn by experience the technique of bringing the board into correct adjustment for a setup of a particular network and loading condition. After the board is adjusted correctly for one set of readings, it is usually a simple matter to readjust it for other loading conditions or network changes, if the changes in loadings and circuits are not too great.

On the new calculator, described in this paper, the constant-potential source may be either alternating or direct current as desired, but the a-c type is somewhat simpler, and therefore the description is limited to this type. Since the circuit elements are made up of resistors only, it is evident that the flow established through these elements is real power and is of no importance except that it does determine the watt rating of the resistors used in the design of the calculator. The manner in which resistance circuit elements are set up with voltage components impressed across them to obtain a simulated flow of real or reactive power through them is the basic principle of the new calculator. Although one means of measuring this simulated real and reactive power flow is described, there are other methods of accomplishing these measurements.

The simulated real and reactive power flow on the new calculator is obtained by setting up the inphase and quadrature components of voltage, which in the operator's judgment would be likely to exist at each bus in the network under study with a particular loading condition. In effect, this is similar to the technique used in setting up the generator units on an a-c board of the conventional design.

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Because the loads, as well as the generators, are set up in this way, it is evident that more adjustments will be required to bring this new calculator into correct balance for a particular loading condition on the network. Therefore, as with existing a-c boards, the speed with which results can be obtained with this new device depends on the technique of the operator in making a series of adjustments at generator and load points to bring the board into correct balance for reading. It may be granted that the new board requires a greater number of adjustments to obtain a correct balance which will consume additional time. However, this does not mean that the device will not make an excellent showing as compared to other means of analyzing load flow in a-c networks. Also, as with existing a-c boards, after a network has been set up and brought into balance for one particular loading condition, if changes are made in the network or in the loading that are not too great, it will be a simple matter to readjust this board to suit the new conditions.

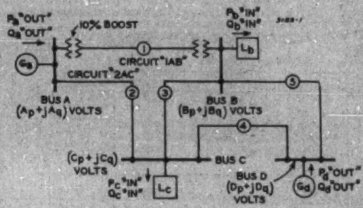

Figure 1 (left). One-line diagram of three-phase network to be used to illustrate connections and operation of calculator

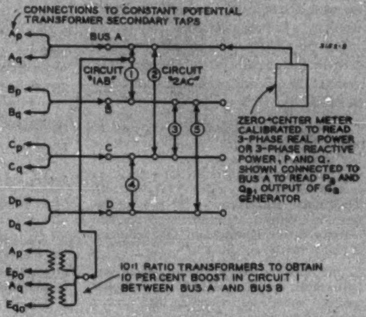
CONNECTIONS TO CONSTANT POTENTIAL TRANSFORMER SECONDARY TAPS
Ap
Aq
BUS A
3188-2
CIRCUIT "IAB"
①
②
CIRCUIT "2AC"
Bp
Bq
B
③
⑤
Cp
Cq
C
④
Dp
Dq
D
ZERO-CENTER METER CALIBRATED TO READ 3-PHASE REAL POWER OR 3-PHASE REACTIVE POWER, P AND Q. SHOWN CONNECTED TO BUS A TO READ Pa AND Qa, OUTPUT OF Ga GENERATOR
Ap
Epo
Aq
Eqo
10:1 RATIO TRANSFORMERS TO OBTAIN 10 PER CENT BOOST IN CIRCUIT 1 BETWEEN BUS A AND BUS B

Figure 2 (right). Plugging diagram for network of Figure 1, as it would be connected on the calculator

There are some points in the favor of the new calculator in addition to its extreme simplicity in design, small size, and low cost. One point is that it could be handled easily by a single operator. A second point is that, because of the small number of resistances required for circuit elements, they can be of a wattage rating that will allow substantial amounts of current to flow, so that the sensitivity of the measuring instrument need not be so great. A third point is that, because of its simplicity, the network connections can be made in a minimum time. A fourth point is that, by the elimination of generator and load units, it does not necessitate the reduction of an extensive network in advance to bring it down to a certain number of generators or loads before it can be set up on the board.

The circuit theory which is fundamental to this new calculator was presented in detail in the two references and therefore only a brief summary of this theory will be given as a background for the development of the equations for real and reactive power which are basic to the design of the new constant-potential type of calculator.

Mathematical Basis for New Calculator

If a vector current $\mathbf{I}=I_p+jI_q$ is flowing through a three-phase circuit of impedance $\mathbf{Z}=R+jX$ ohms to neutral, the voltage drop to neutral is $\mathbf{V}=V_p+jV_q=(I_pR-I_qX)+j(I_qR+I_pX)$ where vector $\mathbf{V}$ refers to the same reference axis as vector $\mathbf{I}$.

If a vector voltage drop $\mathbf{V}=V_p+jV_q$ occurs in a circuit with impedance $\mathbf{Z}=R+jX$, then the vector current flowing will be $\mathbf{I}=I_p+jI_q=(GV_p+BV_q)+j(GV_q-BV_p)$ where conductance $G=R/Z^2$ and susceptance $B=X/Z^2$.

If the voltage to neutral at one end of the circuit is $\mathbf{E}=E_p+jE_q$ volts, then, with balanced loading on the three phases, at this end the real power is $P=3(E_pI_p+E_qI_q)$, and the reactive power is $Q=3(E_pI_q-E_qI_p)$.

By substitution these equations P and Q may be rewritten

$$P=3E_p(GV_p+BV_q)+3E_q(GV_q-BV_p)$$

and

$$Q=3E_p(GV_q-BV_p)-3E_q(GV_p+BV_q)$$

To put the equations in a more useful form for the calculator design, let $M=1/G$ and $N=1/B$.

$$P=3E_p(V_p/M+V_q/N)+3E_q(V_q/M-V_p/N)$$

and

$$Q=3E_p(V_q/M-V_p/N)-3E_q(V_p/M+V_q/N)$$

In case it is desired to obtain the total net real and reactive power flow in a group of circuits connected to a common bus, the equations may be rewritten as follows:

$$P=3E_p[\Sigma(V_p/M)+\Sigma(V_q/N)]+3E_q[\Sigma(V_q/M-\Sigma(V_p/N)] \quad (1)$$

$$Q=3E_p[\Sigma(V_q/M)-\Sigma(V_p/N)]-3E_q[\Sigma(V_p/M)+\Sigma(V_q/N)] \quad (2)$$

Equation 1 states mathematically that the total net real power in a group of circuits connected to a common bus is equal to the algebraic sum of the real power flow in the individual circuits which combine to make up the group. Equation 2 is similar, except that it covers the same case for reactive power. These two equations are fundamental to the design of the new calculator, and this will be shown by illustrative diagrams later in the paper.

If a generator supplies a group of circuits from a bus, the algebraic sum of the real power flowing in the circuits is a measure of the real power supplied from the generator. Likewise the algebraic sum of the reactive power in the circuits is a measure of the reactive power supplied from the generator.

If a load is supplied from a bus which in turn is supplied from a group of circuits, then the algebraic sum of the real power flowing in the circuits is a measure of the real power supplied to the load. Likewise, the algebraic sum of the reactive power in the circuits is a measure of the reactive power supplied to the load, or, in case the load has leading power factor, it will be a measure of the reactive power supplied from the load.

The convention used in this paper is that power flow in a circuit, or group of circuits, is "out" if it flows away from the bus. As indicated on a zero-center wattmeter, "out" is to the right of zero. If power flow is toward the bus, it is "in" and would be indicated to the left of zero center on the instrument scale.

Reactive power flow in an indicated direction usually carries a negative sign to make it conform to the mathematical relationship it has with the quadrature component of current. In this case, however, the negative sign may be disregarded by using the convention of "out" and "in" which corresponds to the convention previously described for real power.

Calculator Operating Technique

The best way to establish an operating technique for this new calculator is by experience with the device, but it is possible to point out several factors which will aid in determining what procedure might be used in setting up the voltage components for a test on a network.

For the (X/R) ratios usually encountered in the circuits of an electric-power system, the following generalizations may be made:

1. At generator busses, both E_p and E_q would be set at a higher positive voltage tap than at load busses, because real and reactive power flowing in the same direction in a circuit will cause a drop in E_p and E_q. The amount of the drop in the two components will depend on the amount of real and reactive power flowing and the values of X and R for the circuit.
2. At load busses close to generation, both the E_p and E_q would be set at a higher positive voltage tap than at load busses more remote electrically from generation.
3. At load busses where reactive power is supplied to the network because of a synchronous condenser or other capacitance, then the E_p component may be more positive than that at surrounding busses, even though the real power flow is toward this bus. The value of E_q will be less positive or more negative than that of the surrounding busses in this case.

Description of New Calculator as Illustrated by Figures 1 to 4, Inclusive

Figure 1 shows the one-line diagram of a simple three-phase a-c network which is used as a basis for illustrating the setup and operation of the new calculator for determining load flow. Although the network is small and is self-explanatory on the diagram, it should be noted that it covers the pertinent points of generation, loads, and one circuit with step-up and step-down transformers set at different taps so that a net boost is effective in the closed loop of which this circuit is a part.

In setting up the network of Figure 1 on the calculator, it is necessary to establish the circuit constants $M=Z^2/R$ and $N=Z^2/X$ on the common kilovolt-ampere or voltage base of the board. In the case of circuit $1AB$ which includes the two transformers, the lumped-impedance method is used as the basis for obtaining the values for M and N for circuit $1AB$, and then a ten per cent series boost is inserted in this lumped circuit from A to B.

Figure 2 shows the schematic plugging diagram for the network of Figure 1 as it would be set up on the calculator. The movable cords, or other means of adjusting connections, are indicated by the heavy lines with the plugs and jacks shown as arrows and small circles, respectively. It should be noted that the plugging arrangement is simple and straightforward so that the connections can be made quickly.

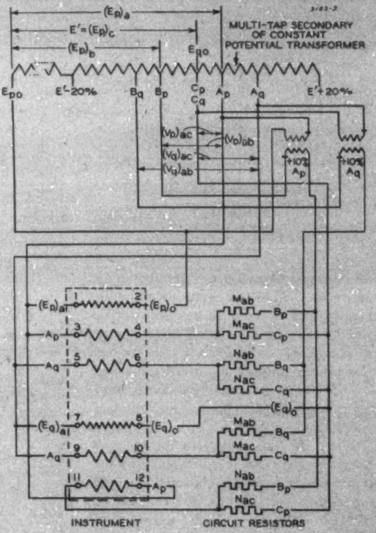

Figure 3. Schematic diagram of connections when measuring power flow at bus *A* of the network of Figure 1

Fundamental equation for three-phase power:

$P=3(E_pI_p+E_qI_q)$ where E_p and E_q are to neutral

Measurement of three-phase power fed to bus A by generator G_a is obtained by measuring the algebraic sum of the power flowing from bus A to bus B in circuit 1 and to bus C in circuit 2

$$P_a=3(E_p)_a\left[\frac{(V_p)_{ab}}{M_{ab}}+\frac{(V_p)_{ac}}{M_{ac}}+\frac{(V_q)_{ab}}{N_{ab}}+\frac{(V_q)_{ac}}{N_{ac}}\right]+3(E_q)_a\left[\frac{(V_q)_{ab}}{M_{ab}}+\frac{(V_q)_{ac}}{M_{ac}}-\frac{(V_p)_{ab}}{N_{ab}}-\frac{(V_p)_{ac}}{N_{ac}}\right]$$

Figure 3 shows the complete wiring for the fundamental circuit for measuring P, real power at bus A of the network. This diagram illustrates how the equation for power in terms of voltage components E_p, V_p, E_q, V_q, and circuit constants M and N may be set up with a measuring instrument to obtain a reading of real power flow. The instrument shown is essentially the same as a three-phase three-wire electrodynamometer type of wattmeter with a zero-center scale. The potential coils connected 1–2 and 7–8 are the moving coils whose combined torque act together to move the pointer. The current coils 3–4 and 5–6 are fixed coils and work together with the potential coil 1–2 to produce the torque of the upper element of the instrument. Likewise the current coils 9–10 and 11–12 are fixed coils which work together with potential coil 7–8 to produce the torque of the lower element of the instrument. The only unusual feature insofar as the instrument is concerned is that there are two fixed current coils on each of the two elements.

Referring to Figure 3, it will be seen that $(E_p)_a$, the inphase component of voltage to neutral at bus A, is acting across potential coil 1–2; that the current in coil 3–4 is the algebraic sum of $(V_p)_{ab}/M_{ab}$ and $(V_p)_{ac}/M_{ac}$; further that the current in coil 5–6 is $(V_q)_{ab}/N_{ab}+(V_q)_{ac}/N_{ac}$. The torque developed in the top element of the instrument is proportional to

$$(E_p)_a[(V_p)_{ab}/M_{ab}+(V_p)_{ac}/M_{ac}+(V_q)_{ab}/N_{ab}+(V_q)_{ac}/N_{ac}]$$

If the potentials and currents are traced for the lower element of the instrument, it will be seen that the torque developed is proportional to

$$(E_q)_a[(V_q)_{ab}/M_{ab}+(V_q)_{ac}/M_{ac}-(V_p)_{ab}/N_{ab}-(V_p)_{ac}/N_{ac}]$$

The sum of the upper- and lower-element torques is proportional to $P/3$ in the equation for power, P. The instrument

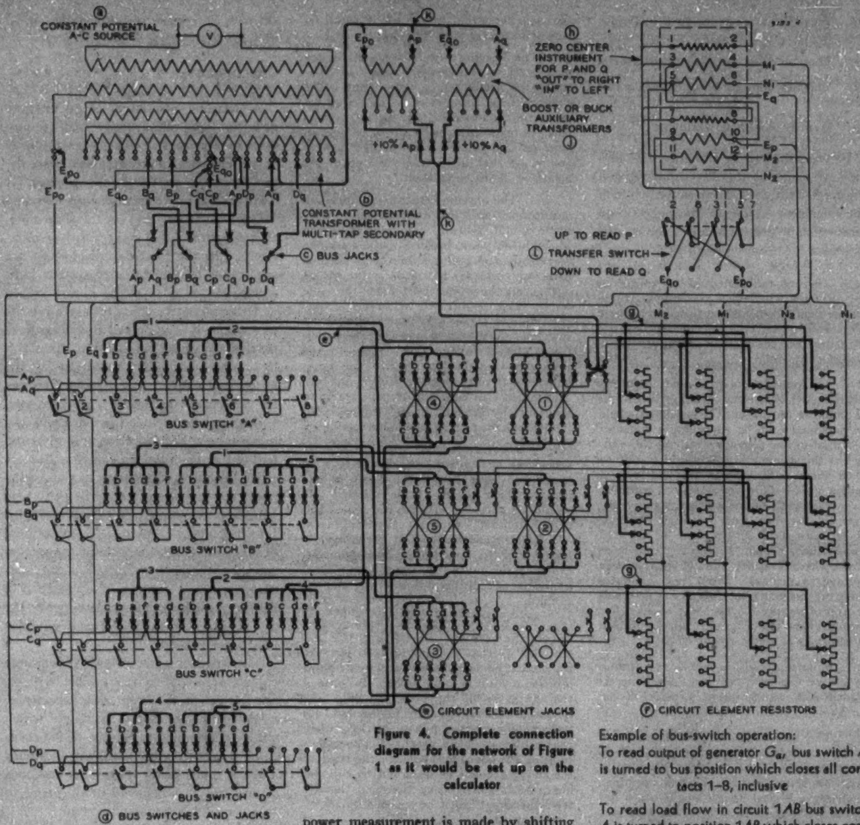
ⓐ
CONSTANT POTENTIAL A-C SOURCE
V
ⓚ
E_{po} A_p E_{qo} A_q
ⓗ
ZERO CENTER INSTRUMENT FOR P AND Q "OUT" TO RIGHT "IN" TO LEFT
BOOST OR BUCK AUXILIARY TRANSFORMERS
ⓙ
+10% A_p
+10% A_q
M_1
N_1
E_q
E_p
M_2
N_2
ⓑ CONSTANT POTENTIAL TRANSFORMER WITH MULTI-TAP SECONDARY
ⓒ BUS JACKS
A_p A_q B_p B_q C_p C_q D_p D_q
UP TO READ P
ⓛ TRANSFER SWITCH
DOWN TO READ Q
ⓖ
ⓔ
BUS SWITCH "A"
BUS SWITCH "B"
BUS SWITCH "C"
BUS SWITCH "D"
ⓔ CIRCUIT ELEMENT JACKS
ⓕ CIRCUIT ELEMENT RESISTORS
ⓓ BUS SWITCHES AND JACKS

Figure 4. Complete connection diagram for the network of Figure 1 as it would be set up on the calculator

Example of bus-switch operation:
To read output of generator G_a, bus switch *A* is turned to bus position which closes all contacts 1–8, inclusive

To read load flow in circuit 1*AB* bus switch *A* is turned to position 1*AB* which closes contacts 1–4, inclusive, but opens all other contacts

To read 2*AC* bus switch *A* is turned to 2*AC* which closes contacts 1, 2, 5, 6 but opens all other contacts

Circled letters refer to items described in paper

— Flexible connection cord

O← Plug inserted in jack or one point of a polarized plug on a multiconductor cord

— Lightweight lines indicate fixed wiring

scale calibration may be made to read the three-phase power, *P*.

This reading for power is the sum of that flowing in the two circuits 1*AB* and 2*AC*, and therefore it is also equal to the generator G_a output.

To obtain the power flow in the circuit 1*AB* it is only necessary to turn the bus *A* switch to position 1*AB* which disconnects 1*AC* circuit from the meter. Likewise, to read power flow in 1*AC*, the bus *A* switch would be turned to 1*AC* position which disconnects 1*AB* circuit from the meter. This bus switch is not shown on Figure 3, but it is shown on the more complete diagram on Figure 4.

Because of the similarity of the reactive-power measurement to that described for real power, a separate diagram and discussion are not included. The reactive-power measurement is made by shifting E_p and E_q potential connections with respect to the currents by means of a transfer switch as shown on Figure 4.

Figure 4 is a complete wiring diagram of one arrangement for the new constant-potential resistance-circuit type of network calculator. It meets the requirements of the simple plugging arrangement of Figure 2 for the complete setup of the networks shown on Figure 1.

The calculator consists of the following elements:

(*a*). Constant-potential a-c source.

(*b*). Constant-potential transformer with multitap secondary winding.

(*c*). Plugging cords for connecting between transformers secondary tap jacks and bus jacks.

(*d*). Bus switches, multipole and multiposition.

(*e*). Plugging cords for connecting between bus-switch jacks and circuit-element jacks.

(*f*). Circuit-element resistors, multitap.

(*g*). Plugging cords between circuit-element jacks and circuit-element resistors, or the equivalent in multiposition switches.

(*h*). Zero-center measuring instrument for real and reactive power.

(*i*). Transfer switch to connect meter for real-power or reactive-power measurement.

(*j*). Boost or buck auxiliary transformers.

(*k*). Plugging cords for boost or buck transformers.

(*l*). Miscellaneous fixed wiring.

Figure 2 showed the fundamental plugging arrangement in a simple manner, and Figure 3 illustrated the basic principle of the calculator design, so that only a brief discussion is presented on some of the elements as shown on Figure 4.

(*a*). The constant-potential a-c source to the primary winding of the supply transformer should be regulated closely, and there are several static types of constant-voltage regulators on the market which would be ideal for this duty.

(*b*). The constant-potential a-c transformer with tapped secondary winding should be of ample design so that the regulation could be neglected and, by supplying a constant potential to the primary winding, the voltage at the secondary taps would remain at fixed values. The range for the taps as shown would allow for a 40 per cent difference in each of the voltage components over the network, from reference point E^1 or $(E_q)_0$ to plus 20 per cent of E^1, and from reference point to minus 20 per cent of E^1, where E^1 is the value of the reference voltage to neutral. From test computations the secondary taps may be at intervals of approximately 0.5 per cent of E^1. For simplicity on the diagram, taps are shown at intervals of two per cent of E^1.

(*c*). Plugging cords between transformer secondary taps and bus jacks could be replaced with multiposition switches if desired.

(*d*). The bus switches are shown with enough contacts to take care of three circuits per bus. These could be extended to take care of any number of circuits per bus. Probably a logical design would be to use about a six-position switch which would take care of metering the total at the bus and five individual outgoing circuits, and then, when more circuits than five are connected to a single bus, additional bus switches would be assigned to that particular bus to take care of the additional circuits. It is possible to replace the bus switches with bus jacks and cords, but the switches should give a means of speeding up the taking of readings. Another alternate to the bus switches would be the use of small relays actuated by push buttons.

(*e*). The plugging cords between bus-switch jacks and circuit-element jacks are six-conductor flexible cords, all similar. The change in the rotation of the lettering between the connections to the top and bottom jacks is taken care of by cross connection between the jacks, as shown on the diagrams, so that all of the cord plugs are polarized in the same way.

(*f, g*). The circuit-element resistors are multitapped, and four resistors make up a single circuit element. These resistors can be set up in pairs, because two are for the *M* setting, and two are for the *N* setting. Although cords are indicated as the means of choosing the proper resistor tap, the same results may be obtained by having a suitable number of rheostat-type multicontact switches. As discussed earlier in the paper, the number of resistor units required for the calculator is dependent on the maximum number of circuits connected to any one bus. This means that each unit may be set up to simulate any number of circuits, provided they do not connect to a common bus.

(*h*). The operation of the zero-center measuring instrument is discussed fully in connection with Figure 3. There is one feature which has not been included. It should be provided with several scales and a multicontact switch for switching from one scale to another to suit the range required for a particular measurement. Probably the multirange would be obtained by the use of multipliers in the two potential circuits.

(*i*). The transfer switch for the measuring instrument operates to transfer the potentials, so that in the up position the instrument reads real power and in the down position it reads reactive power. Because of the simplicity of the board, it might be advisable to have separate instruments for real and for reactive power, so that, when changes are made in potential connections to bring the board into balance, the effect of the changes could be observed on the two instruments at the same time and thereby the adjustment might be speeded up.

(*j*). The boost or buck auxiliary transformers are provided to take care of the effect of net boost or buck in a circuit which forms one branch of a closed loop in the network. The impedance of these should be low as compared to the resistance values used in the circuit elements, so that the transformer introduces a series boost or buck without changing the circuit constants appreciably.

(*k*) and (*l*) require no explanation other than the diagram.

Conclusions

A network calculator, of an entirely new form, and of unusually small proportions in physical size and in the simplicity of its electrical elements, has been described in this paper. It may be used for analyzing a-c networks for load-flow and voltage conditions and for the determination of system losses and reactive requirements. In the form described, it is not applicable to the study of system fault conditions.

Although not specifically covered in the discussion, it is apparent that long lines may be set up on this type of calculator by using constants based upon the nominal or equivalent π. In such a case, the effect of the leading reactive and leakage can be included by adjusting the measurement of generation or load at the two terminals of the transmission line.

In the general discussion section of the paper the technique of operating this new calculator was compared to that now used for complete a-c boards of conventional design. The new calculator is not intended to be the equivalent of the complete a-c board but instead is designed to fill the gap which now exists between the a-c analyzer or calculator and complex analytical methods.

The writer intends to build this simple calculator as soon as it is possible to do so, and, on the basis of operating experience, to determine the board technique which will give the quickest results. If it proves to be slower than the complete a-c board and yet much faster than other methods of analysis, it is believed it will prove to be a completely satisfactory tool.

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The Frequencies of Natural Power Oscillations in Interconnected Generating and Distribution Systems

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1. Introduction

THE distribution of power in the steady state of interconnected electric networks with large numbers of generators and of consumers of power is well known for a given system. The flow of energy in detail depends on the distribution of the loads, on the admission of power to the prime movers, and on the data for all the connecting lines. With every change of the power distribution in any branch of the system, transient phenomena are initiated which influence the entire complex of prime movers and generators, motors and other consumers, and intermediate lines. As is often observed in the larger systems, the changes in the flow of energy lead to oscillations of power and frequency, depending on the electrical and mechanical data of machines and network, and because of the magnitude of the active energy in modern machines, this may influence detrimentally the performance of the entire system.

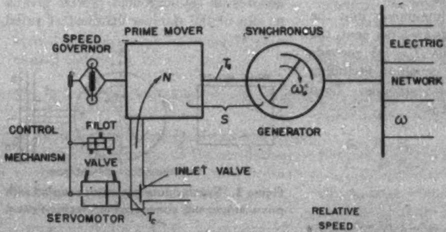

Figure 1. Typical arrangement of generator, prime mover, and speed-governing mechanism

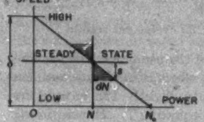

Figure 2. Droop characteristic of typical speed-governed prime mover

All pure electromagnetic oscillations and similar disturbances, comprising fluctuations merely of the electric and magnetic fields of the conductors, for the most part have frequencies which are higher than the operating frequency of 25 to 60 cycles per second. We will exclude such rapid fluctuations from our consideration since they appear within the electrical part of the system only and have no substantial direct influence on the generation and the use of the a-c energy. Also we will exclude such very slow fluctuations as may be caused by the control of the steam plant proper or the regulation of the flow of water and which have no direct relation to the electrical part of the system. We shall concentrate our attention on those power oscillations which influence the system frequency and change the distribution of the flow of the active energy within and between the various parts of interconnected systems. These electromechanical fluctuations affect the speed of the generators with their prime movers and speed governors as well as the speed of the numerous motors of the consumers.

We are investigating any deviation from the steady-state operation only, and all powers, speeds, frequencies, and position angles may refer, therefore, only to the deviations, leaving open the steady-state values of these magnitudes. For the sake of simplicity, and in order to emphasize the main factors, we neglect unimportant mechanical or electrical losses and any lack of perfection in the working parts of the system.

In order to obtain a closed solution of our problem which is fairly complex because of the large number of coupled units most of which have different designs, we introduce further simplifying assumptions which, however, do not affect the general value of the solution. Thus, we will assume that during the transient periods all the main data of machines and network remain constant, that all deviations from the steady state are relatively small and are linear, that unnecessary disturbing effects within the mechanical elements, like backlash or friction in the joints, are avoided, and that the electrical part of the network can be reduced, at least approximately, to a simple form, namely, a radial star system. We do not consider direct-acting speed governors since these play no important part in modern power systems.

2. Power Oscillations of the Single Elements

The entire system here considered may consist of any number of prime movers of different size and type, with like or different speed-governor mechanisms, and driving various synchronous generators, all interconnected to a common system and loaded with any number of induction motors. Our ultimate aim is to determine the amplitudes and frequencies of the deviation of power from the steady-state distribution among the various machines. However, before we consider the influence of the coupling or interconnection of the machines, we will review briefly the well-known analysis of oscillations both of speed-governed prime movers and of synchronous generators and supplement it by a new analysis of transients in induction motors.

A. Prime Mover With Indirect Speed Governor

In Figure 1 a prime mover is shown coupled with a synchronous generator feeding a network and controlled in its speed by a governor with a valve-operating servomotor, as is usual with steam and water turbines. The droop of the speed against generated power is shown in Figure 2.

An excess N of power of the prime mover accelerates the masses of the rotating system, the relative speed deviation of which may be s, so that ds/dt is the relative acceleration. If the electric load of

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the generator remains constant, the energy relation is

$$N+S\frac{ds}{dt}=0 \tag{1}$$

where

$$S=T_sN_0=Ig\omega_0^2 \tag{2}$$

is an inertia constant defined either by the product of the starting time constant T_s and the rated power N_0 of the prime mover, or by twice the kinetic energy of the moving masses having the moment of inertia I, and rotating with the rated mechanical angular speed ω_0, the gravity factor g transforming the units to the practical metric system. The starting time constant of prime mover plus generator is, for the most part, in the limits $T_s=3$ to 20 seconds.

The speed-governing mechanism causes the admission of power to the prime mover to change with time proportional to the speed deviation s so that

$$\frac{dN}{dt}=\frac{N_0}{\delta T_c}s \tag{3}$$

Herein the coefficient of s is taken usually as inversely proportional to the total droop δ of the governor, Figure 2, and to the given total closing time T_c of the valve moving servomotor. For reasons of stabilization, it is customary to affect the movement of the pilot valve by means of a supplementary return, derived from the position of the servomotor itself and proportional to the power admission, so that

$$\frac{dN}{dt}=\frac{N_0}{\delta T_c}s-\frac{N}{T_c} \tag{4}$$

expresses the operation of the speed-governing mechanism for most of the present-day generating systems. The usual limits for the governor droop are $\delta=2$ to 8 per cent, and for the valve closing time are $T_c=0.5$ to 5 seconds.

There are many additional devices for load control or droop compensation, either by hand or automatically, but most of them are intended to affect more the steady-state speed-power characteristic than the deviation during a state of unbalance. Some governor systems have secondary detrimental qualities such as undamped rapid vibrations of their inherent masses, dead bands caused by friction or by play in the joints, nonlinear power characteristic of the inlet valve, and so forth, qualities which even may initiate self-excited oscillations. But these disturbances, if reduced by skillful construction, have only slight influence on the main and unavoidable effects considered in this paper, which are preponderantly due to the quotient $N_0/\delta T_c$, the power velocity of control expressed in kilowatts per second, of the entire governing mechanism.

By substituting for the controlled power of equation 4 the energy of equation 1, we obtain the differential equation for the speed deviation of the prime mover and governor system

$$\frac{d^2s}{dt^2}+\frac{1}{T_c}\frac{ds}{dt}+\frac{N_0}{\delta T_cS}s=0 \tag{5}$$

The solution is given by the free natural oscillation

$$s=F\cdot\epsilon^{-\frac{\rho_\gamma}{2}t}\cdot\cos\gamma t \tag{6}$$

where the amplitude F expresses the maximum speed or frequency deviation of the generating system from the steady-state value. With use of equation 2,

$$\gamma=\sqrt{\frac{1}{\delta T_cT_s}-\left(\frac{\rho_\gamma}{2}\right)^2} \tag{7}$$

is the frequency of the speed oscillations, and

$$\frac{\rho_\gamma}{2}=\frac{1}{2T_c} \tag{8}$$

is the exponent of the decay with time of the amplitude. The oscillation N of the power supplied to the prime mover is given by differentiation of equation 6, substituted in equation 1.

For an average prime-mover unit the droop may be taken as $\delta=5$ per cent, the valve closing time as $T_c=1.5$ seconds and the starting time constant, including the generator, as $T_s=8$ seconds. Then the damping exponent is

$$\frac{\rho_\gamma}{2}=\frac{1}{2\cdot1.5}=0.33$$

and the frequency

$$\gamma=\sqrt{\frac{100}{5\cdot1.5\cdot8}-0.33^2}=\sqrt{1.67-0.11}$$

$$=1.25\text{, or }0.20\text{ cycles per second}$$

For most of the usual machine systems, the damping effect is moderate and thus influences only slightly the natural frequency γ of equation 7. Hence the significance of the last term in equation 4, responsible for the damping, is negligible and equation 3 can be used in good approximation for all problems concerning merely the frequency of the power oscillations.

B. Electric Generator and Network

Every synchronous generator, as in Figure 3, is coupled elastically with the electric network, because of the magnetic forces between stator and rotor. If the rotor speed varies, electric power P is exchanged with the network, and, if we consider the generator alone, without power variations from the prime mover, the energy relation during acceleration is

$$S\frac{ds}{dt}+P=0 \tag{9}$$

The generator produces synchronous power dependent on the position angle of the pole wheel viewed from the network voltage vector and usually produces, in addition, some asynchronous power by action of damper circuits on the rotor, and dependent on the change with time of this same pole angle. The total electric power of the generator as differing from the steady-state power is, when the angle deviates by θ from the network angle,

$$P=p_s\theta+\frac{p_a}{\omega}\frac{d\theta}{dt} \tag{10}$$

where

$$p_s=kP_0 \tag{11}$$

denotes the synchronizing power of the machine proportional to the rated electric power P_0, and to a synchronizing coefficient k, which in turn depends upon reactances and load of the machine. Furthermore

$$p_a=P_0/s_0 \tag{12}$$

denotes an asynchronous power, with s_0 as slip of the damper attained at rated power, while the damper runs as an induction motor. Finally

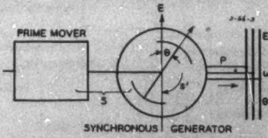


Figure 3. Synchronous generator coupled with prime mover and connected to electric system

$$s'=\frac{1}{\omega}\frac{d\theta}{dt} \tag{13}$$

is the relative slip between the instantaneous electrical speed of the generator and the angular frequency ω of the network, as determined by the machine angle θ.

Numerically, the synchronizing coefficient is for the most part in the limits $k=1$ to 3 and the rated damper slip $s_0=$ 3 to 30 per cent, while the usual frequency of 60 cycles per second gives $\omega=377$.

The total speed deviation of the pole wheel is given by the sum of the machine and network variations and is expressed by their angles θ and θ_n as

$$s=\frac{1}{\omega}\frac{d(\theta+\theta_n)}{dt} \tag{14}$$

Introducing equations 10 and 14 in equation 9, we obtain the differential equation

$$\frac{d^2(\theta+\theta_n)}{dt^2}+\frac{p_a}{S}\frac{d\theta}{dt}+\frac{\omega p_s}{S}\theta=0 \tag{15}$$

which determines the angular movement of the generator for any given variation of the network or system angle θ_n.

If the network is in steady state, the natural oscillations of the pole angle are as solution of equation 15 with $\theta_n=0$.

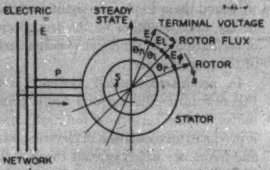


Figure 4. Voltages and phase angles of induction motor fed by fluctuating electric power

$$\theta=A\cdot\epsilon^{-\frac{\rho_\sigma}{2}t}\cdot\cos\sigma t \tag{16}$$

Here, with use of equations 2 and 11,

$$\sigma=\sqrt{\frac{\omega p_s}{S}-\left(\frac{\rho_\sigma}{2}\right)^2}=\sqrt{\frac{\omega k}{T_s}-\left(\frac{\rho_\sigma}{2}\right)^2} \tag{17}$$

is the frequency and, with use of equations 2 and 12,

$$\frac{\rho_\sigma}{2}=\frac{p_a}{2S}=\frac{1}{2s_0T_s} \tag{18}$$

is the damping exponent of the free oscillations. The power of the oscillations is given by equation 10 substituting equation 16 for the pole angle θ.

For an average generator unit the synchronizing factor may be taken as $k=2$, the rated damper slip as $s_0=10$ per cent, and the starting time constant again as $T_s=8$ seconds. Then the damping exponent is

$$\frac{\rho_\sigma}{2}=\frac{100}{2\cdot 10\cdot 8}=0.63$$

and the frequency

$$\sigma=\sqrt{\frac{377\cdot 2}{8}-0.63^2}=\sqrt{94.2-0.40}$$
$$=9.67\text{, or 1.54 cycles per second}$$

Damper circuits in actual machines always give a moderate damping factor ρ_σ. Its influence on the natural frequency σ in equation 17 is thus only slight and we can often neglect the asynchronous power in equation 10 and the second term in the differential equation 15 for the problems concerned only with the frequency of oscillation. It is well known that the synchronizing power and its coefficient k in equation 11 are slightly different for very slow and very fast movements of the pole angle θ. Thus it might be necessary to choose different values for k, depending on the frequency of oscillations resulting from our analysis.

C. Induction Motor and Network

If an asynchronous induction motor, as in Figure 4, is fed by a network with varying phase angle of the voltage E, the deviation θ_n of which is measured against a uniformly rotating or steady-state voltage vector, the motor takes up a varying power, the deviation of which from the steady-state supply we will determine. If the rotor of the induction motor slips against its rotating flux, a power is developed

$$P=P_0\frac{s'}{s_0}=\frac{P_0}{s_0}\frac{1}{\omega}\frac{d\theta_r}{dt} \tag{19}$$

where P_0 is the rated electric power of the motor, s_0 is the rated asynchronous slip, and s' is the momentary slip determined by the variation of the electrical angle θ_r between rotor and flux.

The effective rotor flux, on the other hand, has a phase displacement with respect to the stator terminal voltage, because of the leakage flux of the motor. This leakage angle θ_l, as shown in Figure 4, is, for small deviation, given directly by the quotient of the momentary leakage voltage E_l and the terminal voltage E so that the angular deviation of terminal voltage and rotor flux is

$$\theta_l=\frac{E_l}{E}=\frac{E_{l0}}{E}\frac{E_l}{E_{l0}}=e_l\frac{P}{P_0} \tag{20}$$

the leakage voltage being proportional to the current and therefore to the momentary power P. Here $e_l=E_{l0}/E$ denotes the relative leakage voltage of the entire motor at rated load.

For most induction motors the leakage voltage is $e_l=20$ to 30 per cent while the rated slip is in the limits $s_0=0.5$ to 5 to 10 per cent.

According to the law of energy, the power deviation exchanged between network and motor again is determined by

$$P+S\frac{ds}{dt}=0 \tag{21}$$

where S is here the inertia constant of equation 2 for the motor. The starting time constant of induction motors as quotient S/P_0 is usually in the limits $T_s=0.5$ to 3 seconds.

The total speed deviation s of the rotor, as indicated in Figure 4, is given by the variation with time of the slip angle θ_r, the leakage angle θ_l, and the network angle θ_n in conjunction

$$s=\frac{1}{\omega}\frac{d}{dt}(\theta_r+\theta_l+\theta_n) \tag{22}$$

Substituting equations 19 and 20 in equation 22, we obtain from equation 21 the differential equation for the electric power deviation

$$P+s_0T_s\frac{dP}{dt}+\frac{e_lT_s}{\omega}\frac{d^2P}{dt^2}=-\frac{T_sP_0}{\omega}\frac{d^2\theta_n}{dt^2} \tag{23}$$

This result shows that an asynchronous induction motor by variation of the network angle θ_n can be excited to forced oscillations, and that even with a steady-state network it may perform transient natural oscillations which are determined by a zero value of the right-hand side of equation 23.

The power of such free oscillations exchanged between rotor and network is given by the solution

$$P=\bar{P}\cdot\epsilon^{-\frac{\rho_\mu}{2}t}\cdot\cos\mu t \tag{24}$$

where P is the amplitude of the oscillation. The natural frequency is

$$\mu=\sqrt{\frac{\omega}{e_lT_s}-\left(\frac{\rho_\mu}{2}\right)^2} \tag{25}$$

and the damping exponent is

$$\frac{\rho_\mu}{2}=\frac{\omega s_0}{2e_l} \tag{26}$$

It is significant that an induction motor can undergo free oscillations attributable to the effect of the varying leakage voltage, as seen clearly by the third term of the differential equation 23. The natural slip of the rotor, on the other hand, produces a damping effect, as seen by the second term of that equation.

For an average motor with a rated slip $s_0=1$ per cent, a leakage voltage $e_l=25$ per cent, and a starting time constant $T_s=1$ second, the damping exponent is

$$\frac{\rho_\mu}{2}=\frac{377\cdot 0.01}{2\cdot 0.25}=7.54$$

and the frequency

$$\mu=\sqrt{\frac{377}{0.25\cdot 1}-7.54^2}=\sqrt{1{,}510-57}$$
$$=38.1\text{, or 6.05 cycles per second}$$

This shows that for average values of the motor constants, the natural frequency is relatively high compared with that of synchronous generators, and the damping effect is considerable. It may be noted that the influence of the slip s_0 for induction motors, equation 26, is opposite to that for synchronous generators with damper, equation 18.

Therefore, larger slip of the motor increases the damping of the natural oscillations and with a rated slip

$$s_0 \geqq \sqrt{\frac{4e_l}{\omega T_s}} \qquad (27)$$

given by the zero value of the radical in equation 25, the movement becomes aperiodic. Hence a motor with the data just mentioned would lose its natural oscillations if the slip were

$$s_0 \geqq \sqrt{\frac{4 \cdot 0.25}{377 \cdot 1}} = 5.15 \text{ per cent}$$

Small motors are often in this aperiodic range, while larger motors, for the most part, work in the oscillatory range.

At forced fluctuations of the network angle, aperiodic motors follow with a sliding movement, while motors in the periodic range respond according to the laws of resonance. For an oscillation with frequency ν of the network angle θ_n, the forced power exchange of the motor with the network becomes, by equation 23

$$P = \frac{T_s P_0/\omega}{1 + j\nu s_0 T_s - \nu^2 e_l T_s/\omega} \nu^2 \theta_n \qquad (28)$$

At frequencies considerably lower than the natural frequency, the denominator of this equation approaches the value one. For example, the frequency $\nu = 9.67$, corresponding to 1.54 cycles per second found previously as inherent for generators, would give for the aforementioned motor a denominator of equation 28

$$1 + j9.67 \frac{1}{100} 1 - 9.67^2 \frac{0.25 \cdot 1}{377}$$
$$= 1 + j0.0967 - 0.062$$

This vector is represented in Figure 5 and shows that for forced oscillations of such frequency the damping effect of the slip is about ten per cent, and the elastic effect of the leakage is only six per cent of the inertia effect of the rotor.

For small damping effect, as present with large induction motors, equation 28 simplifies to

$$P = \frac{-S/\omega}{1 - \left(\frac{\nu}{\mu}\right)^2} \frac{d^2\theta_n}{dt^2} \qquad (29)$$

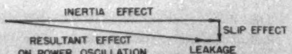
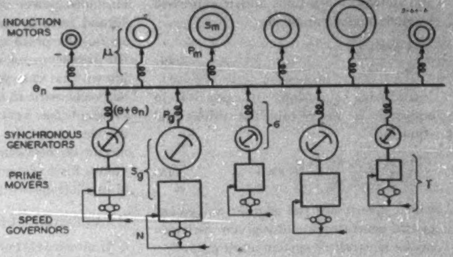


Figure 5. Vector diagram showing the relative effects of inertia, slip, and leakage on slow oscillations of induction motor

Figure 6. Radial interconnection of many speed-governed synchronous-generator units and numerous induction motors in large systems



showing that the forced power exchange is especially large within the resonance zone of the motor.

Similar to the effect of induction motors is the reaction which commutator motors of various types exert on a network with fluctuating frequency. Their power deviation also is determined by equation 21, if we neglect the secondary effects of load and loss variations. Under certain conditions here also natural frequencies may occur, the principal effect, however, being given again by the action of the inertia of such motors under variable speed conditions.

Almost all the other consumers of the electric power, such as light and heat appliances, electrochemical plants, rectifiers, and so forth, transform the electric energy directly or indirectly into heat or other nonmechanical power. No energy is reversibly stored in them, and thus these devices act only as resistance loads and increase the total damping effects of the system. We shall consider these later and will concentrate our attention at first on those effects which produce oscillations.

3. Interaction of All Prime Movers, Generators, and Motors

The entirety of a large interconnected electrical distribution system for the most part can be transformed either rigorously or approximately into a star system, with many branches connected to one suitably chosen common point. In Figure 6 such a system is shown where each synchronous generator, driven by its prime mover with speed governor, feeds into all the various induction motors through the reactances of the distribution network. These reactances now appear in the radial lines and supplement the internal reactances of the various generators and motors. The resistances of the network may be neglected for the sake of simplicity. We will also omit in this section all the other internal damping effects previously considered, since we found them to play no significant part in the determination of the frequencies of oscillation, their influence being merely of the second order.

In an interconnected system, as in Figure 6, no oscillation of a single prime mover can be performed for itself, since it produces an oscillation of electric power and frequency which spreads through the entire network. Thereby all the other generator units and also all the motors are energized oscillatorily from the unit first considered, and this refers to the machines adjacent in the same power plant as well as to those far distant at the last consumer. Thus all the rotating masses in the system will undergo oscillating forces; however, they will react upon any exciting oscillation by virtue of their own restoring forces, namely, those of their governors, their synchronous and their induction effects. Hence by coupling a multitude of machine units to a complex system all the individually possible oscillations will interfere with one another, resulting in a complicated overall variation of power distribution and network frequency, the details of which we will consider analytically.

A. CHARACTERISTIC FREQUENCY EQUATION FOR ANY NUMBER OF MACHINES

For each generator unit with varying mechanical power input and electric power output, the energy relation is now a combination of equations 1 and 9, namely

$$N + S\frac{ds}{dt} + P = 0 \qquad (30)$$

Neglecting the damping effects by using equation 3 for the admission power, and using the first term only of the right-hand side of equation 10 for the electric power, and substituting in all terms the angle θ, by use of equation 14, we obtain

$$\frac{N_0}{\delta T_c} \frac{\theta + \theta_n}{\omega} + \frac{S}{\omega} \frac{d^2(\theta + \theta_n)}{dt^2} + p_s\theta = 0 \qquad (31)$$

We can simplify this equation by the

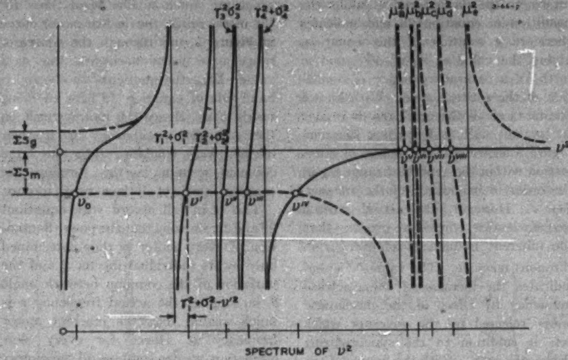


Figure 7. Graphical representation of both sides of characteristic equation for joint natural frequencies of interconnected systems, showing four generator units and five motors

introduction of the inherent natural frequencies γ of the prime mover proper and σ of the synchronous generator proper but now including its line reactance, as in equations 7 and 17, also omitting the damping terms. We have then

$$\frac{d^2(\theta+\theta_n)}{dt^2}+\sigma^2\theta+\gamma^2(\theta+\theta_n)=0 \qquad (32)$$

For each of the prime mover plus synchronous generator units we obtain such a differential equation, only with different constants σ and γ. Hence each generator unit will have a different internal pole angle θ, while the angle θ_n for the network system, referring to the star point of Figure 6, is the same for all generators. So we have as many differential equations of the form of equation 32 as there are generator units in operation in the entire interconnected system.

If the system angle θ_n fluctuates, then every motor operates with fluctuating power according to equation 23. We have therefore as many equations of the form 23 as there are individual motors in the entire system, all having the same network angle variation on the right-hand side. Consistently we omit here the second term of equation 23, representing the damping effect attributable to slip variations of the motor, and include in the leakage voltage e_l of the third term the reactive drop of the feeding line.

Since we have excluded losses in the network, the sum of the electric power fluctuations of all the synchronous generators must be negatively equal to the sum of the power fluctuations of all induction motors.
Hence,

$$\Sigma P_g=\Sigma p_s\theta=-\Sigma P_m \qquad (33)$$

where the sum of P_g denotes all synchronous power deviations delivered or received by the individual generators according to the first term of equation 10, while the sum of P_m denotes the exchanged power deviations of all the motors according to equation 23.

We can solve these systems of linear differential equations directly by substituting harmonic oscillations for the various angles, namely,

$$\theta_1=A_1\cos\nu t;\ \theta_2=A_2\cos\nu t;\ \theta_3=A_3\cos\nu t;\ \ldots\ \theta_n=A_n\cos\nu t \qquad (34)$$

with different amplitudes A_1, A_2, A_3, ... A_n, for generators and network, all having the same frequency ν which is the unknown of our problem. The generator equations of the form of equation 32 then yield the series

$$\left.\begin{aligned}&-\nu^2(A_1+A_n)+\sigma_1^2A_1+\gamma_1^2(A_1+A_n)=0\\&-\nu^2(A_2+A_n)+\sigma_2^2A_2+\gamma_2^2(A_2+A_n)=0\\&-\nu^2(A_3+A_n)+\sigma_3^2A_3+\gamma_3^2(A_3+A_n)=0\\&\ldots\ldots\ldots\ldots\ldots\ldots\ldots\ldots\end{aligned}\right\}(35)$$

From each equation we obtain the ratio of the specified generator amplitude to the network amplitude of oscillation

$$\frac{A_1}{A_n}=\frac{\nu^2-\gamma_1^2}{\gamma_1^2+\sigma_1^2-\nu^2};\quad\frac{A_2}{A_n}=\frac{\nu^2-\gamma_2^2}{\gamma_2^2+\sigma_2^2-\nu^2};\quad\text{and so forth} \qquad (36)$$

On the other hand, equation 33 gives, with use of equation 29 for the motor actions, without damping effect and denoting the various motors by the subscripts a, b, c, and so forth.

$$p_{s1}A_1+p_{s2}A_2+p_{s3}A_3+\ldots$$
$$=-\left[\frac{S_a/\omega}{1-\left(\frac{\nu}{\mu_a}\right)^2}+\frac{S_b/\omega}{1-\left(\frac{\nu}{\mu_b}\right)^2}+\frac{S_c/\omega}{1-\left(\frac{\nu}{\mu_c}\right)^2}+\ldots\right]\nu^2A_n \qquad (37)$$

For simplification of this relation we substitute for the synchronizing powers p_s, by use of the middle term of equation 17, the natural generator frequencies σ, omitting the damping term. We then obtain

$$\sigma_1^2S_1A_1+\sigma_2^2S_2A_2+\ldots$$
$$=-\left[\frac{\nu^2S_a}{1-\left(\frac{\nu}{\mu_a}\right)^2}+\frac{\nu^2S_b}{1-\left(\frac{\nu}{\mu_b}\right)^2}+\ldots\right]A_n \qquad (38)$$

If we now express the amplitudes A_1 A_2, and so forth, of the pole angles of the individual synchronous machines by their values according to equation 36, the network amplitude A_n cancels, and we obtain finally

$$\left.\begin{aligned}&S_1\frac{\sigma_1^2(\nu^2-\gamma_1^2)}{\nu^2(\gamma_1^2+\sigma_1^2-\nu^2)}+S_2\frac{\sigma_2^2(\nu^2-\gamma_2^2)}{\nu^2(\gamma_2^2+\sigma_2^2-\nu^2)}+S_3\frac{\sigma_3^2(\nu^2-\gamma_3^2)}{\nu^2(\gamma_3^2+\sigma_3^2-\nu^2)}+\ldots\\&=-S_a\frac{\mu_a^2}{\mu_a^2-\nu^2}-S_b\frac{\mu_b^2}{\mu_b^2-\nu^2}-S_c\frac{\mu_c^2}{\mu_c^2-\nu^2}-\ldots\end{aligned}\right\}(39)$$

From this characteristic equation we can derive the joint natural frequency ν of all the many coupled synchronous and induction machines throughout the interconnected systems, since the inertia constants S and the inherent frequencies γ, σ, μ of the individual machines, the latter including their network reactances, are given.

B. Graphical Solution for Three Types of Oscillation

The solution of equation 39 can be found by graphical representation of both the left-hand and right-hand sides separately, the former belonging only to the generator units, the latter to the motors alone. Figure 7 shows, by solid and dashed lines, the varying value of the two sides of equation 39 dependent on the square of the unknown frequency ν. For $\nu=0$, all the terms on the left-hand side of equation 39 become minus infinity, while all the quotients on the right-hand side become one, so that the sum of all the kinetic energies S_m of the motors appear with negative sign as starting value of the dashed curve in Figure 7.

For increasing ν, let us first consider the simpler right-hand side of equation

39. All the fractions on this side increase at first very slowly, until ν approaches the lowest natural frequency μ_a of one of the motors. The individual term belonging to this motor increases then toward infinity, there changing sign, so that the dashed curve with further increasing ν descends from plus infinity. Since all the other terms on the right-hand side of equation 39 remain negative, the curve soon reaches zero and passes infinity again when ν reaches the second lowest natural frequency μ_b belonging to another motor. This continues as many times as there are motors in the system. Finally, if ν is larger than all the natural motor frequencies, every right-hand-side term has changed to the positive sign and so, with ever-increasing ν, the dashed curve approaches the zero line from above in a hyperbolic manner.

Now we consider the left-hand side of equation 39 by means of Figure 7. With ν increasing from zero, the term with the lowest natural frequency γ_1 first changes its sign when ν passes γ_1, and this occurs successively for all the terms, the γ^2 of which is smaller than the lowest $\gamma_1^2+\sigma_1^2$. Thus the solid curve, with increasing ν, soon becomes positive. It then increases to infinity when ν^2 approaches the lowest $\gamma_1^2+\sigma_1^2$. The curve there changes sign and ascends from minus infinity, reaching plus infinity a second time at the second lowest $\gamma_2^2+\sigma_2^2$. This repeats as many times as there are synchronous generator units feeding the entire interconnected network. Finally the solid generator curve approaches the zero line hyperbolically from below.

As we have seen that numerically the inherent governor frequencies γ are fairly low, that the synchronous-generator frequencies σ are much higher, and that the induction-motor frequencies μ are still higher, we realize that the three regions in which the curves pass through zero or reach infinity are widely separated, as seen in Figure 7. Furthermore, the value of γ^2 is always very small compared with σ^2, for our examples $1.25^2 = 1.57$ in comparison to $9.67^2=935$, so that in the denominators of equation 39 γ^2 can often be neglected. If the γ's would vanish completely, as for prime movers without speed governors, the remaining ν^2 in the numerators of the left-hand side of equation 39 would cancel and the terms $S_g\sigma^2/(\sigma^2-\nu^2)$ would converge for $\nu=0$ toward the sum of all kinetic energies S_g of the generators, as indicated by the chain-dotted curve end in Figure 7. This shows the similarity between generator and motor effects.

Every point at which the solid curve intersects the dashed curve fulfills the condition of equation 39 and indicates therefore a solution of this equation, giving the value ν_0, ν', ν'', ν''', and so forth, of a natural frequency of oscillation of the entire system. With known constants of all the machines, it is easy to lay out such a frequency diagram. Figure 7 shows that the points of intersection within the generator range are in the order of magnitude of the frequencies σ. However, the actual natural frequencies are somewhat greater than the inherent frequencies of every synchronous machine. The value $\sqrt{\gamma^2+\sigma^2}$ indicates the increase of the electrical frequency by effect of the mechanical power released by the governor which acts in addition to the synchronizing electrical power. With the figures of our examples, we have

$$\sqrt{\gamma^2+\sigma^2}=\sqrt{1.25^2+9.67^2}$$
$$=\sqrt{1.57+935}=9.76$$

indicating an increase of only two per cent. A further increase is due to the effect of the finite value of the motor masses. Only if these approach infinity, would the network become completely rigid and every generator unit would oscillate independently with frequency $\sqrt{\gamma^2+\sigma^2}$, as shown by Figure 7.

If we consider equation 36, we see that for a certain frequency ν the amplitude of that machine only is large compared with the network amplitude A_n, the inherent frequency σ of which is in the neighborhood of the natural frequency ν considered, γ being comparatively small. For all the other machines, the denominators of equation 36 are much larger. We see, therefore, that every synchronous machine oscillates preponderantly with that natural frequency which is the nearest to its inherent frequency but oscillates only slightly with all the other natural frequencies. The actual number of natural frequencies in the generator range equals the number of generators operating in the system.

In the motor range of frequencies, all relations correspond to those in the generator range, except that Figure 7 shows the intersections to be nearer the zero line and hence the differences of the natural and inherent frequencies may be larger for every motor. Therefore the individual motors, according to equations 29 or 37, may not respond with as large amplitudes as the generators do but will oscillate at every natural frequency with moderate magnitude only.

Since the numerical values of the natural frequencies for the generator range are much smaller than those for the motor range, the dashed motor curve in Figure 7 runs through the generator range as a nearly horizontal line of a value $-\Sigma S_m$, the quotients on the right-hand side of equation 39 here all being nearly one. Hence the motors react on the generator oscillations as if their inertias were combined into one large common system inertia, forming the center of inertia of all the network motors.

This is in full accord with equations 28 and 29, showing that the power fluctuation of every motor is thus determined only by its individual inertia S and the variation of the common network angle θ, so long as the actual frequency ν is much smaller than the inherent motor frequency μ. Hence for every slow oscillation, we can combine all the motors into one large equivalent induction motor which has the sum of all the inertia masses of the individual motors. Even if, in some cases, ν/μ should not be small, but of considerable magnitude, the contribution of such motors to the common inertia ought merely to be increased, corresponding to the denominator of equation 29. The larger the sum of the motor inertias, the deeper lies the horizontal part of the dashed line in Figure 7 and the nearer the actual natural frequencies of the generators shift to the inherent values expressed by $\sqrt{\sigma^2+\gamma^2}$.

If there should be a synchronous motor in the system, its inherent frequency σ_z would be moderately high and thus would lie within the synchronous-generator range. However, the value γ^2, expressing the speed-governor action, would vanish in that additional term of the left-hand side of equation 39 which belongs to this motor. Thus that term attains the value

$$S_z\frac{\sigma_z^2}{\sigma_z^2-\nu^2}$$

which has the same form as for induction motors except that σ, following equation 17, is much smaller than μ usually is.

For the motor range, on the other hand, with high natural frequencies ν, the solid line of the synchronous generators in Figure 7 approaches the constant value zero, since all the fractions on the left-hand side of equation 39 vanish rapidly with increasing ν^2. Therefore the induction motors oscillate in all their natural frequencies almost as if no synchronous generators were present in the system. The number of intersections and thus the number of natural frequencies in this range, however, is smaller by one than the number of motors in operation.

If there should be an asynchronous

induction generator in the system, driven by a prime mover with speed governor, we must add in equation 39 an expression

$$S_y \frac{\mu_y^2(\nu^2-\gamma_y^2)}{\nu^2(\gamma_y^2+\mu_y^2-\nu^2)}$$

which is built up like that of the generators but has an inherent frequency μ_y more of the order of that of the motors.

If some or even all of the generators, or some or all of the motors, should have

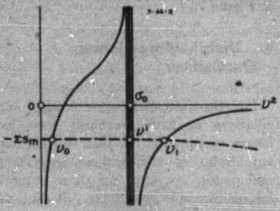


Figure 8. Representation of characteristic equation for four electrically tuned generators

the same inherent frequencies $\sqrt{\sigma^2+\gamma^2}$ or μ, the corresponding frequency quotients in equation 39 become equal, and the corresponding infinities in Figure 7 fall together, forming a multiple intersection so that, according to Figure 8, this natural frequency will coincide with the inherent frequencies of the machines.

C. Electric Tuning of the Generators

If all the synchronous generators are tuned to the same inherent frequency, say σ_0, then Figure 8 shows that besides the multiple natural frequency, $\nu'=\sigma_0$, belonging to the oscillations of all tuned generators against each other, there develops a single higher frequency ν_1 which belongs to an oscillation of all the generators together against all the motors. If we consider all the governor frequencies γ as very small and all the motor frequencies μ as very large as compared with this ν_1, equation 39 becomes in good approximation

$$(S_1+S_2+S_3+\ldots)\frac{\sigma_0^2}{\sigma_0^2-\nu_1^2} = -(S_a+S_b+S_c+\ldots) \qquad (40)$$

and thus this singular natural frequency is

$$\nu_1=\sigma_0\sqrt{1+\frac{\Sigma S_g}{\Sigma S_m}} \qquad (41)$$

For equal generator- and motor-inertia sums S_g and S_m, this predominant natural frequency would be $\sqrt{2}$ times the inherent generator frequencies. Only for infinite motor inertias, indicating a completely rigid network would this frequency reduce to the inherent frequencies σ_0 proper.

The synchronous power amplitude with which every generator participates in the oscillations is given by equation 10, omitting the second damper term. Using the angle amplitude A of equation 34 and transforming the synchronizing coefficient p_s into the frequency σ by use of the first member only under the radical of the central term of equation 17, and finally using equation 36 with neglected γ's, we obtain successively

$$\left.\begin{aligned} P_1&=p_{s1}A_1=\frac{\sigma_1^2S_1}{\omega}A_1=\frac{S_1}{\omega}\,\frac{\sigma_1^2\nu^2}{\sigma_1^2-\nu^2}A_n\\ P_2&=\frac{S_2}{\omega}\,\frac{\sigma_2^2\nu^2}{\sigma_2^2-\nu^2}A_n\\ &\cdots\cdots\cdots\cdots\cdots\cdots \end{aligned}\right\} \qquad (42)$$

Hence, in the general case, each generator follows its own resonance curve of power with the natural frequency ν varying over the entire spectrum, according to Figure 7, and the power oscillations are large for every generator in the neighborhood of its own inherent frequency σ only.

In case, however, that all the inherent generator frequencies are tuned to the same value σ_0, the frequency quotients in equations 42 become equal for all the generators, since ν_1 is now also a unique frequency given by equation 41. Hence the power oscillations of the individual generator units will be distinguished by their inherent inertia constants S only, the other factors being the same for all. Thus no single generator will oscillate with large amplitude of itself, but the entire natural power oscillation with common frequency ν_1 will occur between all the generators in unison on the one hand, and all the motors in unison on the other hand, every generator participating with a value corresponding to the mechanical size of the machine, as given by its inertia constant S.

Equal inherent frequencies σ, according to equation 17, lead to the condition

$$\frac{k_1}{T_{s1}}=\frac{k_2}{T_{s2}}=\frac{k_3}{T_{s3}}\ldots=\text{constant} \qquad (43)$$

for every synchronous machine. This requires the tuning of the synchronizing powers of all the generators, determined by k, to their inertias, determined by the starting time constant T_s. In general, this can be done by proper adjustment of the reactive power distribution among all the generators. Such a system will have the greatest possible stability against too large individual oscillations, or even loss of synchronism by any of the generators, after any disturbance of the equilibrium.

D. Slow Governor Oscillations of the Entire System

Figure 7 shows that there exists a single very low natural frequency ν_0 given by the first intersection of the solid and dashed curves. We will derive an analytical expression for this frequency, the corresponding power oscillation constituting the slowest and therefore the most significant natural power fluctuation in the system. In the general case the natural motor frequencies and the natural generator frequencies all differ from one another and each being preponderantly correlated to one or a few machines only, these elements oscillate almost at random against one another. The lowest frequency ν_0 however, is coordinated to all the machines, since the frequency quotients of equation 36 with vanishing ν all become of the same order. Therefore, we suspect that they may all oscillate in unison, constituting considerable frequency fluctuations of the entire system.

In a power system under perfect conditions the inherent frequencies σ, determined according to equation 17 by the synchronous forces, are numerically large compared with the frequencies γ, determined according to equation 7 by the governor actions. Figure 7 shows that the σ's are large also compared with the lowest natural frequency ν_0. Finally, the motor frequencies μ, determined by equation 25, also are large compared with the frequency ν_0. We have, therefore, the inequalities

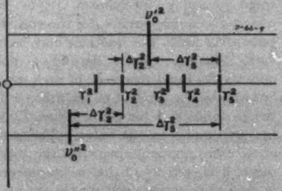


Figure 9. Spectrum of inherent governor frequencies and resultant system oscillations, as significant for distribution of power oscillations among generator units

$$\left.\begin{aligned} &\gamma_1^2 \lesssim \sigma_1^2,\ \gamma_2^2 \lesssim \sigma_2^2\ \ldots;\\ &\nu_0^2 \lesssim \sigma_1^2,\ \sigma_2^2,\ \sigma_3^2\ \ldots;\\ &\nu_0^2 \lesssim \mu_a^2,\ \mu_b^2,\ \mu_c^2\ \ldots \end{aligned}\right\} \qquad (44)$$

In the parentheses of the denominators on the left-hand side of equation 39, thus only the values σ_1^2, σ_2^2, σ_3^2 . . . remain, but they cancel against the same magnitudes in the numerators. On the other hand, the quotients on the right-hand side of equation 39 all become one, and thus we obtain in very good approximation

$$S_1\frac{\nu_0^2-\gamma_1^2}{\nu_0^2}+S_2\frac{\nu_0^2-\gamma_2^2}{\nu_0^2}+\ldots$$
$$=-S_a-S_b-S_c\ldots \quad (45)$$

Herein we can collect all the terms with ν_0 on the left-hand side and combine them with those of the right-hand side into a sum containing all generator and motor inertias, S_g and S_m. We then obtain for the lowest natural frequency

$$\nu_0^2=\frac{S_1\gamma_1^2+S_2\gamma_2^2+\ldots}{\Sigma(S_g+S_m)} \quad (46)$$

and if we use for the terms in the numerator equations 2 and 7,

$$\nu_0=\sqrt{\frac{\Sigma N_0/\delta T_c}{\Sigma(S_g+S_m)}} \quad (47)$$

This shows that there exists one slow natural oscillation of the entire system including all the motors, lines, generators, prime movers, and speed governors, the frequency of this oscillation being determined by the sum of the inertias or kinetic energies of all the machines in operation, whether generators or motors or other rotating masses, and by the sum of the quotients $N_0/\delta T_c$ of all the prime movers with their speed-governing mechanisms. We know from equation 3 that this last quotient for every prime mover expresses the power velocity of control, measured in kilowatts per second, with which its speed-governing mechanism tends to restore the steady state. For the various generating units, this quotient may have very different values, depending not only on the rated power N_0 of each unit, but also on the product of the droop δ of the governor and the closing time T_c of the servomotor of the valve. The sum of all these quotients increases the total power delivered to all the prime movers of the entire system and thus every machine plays its individual part in the whole mechanism of oscillation. Any increased power of a prime mover accelerates all the rotating masses of the system, and so all the various machines of the complete system, prime movers with their generators and all the motors, oscillate mechanically slowly in unison with one another, the energy fluctuating between the rotating masses of the entire system and the power sources of the prime movers, be they steam, water, or oil. This is in contrast to the more rapid oscillations of the individual generators or motors, the energy of which fluctuates only between the various rotating masses of the machines.

Hence, with respect to the slow governor frequency, all the rotating masses of the entire system appear as if they were rigidly coupled with one another, and the governor-controlled power admissions of all the prime movers together are acting in unison upon the total inertia. Thus for the slowest and most significant oscillations in interconnected networks we have attained a very simple conception, namely that the entire system acts as a unit with respect to governor powers and inertia masses.

Let us consider, for example, a power system containing only three synchronous prime movers of $N_0=5$, 10, and 20 megawatts rated power, with starting time constants $T_s=8$, 5, and 20 seconds, with speed governors of $\delta=5$, 3, and 6 per cent droop, and servomotors of $T_c=2$, 1, and 4 seconds closing time, while the numerous motors together have an inertia $\Sigma S_m=500$ megawatt-seconds.

The resultant governor frequency of the entire system then is

$$\nu_0=\sqrt{\frac{\dfrac{5}{5\%\cdot 2}+\dfrac{10}{3\%\cdot 1}+\dfrac{20}{6\%\cdot 4}}{8\cdot 5+5\cdot 10+20\cdot 20+500}}$$
$$=\sqrt{\frac{50+333+83 \text{ mw/sec}}{40+50+400+500 \text{ mw-sec}}}$$
$$=\sqrt{\frac{466}{990}}=0.686, \text{ or } 0.109 \text{ cycles per second}$$

while the governor frequencies of the single free-running units would be

$$\gamma_1=\sqrt{\frac{50}{40}}=1.12, \quad \gamma_2=\sqrt{\frac{333}{50}}=2.58,$$
$$\gamma_3=\sqrt{\frac{83}{400}}=0.454$$

This example shows that the medium machine of ten megawatts rated power by its governor of small droop and its servomotor of small valve-closing time produce by far the strongest forces, four times as large as those of the 20-megawatt machine. On the other hand, the inertia is attributed predominantly to the heavy 20-megawatt machine and to the entirety of the motor masses which reduce the frequency considerably. Measurements in power systems show that the starting time constant T_s of the motor inertias related to the generator power in operation, according to equation 2, is of the order of 10 to 20 seconds, this large value being due to the underloading of most of the motors.

If the electric system is severely disturbed, for instance by a short circuit near some of the generators, the synchronizing power of one or more of the electrical machines may decrease. The inherent frequency σ of these machines, given by equation 17, then drops and may come into the neighborhood of the mechanical frequency ν_0 just determined. The total number of natural oscillations is not changed hereby, only that some of the last inequalities 44 do not hold. It is useful in this case to determine all the lowest frequencies graphically, as already shown in Figure 7.

4. Distribution of Power Oscillations

For the faster electromechanical oscillations we have previously seen, by means of equation 36, that the angular amplitudes of the various generators and motors are large for those natural frequencies only which are adjacent to the inherent frequency of the individual machines. The network amplitude A_n of these oscillations will be relatively small, since all machines oscillate against each other.

The speed deviation or the relative frequency oscillation of the network, is given, corresponding to equation 14. quite generally as

$$s_n=\frac{1}{\omega}\frac{d\theta_n}{dt}=-\frac{\nu}{\omega}A_n\sin\nu t=-F\sin\nu t \quad (48)$$

Thus

$$F=\frac{\nu}{\omega}A_n \quad (49)$$

gives the amplitude of the frequency oscillation, constituting a fluctuation of the regular power frequency ω with low frequency ν. This correlation, equation 49 holds for any value of the frequency ν.

The individual power oscillation of every synchronous generator can be determined by equation 10 with its synchronous and also its asynchronous or damper contribution, the amplitude A of the angle θ of each generator being given by equations 36 with respect to the network angle amplitude A_n. Thus the distribution of the power oscillations among the generators can be accurately found for any actual example.

The power oscillation of every induction motor is rigorously given by equation 23 or 28. For moderate damping, however, we can use the much simpler equation 29, and if we confine our consideration to such network frequencies only as are considerably lower than the inherent motor frequencies μ, the second term in the denominator of equation 29 vanishes, and we obtain with use of equation 48,

$$P_m = -\frac{S_m}{\omega}\frac{d^2\theta_n}{dt^2} = -S_m\frac{ds_n}{dt} = \nu F S_m \cos \nu t \qquad (50)$$

Thus the motor power amplitudes are given in good approximation by the product of frequency ν and amplitude F of the fluctuation of the network frequency, magnitudes which are the same for all the motors, and of the inertia constant S of the individual motor considered.

The total power fluctuation over the entire system between all generators on the one hand and all motors on the other hand is merely the sum of all individual motor powers of equation 50, namely,

$$\Sigma P_m = \nu F \Sigma S_m \cdot \cos \nu t \qquad (51)$$

Its distribution over the various machines of the system and therefore over the various branches of the network is determined by the preceding remarks. This last relation equation 51 holds for the ranges of the synchronous frequencies σ as well as of the governor frequencies γ and ν_0.

For the slow mechanical oscillations of the generators we can use with perfect networks the inequalities 44 for the amplitudes of equation 36 and obtain, with good approximation,

$$\frac{A_1}{A_n} = \frac{\nu_0^2 - \gamma_1^2}{\sigma_1^2}; \quad \frac{A_2}{A_n} = \frac{\nu_0^2 - \gamma_2^2}{\sigma_2^2}; \quad \ldots \qquad (52)$$

Since, according to equation 44, both frequencies in the numerators of equation 52 are small compared with the denominators, we see that the amplitudes of the angles $A_1, A_2 \ldots$ between the synchronous machines and the network for these slow governor oscillations are always small compared with the amplitude A_n of the angular oscillation of the network itself. Therefore the total machine oscillations are nearly of the same angular magnitude as the network oscillations A_n, these being of considerable value since all machines swing in unison.

However, we can compare the relatively small angular amplitudes $A_1, A_2, \ldots$ with each other since, according to equations 10 and 34, they are a measure of the power oscillations of the single machines. The synchronous power amplitude of the generators is, with equations 10 and 34, and using the first equation 17, and finally equation 52,

$$\left.\begin{aligned} P_1 &= p_{s1}A_1 = \frac{\sigma_1^2 S_1}{\omega} A_1 = \frac{S_1}{\omega}(\nu_0^2 - \gamma_1^2)A_n; \\ P_2 &= \frac{S_2}{\omega}(\nu_0^2 - \gamma_2^2)A_n; \\ &\cdots\cdots\cdots\cdots\cdots\cdots \end{aligned}\right\} \qquad (53)$$

Thus the amplitude of the individual synchronous power oscillation of every machine is determined by its inertia constant S, and the difference of the common governor frequency ν_0 for the entire network and the inherent governor frequency γ of the individual machine. Figure 9 shows as an example the distribution of the inherent governor frequencies γ of five individual prime movers with their synchronous generators, and above and below this frequency spectrum two different values are indicated for the resultant actual governor frequency ν_0 of the entire system. The upper larger value ν_0' may belong to a system with very small motor masses so that, according to equation 47, ν_0' is more in the center of the individual frequencies, while the lower smaller value ν_0'' may belong to a system with larger motor masses which decrease considerably the joint governor frequency. The difference $\Delta\gamma$ between the joint frequency ν_0 and the inherent frequencies γ, in the quadratic scale of the diagram Figure 9 determines, according to the equation 53, the individual synchronous power oscillation of every generator unit. The asynchronous power of the second term in equation 10 usually is small compared with the synchronous power.

Thus all the generator units with different γ participate in very different degrees in the power oscillation of the system. Those with large γ, corresponding to a high velocity of control according to equation 7, deliver large electric power into the network oscillation; those with smaller γ deliver small or even negative power amplitudes into the system. These spectral differences, and therefore the subdivision of the power oscillations among the individual machines, are by no means proportionate to the rated powers of the various machines. For example, a large-sized generating unit may have a large product $\delta T_c T_s$ and therefore, according to equation 7, a small γ, and a small-sized machine of the same system may have a small $\delta T_c T_s$ and therefore a large γ. Then the small machine might participate in these power oscillations to an even greater extent than the large machine, dependent in addition on the inertia constants S. Thus it may be heavily overloaded and may even lose synchronism.

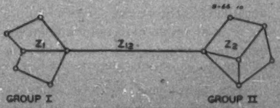


Figure 10. Two closely coupled groups of synchronous power plants, loosely interconnected by tie line of high reactance

In our former example of three different generator units of 5, 10, and 20 megawatts rated power, the distribution among these machines of the power oscillations of frequency ν_0 would be, according to equation 53,

$$\left.\begin{aligned} P_1 &= 8\cdot 5(0.686^2 - 1.12^2) = -40\cdot 0.78 \\ &= -31.2\cdot\frac{A_n}{\omega} \\ P_2 &= 5\cdot 10(0.686^2 - 2.58^2 = -50\cdot 6.19 \\ &= -309\cdot\frac{A_n}{\omega} \\ P_3 &= 20\cdot 20(0.686^2 - 0.454^2) = 400\cdot 0.264 \\ &= +106\cdot\frac{A_n}{\omega} \end{aligned}\right\}$$

These figures show a disproportionate subdivision in magnitude as well as in sign, the medium machine taking over the bulk of the power oscillations, and the large unit acting in phase opposition to the smaller machines. The entire synchronous generator power oscillates with an amplitude

$$P_g = 106 - 309 - 31 = -234\cdot\frac{A_n}{\omega}$$

indicating an extremely unfavorable distribution among the generators. On the other hand, the entire motor power oscillation within this system is, according to equation 51, and with use of equation 49,

$$P_m = \nu\frac{\nu A_n}{\omega}\Sigma S_m = 0.686^2\cdot 500\frac{A_n}{\omega} = 236\cdot\frac{A_n}{\omega}$$

checking closely the total generator value.

If the number of generator units and motors changes, as is the case with varying conditions of operation of all electric systems, the value of ν_0 in equation 53 and in Figure 9 may change over a certain range, with the result that another generator may become more overloaded than before. Always the units with the smallest $\delta T_c T_s$ are predominantly endangered. For the three machines of our example, this characteristic value is $\delta T_c T_s = 5\cdot 2\cdot 8 = 80$ and $3\cdot 1\cdot 5 = 15$ and $6\cdot 4\cdot 20 = 480$. Widely different inherent governor frequencies causing unbalanced load distribution in the transient state of the system are especially found if steam and water power plants are interconnected.

Under one condition only can the overloading of one or another machine be avoided and the subdivision of the oscillating power among all machines remain the same under all circumstances, namely, if we tune the various speed-governing mechanisms so that all give one and the same inherent value γ for every machine. Then, all the frequency differences from any joint frequency ν_0 in equation 53 or Figure 9 attain the same magnitude. Even if the value of ν_0 should change because of a varying number of generators or motors in operation, see equation 47, the frequency differences for the various generators still remain equal between each other. Therefore the synchronous power oscillations of the generators, according to equation 53, are merely proportional to their inherent inertias S_1, S_2, and so forth, a subdivision corresponding to the mechanical size of each machine. Equal γ, according to equation 7, leads to the condition

$$\delta_1 T_{c1} T_{s1} = \delta_2 T_{c2} T_{s2} = \delta_3 T_{c3} T_{s3} \ldots = \text{constant} \quad (54)$$

for every power unit consisting of synchronous generator, prime mover, and governor. This requires a tuning of the speed-governing mechanism with respect to the starting time constant T_s for each machine, which in general can be attained. Such a system will have the greatest possible stability against too large joint oscillations, so that no generator will be overloaded or even lose synchronism with the other generators after a heavy impact of any type on the network.

5. Supplementary Oscillations

There may be examples in which the reduction of the electrical distribution network into a simple star system cannot be performed, either in perfect or in faulty state of the system. In such cases two or more loosely coupled star systems may give a proper approximation. Since all oscillations here treated are due to the energy storage in the various rotating masses, we see immediately that the number of the natural frequencies will not be changed; only the spectral distribution may shift to some degree. If, for example, two groups of machines, each comprising generators and motors, are closely coupled within the groups but loosely between the groups, as in Figure 10, these two groups will be able to oscillate against each other, and the corresponding natural group frequency, dependent preponderantly on the value of the coupling impedance, can be much lower than any individual inherent frequency σ. The general scheme of deriving the actual frequencies of such networks can closely follow the procedure of section 3*A*.

If the coupling of such network groups is so loose that a resultant natural frequency of the electrical machines drops into the region of the slow governor oscillations, even these may be affected and a beating of these two low frequencies may reduce the oscillatory stability of the entire system.

Some types of speed governors do not follow with sufficient accuracy a relation like equation 4 but possess more complex characteristics caused, for example, by droop compensation, by use of intermediate pilot valves, by long water or fuel supply pipes, and so forth. In every case, the equations of operation of the governors and prime movers can be combined, similar to the manner shown in section 3*A*, with those of the electrical system which remain unchanged. As a result, there may appear some additional natural frequencies, attributable for example, to frequencies inherent in the governor systems, which sometimes are much higher than the preponderant low governor frequency γ and which therefore may enter the range of the synchronous-machine frequencies σ. Since both these types belong to natural oscillations and not to forced oscillations, no possible resonance effects can occur. Only the spectrum of the natural frequencies will change to some extent, not causing, however, any additional disturbances.

In large distribution systems, particularly with interconnected networks, the frequency of the entire system, or the power distribution among the different branches, is sometimes controlled by an additional electromechanical device. No standard equipment or customary operation of such superposed control system has been developed as yet, but with all of them the deviation of frequency, angle, or power from the desired values at certain points in the system serves to induce one or more prime movers by influencing their speed governors to compensate for the difference.

In the light of our analysis, such devices introduce another set of restoring powers into the system, very similar to the restoring power of equation 4, and additional oscillations may result. If such superposed control is restricted to one or to some of the generator units only, the restoring power for the entire interconnected system is relatively small compared with that of all speed-governed prime movers together. Therefore the frequency of oscillation for such a scheme of control, in analogy to equation 47, will be still much lower than that of the joint governor oscillations, and usually these very slow power swings are slightly damped by internal forces in the additional control device.

Since such control frequency is always much lower than the electrical frequencies σ and μ between the machines, all the rotating masses will follow in unison, and thus we can use equation 47 for the determination of the control frequency, if only the total power velocity of control in kilowatts per second of the control device is given. Hence every program control of any number of machines will always affect the inertia masses of the entire system as if they were rigidly coupled to a unit.

6. Damping Effects

We have considered the interaction of all the coupled oscillating elements, neglecting their damping forces. Let us supplement the results by discussing qualitatively the influence of these dissipative effects.

With inclusion of the various damping members, the differential equation 31 for the individual angles of deviation θ of the synchronous machines, as derived from equation 30 would contain terms with $d\theta/dt$. The first such term, because of the governor damping, originates from equation 4 by substituting the slip from equation 14 and integrating:

$$N + \int \frac{N\,dt}{T_c} = \frac{N_0}{\delta T_c} \frac{\theta + \theta_n}{\omega} \quad (55)$$

Substituting T_c on the left-hand side from equation 8, we obtain for a power oscillation of any given frequency ν,

$$N = \frac{N_0}{\delta T_c} \cdot \frac{\theta + \theta_n}{\omega} \cdot \frac{1 + j\dfrac{\rho_\gamma}{\nu}}{1 - \left(\dfrac{\rho_\gamma}{\nu}\right)^2} \quad (56)$$

if we shift the complex factor, resulting from the integral, to the right-hand side.

Comparing this with the first term of equations 30 and 31, the last quotient of equation 56 results in a slight increase in N by effect of the denominator and in a damping effect for every prime-mover unit by action of the imaginary term $j\rho_\gamma/\nu$. Since each governor furnishes such damping term in the oscillations of the entire system, the low common governor oscillation with frequency ν_0 will suffer a damping effect which is determined by the average ρ_γ/ν_0 of all the interconnected prime movers. Since ν_0 is decreased by the large inertias of the induction motors in the system, the resultant damping is heavier than with

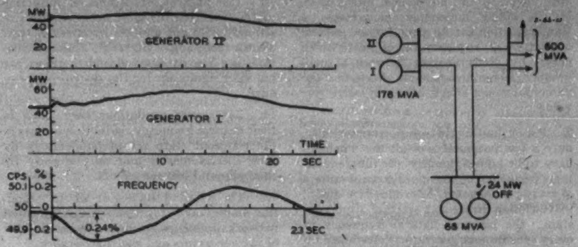


Figure 11. Test on 800-megavolt-ampere system, showing the various frequencies of power oscillations after heavy impact, caused by disconnecting one of feeding generators

the generators alone. However, the oscillations usually remain within the periodic region.

Since the governor damping is inversely proportional to the frequency ν of the oscillation considered, this effect vanishes almost completely for the much faster oscillations with frequencies near σ of the synchronous generators and near μ of the induction motors.

The damping in the synchronous machines is due to the last term of equation 10. For any given frequency ν we have, therefore, for every generator unit the electric power oscillation

$$P=\left(p_s+jp_a\frac{\nu}{\omega}\right)\theta=p_s\left(1+j\frac{\nu\rho_\sigma}{\sigma^2}\right)\theta \qquad (57)$$

equations 17 and 18 being used for the value of p_a/p_s.

This expression enters equations 30 and 31 in place of the last term, producing a damping effect from every individual generator by action of the imaginary term. Since the natural frequencies ν for the entire group of generators are all in the same range, these damping terms of the synchronous units all act with a similar strength and after some time produce decay in all the natural generator oscillations. For the slow governor oscillations ν_0, however, the damping term of equation 57 becomes extremely small, being proportional to the frequency ν, and thus the electric damping in the synchronous machines has practically no influence on the slow power oscillations caused by the governors. The motor oscillations with higher natural frequencies, however, are more strongly influenced in the proportion of the μ range to the σ range.

The damping effect of the slip in induction motors is given by the second term of equation 23. It constitutes a correction of the active power of the first term of that equation, which is for any frequency ν,

$$P+s_0T_s\frac{dP}{dt}=P(1+j\nu s_0T_s)$$
$$=P\left(1+j\frac{\nu\rho_\mu}{\mu^2}\right) \qquad (58)$$

equations 25 and 26 being used for the value of s_0T_s. The imaginary term for every motor causes a damping of all the natural motor oscillations, the effect of the various induction motors being greater the nearer the damping comes to the aperiodic condition of equation 26. Since it is not possible to co-ordinate an actual natural frequency to a single motor, see Figure 7, the damping effect of every motor spreads out over the whole group. The motor damping, according to equation 58, is proportional to ν, and thus its influence on the much slower generator oscillations is small, and particularly that on the governor oscillations is negligible.

Hence the damping effects of governor, generator, and motor oscillations remain in their respective ranges, with the exception only of the generator damping, which overlaps the motor range. The motor oscillations proper are damped, therefore, by two effects, both of which are considerable, and thus are not to play an important part under conditions of unbalance of the system after any disturbance. Individual generator oscillations and joint governor oscillations are the important phenomena which determine the stability of interconnected systems, while all the motors merely act together with the combined inertia of their rotating masses.

In all our derivations we have assumed that the magnitudes of the generator voltages remain constant during any oscillation and that the influence of the network resistances is insignificant, the reactances playing the predominant part. It is well known, however, that even with constant excitation of the synchronous generators, their voltage varies proportionally to the instantaneous speed and, therefore, oscillates to some extent, and that this variation gives rise to a corresponding oscillation of power losses within the resistances of the network, series resistance of the lines as well as parallel resistance of the loads. Thus a network damping occurs, additional to the damping effects within the active elements just considered, the value of which depends largely upon the distribution of resistance-, motor-, and arc-consumers within the system. Since such network damping produces merely a corrective effect which can be either positive or negative, it will not be further considered here. Similarly, the resistances and pressure drops within the steam, water, or fuel system, increasing with the load, cause an additional damping which also is of minor significance for the value of the natural frequencies and will be neglected here.

Any arbitrary change of voltage in one or more of the generators by external change of their excitation acts more on the reactive than on the active power of the machines and therefore influences more the value of the synchronizing forces, see equations 11 and 17, than the damping effects. It therefore merely causes the frequencies to vary slowly during subsequent oscillations.

The absolute amplitudes to which all the oscillations are excited by sudden impacts or by arbitrary or accidental changes in the system remain to be investigated.

Figure 11 shows results of a full-scale experiment with an interconnected system containing scores of generators, totaling a rated power of 800 megavolt-amperes, which was subjected to a power impact of minus 24 megawatts, attained by sudden disconnection of one of the feeding generators. The network frequency in consequence shows a damped oscillation of a period of 23 seconds, corresponding to $\nu_0=0.274$, caused by the combined action of the speed governors in the system. The relative amplitude of this frequency oscillation is $F=-0.24$ per cent of the normal frequency. Two generators measured in a distant power plant show a slow power oscillation of the same frequency but of different amplitudes and, in addition, a more rapid oscillation for each of them in the beginning, the periods of which are 1.57 and 1.40 seconds, respectively, and which are due to the different synchronous forces of these individual machines.

7. Conclusions

1. Notwithstanding the complexity of the problem of interrelation of the many tran-

sient electrical and mechanical forces existing in widely interconnected systems, a simple solution can be given for the natural oscillations of frequency and of power which occur, by discriminating between a small number of significant major effects and the numerous minor effects of merely corrective value.

2. For evaluating this solution numerically, only a few parameters of all the machines have to be known, namely: the inherent natural frequency of the speed-control system of every prime mover, determined by droop, valve-closing time, and starting time constant; the inherent natural frequency of every synchronous generator, determined by network frequency, synchronizing coefficient and the same value of starting time constant; and, although not necessarily, the inherent frequency of the larger induction motors, determined by leakage voltage and their starting time constant.

3. All of the many rotating machines in an interconnected system are coupled by the electric energy which they can exchange in the transient state. Thus they perform coupled oscillations, the frequencies of which are different from those inherent in the single machines when not connected together.

4. The characteristic equation for the determination of the coupling frequencies is of a very high degree, given by the number of oscillatory elements, namely, the total number of prime movers with generators, and of motors, in the entire system. An analytical solution of such an equation is not practicable, but a graphical representation can be developed, giving an excellent survey of the dependence of the frequencies and the power amplitudes on the various parameters.

5. There exists no isolated oscillation of any one machine with respect to a large network. All oscillations, although they may be excited individually, spread out over the entire network affecting every line and every rotating machine.

6. The natural frequency of oscillation of every synchronous generator is increased by the reaction of the mechanically coupled speed governor of its prime mover, and by the electric coupling through the network with other rotating machines.

7. Tuning of the inherent frequencies of all the synchronous generators to the same value, accomplished by proper distribution of the reactive power among the generators insures their oscillating in unison during the transient state and results in maximum stability of the entire system.

8. A single induction motor is capable of natural oscillations of moderate frequency if the slip at rated load is under a certain limiting value. For larger rated slip, aperiodic gliding movements occur in the transient state.

9. The limiting rated slip of induction motors, separating their aperiodic and oscillatory behavior, is determined by network frequency, relative leakage voltage and starting time constant. Since the limiting slip is of an average value of five per cent, small motors perform aperiodically while large motors may oscillate.

10. The natural oscillations of induction motors fed from the same network interfere strongly with one another by effect of their electric coupling. However, these oscillations are heavily damped and therefore are not very significant for the system performance.

11. For slower oscillations than those in their natural frequency, induction motors in the transient state act preponderantly by their inertia masses and only slightly by their slip and leakage effects.

12. The rotating masses of all the induction motors taken together in interconnected networks influence strongly the oscillations of the generators and speed governors.

13. All the speed governors taken together constitute a unit of restoring forces, acting not only on the inertia masses of all the prime movers and generators, but also on the inertia masses of all the induction motors and other rotating machines, all in unison.

14. Independent of the number of machines in the interconnected networks, there develops only a single low frequency by action of all the speed governors, this frequency being given merely by the sums of all the restoring forces and all the rotating masses of the system.

15. Although all the governors oscillate in unison at very low frequency, the electric power exchange of their generators with the interconnected system may be entirely disproportionate, the units with low inertia, small droop and low valve-closing time being overloaded, while those with large values may even be negatively loaded by the oscillations, thus driving their inertia masses as synchronous motors.

16. Tuning of the inherent frequencies of all the speed governors to the same value, secured by adapting the properties of the speed-control mechanism of each prime mover to the inertia of its rotating masses, insures a proportionate distribution of power in the transient state and results in maximum stability of the entire system.

17. Such tuning requires that the product of speed-governor droop, valve-closing time, and starting-time constant be the same for every generator unit. Thus heavy machines with relatively high mass inertia should be controlled by fast servomechanisms and speed governors of small droop, while relatively light machines should have slow servomechanisms and larger governor droop.

18. In tuned conditions of governors and of generators, the distribution of power oscillations among the generator units corresponds to the inertia of their rotating masses which usually are in proportion to the mechanical and electrical size of the machines.

19. Oscillations of power and frequency produced by the action of any type of power or frequency program of control, superimposed on a limited number of prime movers, have a very low frequency on account of the relatively weak restoring forces acting on the inertia masses of the entire system.

20. By effect of the various restoring forces, three main groups of oscillations prevail in interconnected systems, influencing one another but being distinguished by their frequency ranges; namely, a motor range of 5 to 10 cycles per second; a generator range of 1 to 2 cycles per second; and a governor range of 0.05 to 0.1 cycles per second.

21. The entire number of frequencies to be expected in interconnected networks under perfect conditions with all machines different, is in the governor range equal to one, in the generator range equal to the number of synchronous generators, and in the motor range equal to the number minus one of oscillating motors.

22. For power systems loosely interconnected by high reactance lines and for power systems under faulty condition of their lines, one or more of the medium range generator frequencies may shift into the low range of the governor frequency.

23. Records of full-scale tests on large interconnected power systems while suddenly imposing a severe power impact, show very slow frequency oscillations at the bus bars, and faster power oscillations of the generators, consistent with the foregoing conclusions.

24. All the natural oscillations suffer a continuous damping, preponderantly caused by the effect of the return of the speed governors, the damper of the synchronous generators, and the slip of the induction motors.

25. The damping of the slow governor oscillation of the entire system depends upon the average damping force of the returns of all the speed governors and is considerably increased by the effect of the rotating motor masses, but not so much as to become aperiodic.

26. The damping of the faster synchronous generator oscillations throughout the system depends preponderantly upon the average damping force of all the damper cages in the generators and always remains in the periodic range.

27. The damping of the still faster induction motor oscillations throughout the system depends upon the joint effects of the slip in the motors and the dampers in the generators. Smaller motors with considerable slip glide in the aperiodic range, while larger motors with small slip oscillate in the periodic range, but with heavy decay of their amplitudes.

8. Summary

Electric generating and distribution systems are capable of performing natural oscillations of frequency and of power, superposed upon the ordinary operation and sometimes disturbing the regular behavior of the system or its parts. These oscillations are due to the interlinked cooperation of the prime movers with their speed-governing mechanisms, the synchronous generators, the ramified network, and the numerous induction motors, and similar power consumers. Since most of the machines and network branches usually are different in size and in individual design, a multitude of

natural oscillations will develop, the frequency spectrum of which can be divided into three groups.

In perfect condition of the system, a single very low frequency develops, which is determined by the inherent properties of all speed governors and servomotors together, and by the inertias of the rotating masses of all the generators and motors together. This joint natural oscillation has a period around 10 to 20 seconds and causes an inphase movement of all mechanical parts of the system, which can be of considerable magnitude, while the electric power may oscillate in phase opposition within the various parts of the system.

The next group of frequencies contains as many natural oscillations as there are different synchronous machines in the system and is defined by the faster electromechanical oscillations occurring between each synchronous generator and the entirety of all the other synchronous and induction machines. The mechanical parts of the entire power system oscillate here in phase opposition at all the different frequencies which have periods of about 0.5 to 1 second and are of moderate amplitude as long as the system is in perfect condition.

The third and highest group of natural frequencies is caused by the different inertia effects of the various induction motors, all oscillating against each other with periods around 0.1 to 0.2 seconds. The amplitudes are usually insignificant since they are damped by the effect of the rotor slip of each machine, for small motors even aperiodically.

During every transient state of the system, such oscillations produce slow and fast exchanges of power between the various parts of the network, temporarily increasing the load on some machines or in some tie lines and decreasing it on others, which often is detrimental to good operation. However, certain tuning conditions are derived under which the amplitudes of all these oscillations reduce to a minimum, thereby securing the highest possible stability of the entire system after any disturbance.

Tests during the operation of a large system of interconnected networks after a severe impact show frequency and power oscillations consistent with the derivations in this paper.

Special Symbols

A = angular amplitude
F = frequency amplitude
N = mechanical power
P = electric power
S = inertia constant
T = time constant
e = relative voltage
g = gravity constant
$j = \sqrt{-1}$
k = synchronizing coefficient
p = specific power
γ = governor frequency
δ = droop of governor
θ = pole angle
μ = motor frequency
ν = natural frequency
σ = synchronous generator frequency
ρ = damping factor

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The Sorocabana Railway Electrification

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Synopsis: The Sorocabana Railway electrification is noteworthy in that the railroad is a meter-gauge line run largely on wood fuel. The lack of suitable national coal or oil sources and the increasing difficulty of obtaining wood fuel led the railway to electrify its line between São Paulo and Santo Antonio.

Rolling stock is outstanding in that the locomotives will be the most powerful 3,000-volt narrow-gauge units ever built. The multiple-unit cars are the first 3,000-volt meter-gauge units to be used.

The distribution system is designed to minimize the use of steel supports. Steel must be imported, and locally made concrete poles are to be utilized extensively.

The power-supply system using 3,000-volt mercury-arc rectifiers is the first installation in this hemisphere to use rectifier conversion with regenerative braking on locomotives. Regenerated energy returned to the substations will be dissipated in resistors roof-mounted at the several stations.

THE Sorocabana Railway—the EFS (Estrada de Ferro Sorocabana) as it is commonly known—consists of a network of meter-gauge lines traversing the southern and southwestern parts of the state of São Paulo in Brazil. The system is confined entirely within the state.

The network is composed of two principal lines:

(*a*). The east–west line, which extends 556 miles from the city of São Paulo westward to Presidente Epitacio, a small town on the Paraná River, the border line between the states of São Paulo and Matto Grosso

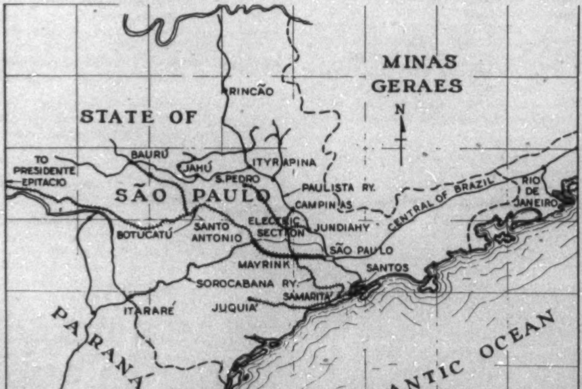
(*b*). The north–south line, which runs from the port of Santos to San Pedro—a distance of 238 miles—crossing the east–west line at Mayrink, a junction point approximately 43 miles west of São Paulo

and the several branch lines emanating from these main arteries as shown in the sketch map of Figure 1.

The two principal lines account for 794 miles of the total line trackage of 1,340 miles. All of the line trackage is single with the exception of that portion between São Paulo and Santo Antonio on the east–west line. This 87-mile-long section is double-tracked and is the territory selected for the initial application of electric traction.

Figure 2 is a condensed profile of the line now being electrified, and Figure 3 shows the profile of the line between sea level and the junction of the north–south and east–west lines at Mayrink. Orientation of the two profiles by means of the station (Mayrink) common to both will indicate the elevated location with respect to sea level of the São Paulo–Santo Antonio section.

Figure 1. Sketch map showing Sorocabana Railway lines



Although the railway is owned by the state of São Paulo, it is managed and operated as a practically autonomous institution responsible to the State Secretary of Transportation and Public Works. In gross income, the system is the second largest in Brazil and is exceeded only by the federally owned and operated Central Railway of Brazil.

The predominant factor influencing the decision to electrify is that of locomotive fuel supply. Brazil has no developed sources of fuel oil. Native coal, because of its low Btu content, is not entirely satisfactory as a locomotive fuel. Not only is the coal of a poor quality, but sufficient coal to meet all of the country's requirements is not mined—a state of development attributable to the poor quality of the coal. Steam-locomotive fuels, alternative to native coal, are imported coal and oil and native wood.

The long hauls between the nearest oil fields and the points of use in Brazil make the use of oil as locomotive fuel economically prohibitive.

Many of the railroads employ English coal in normal times and, since the incidence of the present world-wide war, coal imported from the United States.

In 1939 the Sorocabana used 6,500 tons of imported coal, 6,015 tons of native coal and 502,000 cords (128 cubic feet per cord) of wood* at a total cost of approximately $1,593,000. This represents 30 per cent of the railway's total operating expenses. Although the comparison may not be strictly balanced, it is of interest to note that during the year 1939 fuel expenditures for railroads in the United States averaged but nine per cent of the total operating expenses.

Because of its relatively low heating value, wood fuel requires the handling of large volumes, compared to coal, and on the Sorocabana there were assigned to nonrevenue company fuel-haulage service 25 locomotives and 360 flatcars—figures which represent eight per cent of the total motive power and 20 per cent of the flatcars owned.

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* 1 cord of wood = 1,000 pounds of Cardiff coal on an equivalent heating basis

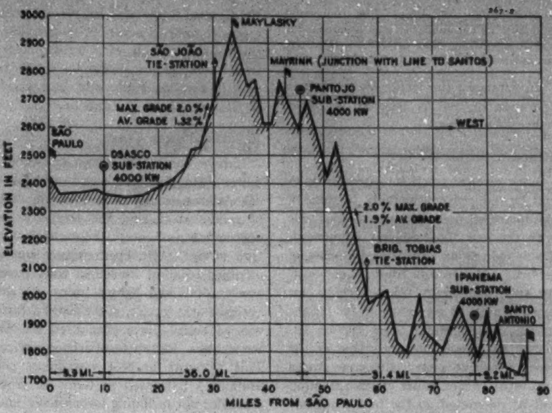


Figure 2. Condensed compensated profile—Sorocabana Railway—São Paulo to Santo Antonio double-track meter gauge

A factor which further aggravates the fuel-supply situation is that of increasing scarcity of suitable wood supplies. Excluding that section of the railroad in the extreme western part of the state, fire wood supplies contiguous to the main-lines are inadequate, and it has been found necessary to construct branch lines—some of these are 15 miles in length—expressly for wood-fuel haulage.

That the fuel-supply problem eventually would become critical has been recognized for some time, and as far back as 1924 electrification of various sections of the line was studied. In the interim, traffic has continued to increase in pace with the expansion and development experienced by the territory traversed. Preliminary studies indicated the economic desirability of electrifying the main east–west line between São Paulo and Botucatú and the north–south line from Samaritá (12 miles from Santos) to Mayrink. Further studies developed the feasibility of electrifying initially the double-tracked portion of the east–west line between Sao Paulo and Santo Antonio—since this section carries the greatest traffic density—leaving the Samaritá–Mayrink and the Santo Antonio–Botucatú sections for future electrification extension.

Hydroelectric power is relatively abundant and cheap in the territory served by the section to be electrified and for these reasons seems to be the logical solution for the difficult fuel problem.

Economics of Electrification

In studying the economic feasibility of electrifying the São Paulo–Santo Antonio line, the commission appointed by the state selected as a basis for its calculations the average traffic conditions expected to hold for the ten-year period 1942–1952, that is, the conditions corresponding to those anticipated for the year 1947.

The railway's records for the year 1939 show the performance for the São Paulo–Santo Antonio section indicated in Table I.

Statistics for the period 1928–1938 show that the increase of passenger and mixed trains, as compared with freight trains, has been small. During this period the annual traffic increase for the whole railway system averaged 6.2 per cent. It will be well to note that during these ten years three abnormal periods were experienced—the economic crisis starting in 1929, and the civil disturbances occurring in the years 1930 and 1932—and these served to lower the traffic index figures to 92, 97, and 94, considering the 1928 index as 100. Because of this, the average rate of increase in freight-train traffic for the period 1942–1952 has been taken at seven per cent, and on this basis the anticipated performance for the year 1947 will be 56 per cent greater than that of 1939.

In forecasting passenger-train performance, it was assumed that the use of electric locomotives more powerful than existing steam units would permit maintaining the same number of train-miles as in 1939, at the same time increasing the tonnage hauled by the addition of cars to both through and suburban trains. The projected 1947 figures appeared as shown in Table II.

Steam Operating Costs

Unit steam operating costs were based on 1938 figures, since those for 1939 were not available at the time the study was under way. Wages involved in the operation of various classes of trains per 1,000 train-miles during 1938 are shown in Table III. The average cost of locomotive repair and maintenance amounted to $31.50 per 1,000 locomotive-miles. The average cost of fuel per 1,000 gross ton-miles of traffic amounted in 1938 to $1.485.

The average costs of fuel prevailing during 1939 is shown in Table IV.

Water costs amounted to an average of 2.3 cents per 1,000 gross ton-miles.

Lubricant (oil and waste) costs amounted to $3.86 per 1,000 locomotive-miles.

The aforementioned unit costs being combined with the projected traffic figures, the cost of operating the São Paulo–Santo Antonio line with steam motive power would amount to—so far as the items to be affected by electric operation are concerned—the figures shown in Table V. Locomotive-miles are assumed to be equal to train-miles.

Electric Operation

Unit cost data utilized for estimating the expenses expected under electric operation are based on statistics furnished by the Central Railway of Brazil, which operates an extensive multiple-unit-car suburban service out of Rio de Janeiro, and by the Paulista Company—in the case of passenger-, mixed-, and freight-train operation—which operates a 3,000-volt main-line electrification in Brazil.

In estimating the performance with electric traction, the following assumptions have been made:

1. That the through passenger service will be identical with that under steam traction.

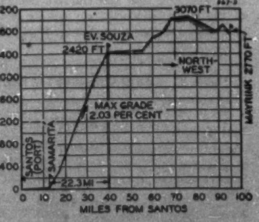


Figure 3. Condensed profile—Sorocabana Railway—Santos to Mayrink

Table I. Performance of São Paulo–Santo Antonio Section of Sorocabana Railway, 1939

	Train-Miles	1,000 GTM*
Passenger	273,000	78,500
Suburban	181,200	24,400
Mixed	128,300	25,800
Freight	1,042,000	366,200
Totals	1,624,500	494,900

* Gross ton-miles, not including locomotives.

Table II. Anticipated Performance of São Paulo–Santo Antonio Section of Sorocabana Railway, 1947

	Train-Miles	1,000 GTM
Passenger	273,000	100,500
Suburban	181,000	31,200
Mixed	128,300	36,150
Freight	1,623,000	572,000
Totals	2,205,300	739,850

Table III. Wages Involved in Operation of Trains Per 1,000 Train-Miles, 1938

Class of Train	Operating	Repairs
Passenger and suburban	$16.26	$33.10
Mixed	$22.40	$21.85
Freight	$30.05	$28.15

Table IV. Average Costs of Fuel, 1939

Wood	$ 2.37 per cord (128 cubic feet)
Imported coal	$13.63 per ton (2,000 pounds)
Brazilian coal	$ 5.90 per ton (2,000 pounds)

2. That suburban trains out of São Paulo will perform the same number of train-miles, but the ton-miles involved will be reduced from 31,200,000 to 26,650,000, because of the difference in dead weight between the steam-hauled cars and the multiple-unit cars.

3. The mixed-trains data are based on an assumption of six trains daily between Barra Funda (the São Paulo yards) and Sorocaba, and four trains between Sorocaba and Santo Antonio, with a total of 170,500 train-miles and 61,500,000 gross ton-miles.

4. In the case of the freight trains, it has been assumed that the tractive utilization of the electric locomotives will be 90 per cent, and the average train tonnage will be approximately 520 tons. For the same quantity of ton-miles as was assumed for steam operation, that is, 572,000,000, the train-miles will amount to 1,100,000.

In summary the traffic under electric operation in 1947 is expected to be as indicated in Table VI.

Electric Operating Costs

With electric traction, schedule times will be reduced considerably because of the increased operating speeds available with the new motive-power equipment. These reductions vary with the class of service: for passenger trains 23 per cent; for suburban trains, 25 per cent; for mixed trains, 13 per cent and for freight trains, 25 per cent. Since certain expenses are irreducible, regardless of the type of motive power, the crew wages and repair wages under electric traction will be calculated on the basis that but 90 per cent of these reductions will be realized. Labor costs per 1,000 train-miles will then amount to the figures shown in Table VII.

Electric-locomotive maintenance costs are prorated from actual cost data supplied by the Paulista Company lines. The average cost of labor and maintenance on electric locomotives on these lines during the years 1935–1937 was 1.385 cents per locomotive-mile. During the same period, the average cost of labor and maintenance for steam locomotives on the Paulista Company meter-gauge lines was 1.85 cents per locomotive-mile. If this ratio is assumed to prevail on the Sorocabana Railway lines, the cost of labor and maintenance on electric locomotives will be

$$\$31.50\times\frac{1.385}{1.85}=\$23.55/1{,}000 \text{ locomotive-miles}$$

For the suburban trains, it is assumed that the cost of maintenance on the mechanical portions of the cars will be the same as under steam operation. Consideration will be given, therefore, only to maintenance costs on the electrical equipment. The unit cost is taken as that found in the years 1938–1939 for multiple-unit cars in suburban service on the Central Railway of Brazil. The cost amounts to $7.08 per 1,000 train-miles—the train consisting of a trailer–motor–trailer combination similar to that to be used in the São Paulo suburban service on the Sorocabana Railway.

Electric-Power Costs

Energy consumption and consequent power costs have been based on detailed calculations, taking account of the class of rolling stock and the profile to be operated, for all except the multiple-unit train service. Actual performance figures, modified to allow for longer runs, fewer accelerations and lighter trains, for the multiple-unit service on the Central Railway of Brazil, have been used for the Sorocabana Railway suburban service. This amounts to 50.1 watt-hours per ton-mile. Expressed in terms of gross ton-miles not including locomotives, and considering the power at the motive-power pantographs, these values are as given in Table VIII. With an efficiency of conversion and distribution at 87 per cent, energy consumption for the various services, without taking into account regenerated energy, will be as shown in Table IX.

The maximum demand taken from a plot of power demand against time for a train sheet representing a maximum day was calculated at 11,200 kilowatts measured at the a-c incoming lines.

Regenerated energy according to calculations will amount to approximately 16 per cent. With this amount allowed for in all services excepting the suburban, the total purchased energy will come to 37,259,000 kilowatt-hours annually.

Based on the contract signed by the Central Railway of Brazil with the Rio de Janeiro Tramway Light and Power Company, the cost of power for the Sorocabana Railway would be on a monthly basis

$$\begin{array}{lr}
11{,}200 \text{ kilowatts}\times\$0.90 & =\$10{,}080.00\\
\dfrac{37{,}259{,}000 \text{ kilowatt-hours}}{12}\times\$0.00105= & \underline{\$\ 3{,}260.00}\\
 & \$13{,}340.00
\end{array}$$

To this a two per cent tax for retirement funds is added, bringing the total monthly power bill to $13,607.

The average cost per kilowatt-hour will amount to, on this basis,

$$\frac{\$13{,}607\times12}{37{,}259{,}000}=\$0.00439 \text{ per kilowatt-hour}$$

Power charges attributable to the several services per annum—after allowing for regeneration—will amount to the figures given in Table X.

The cost of lubricating oil, grease, and

Table V. Cost of Operating São Paulo–Santo Antonio Line With Steam Motive Power

Type of Service	Passenger and Suburban	Mixed Trains	Freight Trains	Totals
Train crews	$ 7,390	$ 2,060	$ 48,800	$ 58,250
Locomotive crews	15,050	2,780	46,000	63,830
Repairs	14,300	3,990	51,100	69,390
Fuel	196,100	53,700	850,000	1,099,800
Water	3,060	840	13,180	17,080
Lubricants	1,760	490	6,290	8,540
	$237,660	$63,860	$1,015,370	$1,316,890

waste for electric motive power was estimated at $1.21 per 1,000 locomotive-miles, and for the multiple-unit trains, 40 cents per 1,000 train-miles.

An average value for distribution-system maintenance costs as reported by the Paulista Company and the Central Railway of Brazil was found to be $96.50 per mile per annum.

Substation and tie-station maintenance costs were taken on the unit basis reported by the Central Railway of Brazil, since this railway's conversion equipment is similar to that projected for the Sorocabana Railway. Rectifier conversion is involved in both cases.

Summarized, the costs under electric traction are expected to amount to the figures shown in Table XI.

COMPARISON—STEAM AND ELECTRIC TRACTION

The total annual costs of items which will be affected by the change from steam to electric motive power are

Steam	$1,316,890
Electric	308,245
Difference in favor of electric	$1,008,645

In adopting electric traction for the São Paulo–Santo Antonio line, 50 steam locomotives will be made available for rental to other divisions or roads at an annual rental price of $1,500 per locomotive—a total income credit from this source of $75,000 annually.

A further credit resulting from the electrification will accrue from the release of the large number of flatcars now used for wood-fuel transport. This has been estimated at a total of $12,000 annually.

Thus, the total economy resulting from the electrification is expected to be approximately $1,100,000 per annum—a saving which when considered for a ten-year period will more than pay for the capital investment and carrying charges involved for the new system.

The Electrification

Preliminary studies for the electrification included consideration of a-c as well as d-c systems. Considerable attention was given to a 17,000-volt $16\frac{2}{3}$-cycle single-phase system proposed by continental European manufacturers, but, after comparing the economics of this system with those of the 3,000-volt d-c system, the railway company issued its call for tenders specifying the latter system. That this choice of systems is a happy one is evident from the following.

Both the Paulista Company and Central Railway of Brazil electrifications are operated at 3,000 volts direct current. The São Paulo Railway, if and when electrified, will doubtless adopt the 3,000-volt system, since it interchanges rolling stock with and handles through trains to and from both the Paulista Company and Central Railway systems. The question of suburban electrification of the Central Railway lines out of São Paulo is perennial, and unquestionably such an electrification would be operated at 3,000 volts direct current as is the Rio de Janeiro existing suburban service of the Central Railway.

In short, the 3,000-volt d-c system seems to have been adopted as the standard main-line electrification system for Brazil, although there are one or two secondary electrifications operating at 1,500 volts direct current.

Aside from questions of standardization and interchange of rolling stock, the d-c system does not involve the severe inductive interference problems commonly encountered with the a-c systems, and, as a consequence, no relocation of telephone and telegraph circuits adjacent to the railway will be necessary, except where physical interference may exist between the communications lines and the railroad's overhead contact lines.

Regenerative braking—developed to a high degree for d-c motive power—is not obtained easily with the single-phase systems, and, because of the multitude of heavy gradients and concomitant sharp curvature, electric braking is expected to be of invaluable service.

TRAIN PERFORMANCE AND LOCOMOTIVES

The schedule of freight and passenger trains set up by the railway company called for a total of 17 locomotives in active service at one time. To meet this requirement, to provide for spares, and to allow for normal stopping, a total of 20 locomotives was specified. Ten of the locomotives are to be furnished by the General Electric Company, and the Westinghouse Electric and Manufacturing Company will furnish the remaining ten.

The railroad's performance specifications stipulated that the locomotives be capable of hauling a 12-car passenger train weighing 475 tons in either direction between São Paulo at the eastern terminus of the electrification and Santo Antonio at the western end of the run, in 150 minutes. For the distance of 87 miles, this means a schedule speed of 35 miles per hour. Calculations show that the locomotive selected can meet this requirement, observing a maximum permissible speed limit of 44 miles per hour with 15 minutes leeway in time, and making two intermediate station stops.

In freight service trailing tonnages of 660 tons eastbound, and 500 tons westbound in 20-car trains in both cases are to be handled with a top speed restriction of approximately 30 miles per hour. Preponderance of tonnage moves from the interior toward the sea coast. Hence, the eastbound trains will be some 30 per cent heavier than those westbound.

These schedules, viewed from the standpoint of American railroad practice,

Table VI. Traffic Under Electric Operation, 1947

	Train-Miles	1,000 GTM
Passenger	273,000	100,500
Multiple-unit trains	181,200	26,650
Mixed	170,060	61,500
Freight	1,100,000	572,000
Totals	1,724,260	760,650

Table VII. Labor Costs Per 1,000 Train-Miles

	Operating	Repairs
Passenger	$12.90	$26.25
Multiple-unit trains	$ 6.30	$25.65
Mixed	$19.75	$19.30
Freight	$23.00	$21.80

Table VIII. Calculated Performance Figures for Sorocabana Suburban Service

Class of Service	Watt-Hours Per Ton-Mile at the Pantograph
Passenger	52.0
Multiple-unit trains	50.1
Mixed	52.0
Freight	50.0

Table IX. Energy Consumption for Various Services

Class of Service	Kilowatt-Hours at A-C Bus in Substations
Passenger	6,000,000
Multiple-unit trains	1,538,000
Mixed	3,675,000
Freight	32,850,000
Total	44,063,000

Table X. Power Charges Attributable to the Several Services Per Annum

Class of Service	Annual Power Charge
Passenger	$ 22,100
Multiple-unit trains	6,750
Mixed	13,540
Freight	121,000
Total	$163,390

Table XI. Expected Costs Under Electric Traction

Type of Service	Passenger	Suburban	Mixed	Freight	Total
Train crews	$ 3,525	$ 1,140	$ 3,370	$ 25,340	$ 33,375
Repair crews	7,175	4,650	3,290	23,960	39,075
Repair and maintenance of locomotives	6,425		4,010	25,840	36,275
Repair and maintenance of multiple-unit cars		1,285			1,285
Electric power	22,100	6,750	13,540	121,000	163,390
Lubrication	330	75	205	1,330	1,940
	$39,555	$13,900	$24,415	$197,470	$275,340
Operating costs as above					$275,340
Maintenance distribution system					20,400
Maintenance sub- and tie-station					12,505
Total					$308,245

Table XII. Dimensions and Ratings of Sorocabana Locomotives

Nomenclature $1-C+C-1-$ 238/286 $\left\{\begin{matrix}6W374\\6GE734A\end{matrix}\right\}$ 3,000 volts	
Length, inside knuckles (feet–inches)	61–0
Width, over-all (feet–inches)	9–7$^7/_8$
Height over collectors, locked down (feet–inches)	13–6$^1/_4$
Wheel base, total	50–0
Wheel base, rigid	13–0
Diameter driving wheels (inches)	44
Diameter guiding wheels (inches)	33
Track gauge	1 meter
Weight, total (pounds)	286,000
Weight, on drivers	238,000
Weight, on guiding wheels	48,000
Weight, per driving axle	39,667
Number of motors and type	6-*GE*734*A*1 or 6-*W* 374
Gear ratio	75/17
Ratings on Full-Field Strength (3,000 Volts 2*S*3*P*)	
Total continuous rating (horsepower)	1,980
Total one-hour rating (horsepower)	2,280
Continuous tractive effort (pounds)	22,450
One-hour tractive effort (pounds)	27,100
Speed, continuous rating (miles per hour)	33.0
Speed, one-hour rating (miles per hour)	31.5
Adhesion, at continuous rating (per cent)	9.44
Adhesion, at one-hour rating (per cent)	11.37
Ratings on Full-Field Strength (3,000 Volts 3*S*2*P*)	
Total continuous rating (horsepower)	1,385
Total one-hour rating (horsepower)	1,800
Continuous tractive effort (pounds)	24,700
One-hour tractive effort (pounds)	35,000
Speed, continuous rating (miles per hour)	21.0
Speed, one-hour rating (miles per hour)	19.3
Adhesion, at continuous rating (per cent)	10.80
Adhesion, at one-hour rating (per cent)	14.70
Maximum permissible speed (miles per hour)	56

might be considered rather slow and leisurely, but it must be remembered that the Sorocabana is a meter-gauge railroad laid in mountainous territory with an abundance of two per cent grades and heavy curvature. If such allowances are made, it is safe to say that the performance is not only respectable but exceptionally good for narrow-gauge railroading.

In specifying motive power, the railroad stipulated that but one size and type of locomotive suitable for pooled operation in both freight- and passenger-train service be furnished. The top speed at which passenger trains may be operated—70 kilometers per hour (44 miles per hour)—and the speed-tractive-effort requirements of freight trains on the heaviest grades are such that all requirements are met very nicely with a so-called three-speed locomotive.

The tractive-effort and maximum adhesion requirements, coupled with rail-loading limitations, fixed the total weight on drivers and number of driving axles at approximately 119 tons on six axles. Maximum individual axle loading is restricted to 18,000 kilograms or 39,667 pounds, since the limiting weight of rail is 75 pounds per yard. The use of six motored axles provides for a flexible arrangement of motor combinations and, with 1,500-volt motors—insulated for 3,000-volt operation—provides for three-speed control.

Freight-train operation on the heaviest grades will be conducted with the locomotive in the 3*S*2*P* combination of traction motors—a connection that gives speeds two thirds of those obtainable in the high-speed 2*S*3*P* connection.

Operation of the locomotives in a freight-passenger pool will be facilitated by the fact that no train-heating equipment is required in passenger-train service.

Locomotive Description

A picture of one of the locomotives is shown in Figure 5.

In keeping with modern trends, the cab has been semistreamlined and, because of the absence of rivetheads, made possible by all-welded construction, will present a smooth clean appearance. No attempt was made to fair or shroud the running gear, as it was felt that accessibility for maintenance and inspection purposes would be of greater importance than a fully streamlined appearance.

Recently constructed electric locomotives have employed modified streamlined cab shapes very similar in appearance to Diesel-electric streamliners, where the operator's positions are set some distance back of a rounded nose, as, for example, in the New Haven Railroad streamlined electric locomotives placed in service in 1937. This form of construction has the advantage that protection is afforded the engineer in the event of level crossing or other collisions. In the case of the Sorocabana Railway units, however, the length of cab was definitely fixed by the railway specifications, and this requirement, in conjunction with space requirements for control and auxiliary apparatus, precluded the use of the extended-nose type of cab.

The principal dimensions and ratings of the locomotives are given in Table XII.

COMPARISON WITH STEAM LOCOMOTIVES

The locomotives are the most powerful narrow-gauge electric locomotives constructed to date. They are exceeded in continuous horsepower rating by but a few narrow-gauge steam locomotives. Some of the English-built 42-inch-gauge Beyer-Garratt locomotives working in South Africa are slightly higher in continuous horsepower rating.

The locomotives are not particularly powerful, as electric locomotives generally go, since many standard-gauge locomotives with continuous ratings in the neighborhood of 5,000 horsepower and short-time ratings of over 9,000 horsepower are currently operating in the United States. They are, however, out-

Figure 4. Typical Brazilian wood-burning locomotive being fueled

standing in that so much power has been built into a relatively small package. Particularly restricting is the space between wheels—a matter of $35^{1}/_{4}$ inches along the axle—into which a 1,500/3,000-volt motor rating 338 horsepower continuously and 390 horsepower on the hourly basis has been placed without employing an excessively large diameter of the driving wheels. Actually the width of the motor itself exclusive of gearing is $29^{3}/_{4}$ inches.

So far as hauling capacity is concerned, the locomotives could have been built with a six-axle (*C*+*C*) running gear, but the existence of a great deal of sharp track curvature and the desirability of keeping flange wear at a minimum indicated the advisability of a guiding axle at each end. Thus an eight-axle articulated running gear with a $1-C+C-1$ wheel arrangement was included.

While it is difficult to compare electric locomotives and steam locomotives because of their dissimilar characteristics, a comparison between the largest narrow-gauge steam locomotive now in operation with the Sorocabana Railway electric locomotive is interesting. It so happens that the nominal horsepower rating of the largest Garratt articulated steam locomotive in operation on the South African 42-inch gauge lines lies between the continuous and one-hour ratings of the Sorocabana Railway unit.

Figure 7 compares the physical dimensions and weights of the two locomotives. In addition, the steam locomotive in normal service hauls a tank-car tender of water—the capacity in the locomotive tank is but 1,600 gallons.

Figure 8 compares the tractive-effort characteristics of the two locomotives.

The horsepower of the electric locomotive at 25 per cent adhesion (60,000 pounds and 30 miles per hour) is 4,800. This rating is, of course, at a considerable overload and cannot be held for any appreciable time without danger of overheating the traction motors.

The complete set of running characteristics for all connections of the traction motors and for three different values of field strength are shown in Figure 9.

The driving and guiding truck frames (Figure 6) are fabricated throughout—no castings of any kind are employed. Each driving truck employs two rolled-plate side frames, each $3^{1}/_{2}$ inches thick, tied together with rolled-plate cross members. In appearance, the trucks are strongly suggestive of European practice. This suggestion is further emphasized by the journal boxes—these are of the Isothermos type in which no waste packing is employed. This type of box has been used to quite some extent in Europe but only to a limited extent in American railroading.

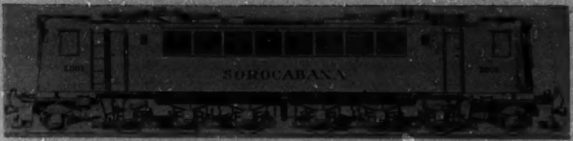


Figure 5. 1−C+C−1 3,000-volt electric passenger–freight locomotive

On multiple-axle locomotives, an effort is made to provide three-point suspension for the spring-borne weight of the locomotive, to allow for track irregularities, and to avoid unnecessary twisting of the truck and cab structures.

The isometric schematic diagram (Figure 10) of the main truck spring and equalizing system for one half of the running gear illustrates the three-point-suspension design employed.

The three-point-support idea is also employed in loading each truck with its share of cab weight. The two center plates in conjunction with four spring loading pads provide three points of support between the cab and each driving truck.

The articulation joint is a pin and ball-and-socket type providing full flexibility for the accommodation of trucks to track irregularities and, at the same time, mutual lateral restraint between trucks to improve tracking qualities of the locomotive as a whole. Tracking ability is further improved by providing lateral restraint in the guiding trucks. The guiding-truck restraint is provided by mounting the truck center plate on a roller-mounted carriage which climbs an inclined plane when the truck is displaced from central alignment with the main frame. The weight carried on the center plate thus acts to return the truck to central alignment.

The cab is divided into three principal compartments—two operators' compartments and a center apparatus compartment (Figure 11). The latter contains the auxiliary machinery—two 3,000/65-volt motor-generator blower sets; two 81 *CFM* exhausters and one 35 *CFM* air compressor—and the control-apparatus subcompartment. Contactors, high-speed circuit breakers, relays, accelerating resistors, and so forth, are all located in a compartment which in manufacture is completely assembled with apparatus installed before being placed in the locomotive.

The 3,000/65-volt motor-generator sets furnish power for the operation of compressor, exhausters, control, lighting; and during regenerative braking operations one of the sets acts as an exciter for traction-motor field excitation.

Ventilating air for traction motors, accelerating resistors, motor-generator sets, and exhauster motors is furnished by two blowers driven directly by the 3,000-volt motors of the two motor-generator sets.

CONTROL SYSTEM

The control system is of the single-unit nonautomatic type with 33 accelerating steps and three running-motor combinations. Each combination of motors

6 series
3 series 2 parallel
2 series 3 parallel

provides one step of full-field and two steps of reduced-field strength, thus providing a total of nine running steps as shown in the speed-tractive-effort characteristics.

Regenerative braking is provided for all three motor combinations. While a multitude of short two per cent grades on the line between São Paulo and Santo Antonio makes regenerative braking desirable, the braking feature will be particu-

Figure 6. All-welded fabricated main-truck-frame assembly for locomotive, classification 1−C+C−1, 130 metric tons, 3,000 volts direct current meter-gauge

cularly advantageous in the operation of the 25-mile grade—which averages very nearly two per cent—on the Mayrink–Santos line which will be electrified at some future date.

Despite the fact that ten locomotives are to be furnished by the General Electric Company and ten by the Westinghouse Company, both makes will be built to the same specifications. The mechanical portions will be identical—all 20 will be built by the General Electric Company.

The traction motors as built by the two companies will be exact duplicates as far as their electrical characteristics are concerned and will be interchangeable physically.

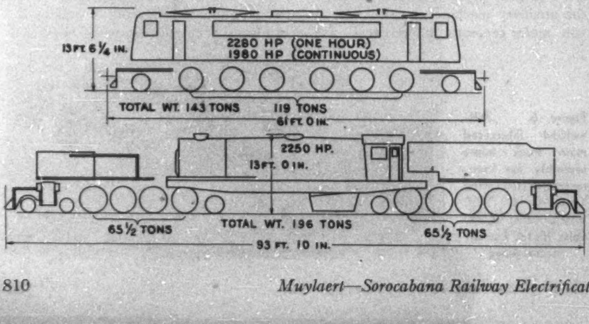
Much of the auxiliary and control equipment will be interchangeable between the locomotives manufactured by the two companies. Pantographs are identical. Motor–generator sets are interchangeable physically and have the same electrical characteristics. Traction-motor blowers are identical. High-speed circuit breakers will be identical, and all 20 will be manufactured by one company.

Multiple-Unit Cars

To handle existing suburban traffic and in anticipation of future commuter traffic expected with the extension of residential sections west of the city of São Paulo, four three-car multiple-unit trains have been specified as a part of the electrification.

These are to be basic units of three cars—a four-motor motorcar with a nonmotored operating trailer at each end of the motorcar. While the basic train unit will consist of three cars as described, an operating position at one end of the motorcar is to be provided, thus permitting of single-end operation of the motorcar alone, or double-end operation of a motor–trailer combination, should such operation be deemed expedient.

Figure 7. Dimensional comparison of Sorocabana 3,000-volt meter-gauge electric locomotive (top) with South African Railway Garratt articulated 42-inch-gauge steam locomotive (bottom)



These multiple-unit cars will be the first 3,000-volt meter-gauge multiple-unit cars to be placed in service. As a matter of fact, there is in operation but one 3,000-volt meter-gauge electrification—the Transandine line over the Andes between Chile and the Argentine. This line utilizes no multiple-unit car equipment—only rack-and-adhesion locomotives.

Schedules to be run by the multiple-unit trains are based on an accelerating rate of 1.5 kilometers per hour per second, which is equivalent to approximately 1.0 miles per hour per second. Braking rates of 1.5 mile per hour per second will be employed. Schedules are to be maintained with a top permissible speed of approximately 40 miles per hour.

The traction motors will be self-ventilated by fans mounted on their own armatures, taking clean air from a duct system the intakes of which are located in the car roofs. This ventilating system is quite similar to that used on the 3,000-volt Lackawanna Railroad multiple-unit cars. It has been found advisable on 3,000-volt self-ventilated motors in multiple-unit car service to take the air at a level high above the track, and to preclude the induction of brake-shoe dust and other foreign matter that would cause insulation breakdown and flashovers.

The 3,000-volt motor on the motor-generator set also takes its air from the duct system.

The control is of the *PCM* type with all 3,000-volt control located in boxes beneath the car flooring.

Control and lighting energy at 65 volts for all three cars in the basic train is to be furnished by the 3,000/65-volt ten-kilowatt motor-generator set and a 32-cell 60 *A-H* lead-acid type of storage battery.

Two running speeds 4*S* and 2*S*2*P* with no field shunting are provided. The acceleration will be nonautomatic—that is, the driver will have complete control during acceleration.

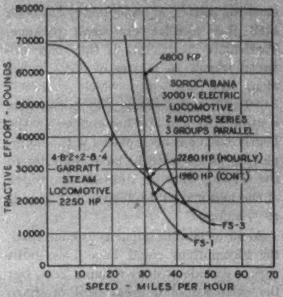


Figure 8. Comparison of tractive-effort characteristics of Sorocabana 3,000-volt meter-gauge electric locomotive and 42-inch-gauge steam locomotive

Air brakes are to be furnished, and a 65-volt 35 *CFM* compressor operating off the motor-generator set will furnish air.

Traction motors are rated at 185 horsepower (one hour) and 148 horsepower (continuous).

The electrical equipment for the multiple-unit cars will be built by the General Electric Company, while the cars will be built and equipped at the Worcester, Mass., plant of the Pullman-Standard Car Manufacturing Company.

Overhead Distribution System

Before the overhead system is described, it should be noted first that two major factors greatly influenced design. These are weather and cost of labor. These two factors should be kept in mind for they will serve to explain some of the deviations made from North American practice.

The weather in Brazil is mild. Freezing temperatures do not occur in the territory involved in the electrification, and the maximum temperature change amounts to only 80 degrees Fahrenheit. The wind pressure on structures and cables barely exceeds eight pounds per square foot.

Common labor in Brazil receives the equivalent of three cents to six cents per hour in United States currency and skilled labor about twice this amount. These two factors, weather and labor cost, have greatly changed the ratio of material cost to labor cost. Structures have been made lighter and of different materials from those which are normally used in the United States, and considerable field labor will be substituted for higher-priced shop labor.

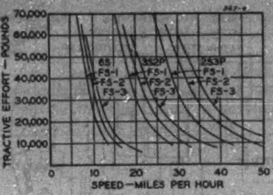


Figure 9. Speed tractive-effort characteristics of Sorocabana 3,000-volt meter-gauge electric locomotive

The distribution system extends approximately 87 miles from the city of São Paulo. The railroad is double-tracked throughout this distance with five main yards at São Paulo, Barra Funda, Mayrink, Sorocabana and Santo Antonio. Two types of catenary, a main-line type and a yard type, were designed for the system. With the exception of a ground wire and pilot wires for electrical machine control, no wires and cables, other than catenary, are supported on the structures. Steel structures will be erected over the first ten kilometers of electrification extending out from São Paulo, and also over most of the yard tracks in other locations. Concrete poles supporting cross spans will be used for the main-line two-track suspension. The return system uses the electric arc-weld bond on each track joint.

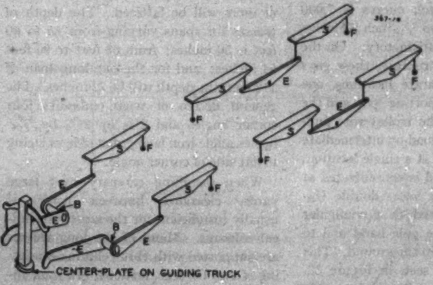


Figure 10. Isometric of three-point suspension

Clearance

Although the railway operates over meter-gauge track, the clearance diagram does not deviate materially from that of a standard-gauge railroad. The distance between track centers is 13 feet, the distance from the center of track to the face of structures is 8 feet, and the head clearance is 15 feet, 9 inches. The trolley-wire height above rail is 18 feet (normal), and the maximum deviation of trolley-wire alignment from the center line of track is 9 inches, either side of center.

Structures

The design and spacing of structures were determined by a study of track alignment, loadings, and costs. In some locations, however, structure designs are altered from standard because of limited clearances caused by walls and buildings. A structure spacing of 60 meters (197 feet) on tangent limited the dead load to 500 pounds at each suspension point. This dead load includes approximately 750,000 circular mils of copper in the messenger and trolley wires over each track, as well as catenary hardware and insulation. There is no ice load. This 500-pound dead load is not a heavy loading when compared to some of the catenary spans on the Pennsylvania, Lackawanna, New Haven, or Reading electrified systems. Using this dead load of 500 pounds per suspension point and the wind load, a study of several types of structures was made. These types included:

1. The two-track portal or bridge type fabricated with steel.
2. The steel-pole cross span.
3. The steel-pole two-track bracket.
4. The concrete-pole cross span.

In the United States there would be very little doubt as to the type of structure erected. If the engineer favored the bridge type of structure, he could no doubt prove the bridge structure to be the most economical. If on the other hand, he favored the cross-span type of structure, he could easily prove the cross span to be the most economical. In the United States there is very little difference in the costs of the two types of structures for comparatively light loadings. However, in Brazil the low cost of labor makes a material difference. Steel must be fabricated 6,000 miles from the field. The dimensions of the structure must originate in Brazil. The design is made in the United States of America and then sent to Brazil to be checked. After the structures are fabricated in the United States of America, they are shipped by rail to the seaboard, then reshipped by boat to Santos, and again shipped by rail to the field. Compare the cost of this procedure for steel structures with the cost of the manufacture of reinforced concrete poles where all ingredients are easily available and skilled concrete workers comparatively plentiful. For Brazil, with a favorable climate, has developed the manufacture and use of concrete beyond that of many countries, just as California and Florida have developed the use of stucco in the United States of America.

On tangent track, the side load on poles supporting cross spans is only 1,800 pounds. The guy load is approximately twice this figure (3,600 pounds). On the most extreme curves, where pullover poles are placed between supporting structures, the side load on the poles, due to double track catenary, does not exceed 3,500 pounds. The guy load would be approximately 7,000 pounds. With these light loads, concrete poles, properly guyed, are more economical than fabricated steel structures, or bridge structures built with simple shapes.

Although all structures must be guyed to prevent excessive bending moment, the guy cost is comparatively low. The total cost of a guyed concrete structure, including the digging of holes, concrete guy slugs, span wire, and hardware, is considerably less than the cost of an unguyed steel structure of the portal type, or the guyed cross-span structure using two steel *CB* columns. The Paulista Railway has constructed many miles of catenary similar to the Sorocabana design with guyed concrete poles.

Concrete Poles

The standard concrete pole for tangent and curve two-track construction is rectangular in shape. The pole tapers from the top to the ground line, and a reverse taper is carried from the ground line to the butt. In other words, the pole has a swelled section at the ground line. Three heights of concrete poles are being manufactured with heights of 33, 36, and 40 feet. At the top all poles are 8 by 10 inches, at the ground line 14 by 20 inches, and at the butt 14 by 14 inches. These poles are placed with the long dimension parallel to the track. The depth of the

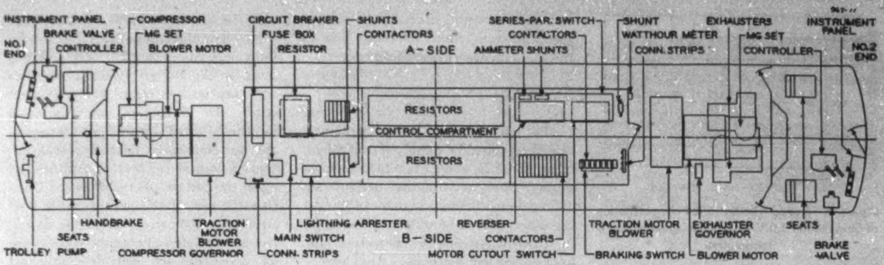


Figure 11. Cab layout of Sorocabana 3,000-volt meter-gauge electric locomotive

average hole is six feet in undisturbed earth. All poles will be manufactured in a pole yard at a convenient shipping point along the railroad.

The design of the supporting pole was made by Cobrazil, the construction organization at São Paulo. The Brazilian Association of Portland Cement was also consulted with respect to the design. These poles will be cast in wood forms. As is usually the case in the manufacture of concrete poles in Brazil, these poles will have a hollow center to reduce weight.

The poles will be reinforced with $^{3}/_{8}$-inch rods at the corners and with $^{5}/_{16}$-inch steel wrapping. Specifications call for a tensile strength of 28,000 pounds per square inch for reinforcing steel and a compressive strength of 1,400 pounds per square inch for the concrete. The concrete is not expected to take any tension. Yield at the end of 28 days is 3,500 pounds per square inch. All concrete will be vibrated during construction, and the hollow core will be filled with water to aid in curing. The ratio of the moduli of steel to concrete for design purposes is 15. A concrete mix of $4:2:1^{1}/_{4}$ is being used.

All span and guy wires and the pilot-wire crossarms will attach to pole collars, and in view of this no provision is made in the pole itself for these attachments. However, a hole will be cut through the wall of the pole below the ground line for ground connection to the vertical ground tap carried in the hollow center of the pole.

Guys

The comparatively light side loads on the concrete two-track structures aided in solving the guy problem. The reinforced concrete pole itself is not considered a good ground, and therefore, principally for protection against electrical shock, all concrete pole guys will be insulated. Heavy guy cables require heavy and expensive insulators and insulator fittings. Insulators and fittings for light guys are comparatively cheap. On the standard tangent two-track cross span, each concrete pole is guyed with a $^{7}/_{16}$-inch Siemens-Martin seven-wire galvanized steel cable. Since the guy load does not exceed 3,600 pounds, this cable with a 9,350-pound ultimate is quite satisfactory. Corrosion is not a factor in the selection of cable sizes. The maximum guy load on two-track curves is 7,000 pounds; therefore two $^{7}/_{16}$-inch Siemens-Martin cables are satisfactory. On the standard two-track structures these guys are attached at separate locations, one guy opposite the supporting span and the other guy opposite the trolley-wire pullover span. The side load on intermediate pullover poles occurs at a single location; therefore the guy load is concentrated at one point. On these poles double $^{7}/_{16}$-inch guys are attached to a triangular equalizer plate at the pole band and to the same guy anchor in the ground. This arrangement can be seen in Figure 13. These light guys have been responsible for the elimination of many turnbuckles necessary with heavier cables.

Steel Structures

In general, the field forces select the type of supporting structures. Having satisfactorily located the structures, and having determined building and guy clearances, also footing restrictions, the field men decide whether a structure will be steel or concrete, bridge, cross span, or bracket. Wiring drawings are prepared in São Paulo showing the location of all structures and overhead, and in addition separate sketches are made of all structures. This information is used in the United States as the basis of structure design.

The two-track tangent steel structure consists of two ten-inch 33-pound *CB* columns and an eight-inch 31-pound *CB* beam. Including knee braces, the total weight of this structure is approximately 3,500 pounds.

The two-track curve structure for the heaviest loads consists of two ten-inch 49-pound columns and a ten-inch 41-pound beam. With knee braces this structure averages approximately 4,500 pounds.

The three-track structure will be built with *CB* columns and a *CB* beam, but the trusses for spans crossing four tracks or more will be latticed. The depth of trusses for spans varying from 45 to 60 feet is 56 inches; from 60 feet to 90 feet 54 inches; and for the one long span of 112 feet, the depth will be 72 inches. The general design of truss embodies four corner angles and $2^{1}/_{2}$ by $2^{1}/_{2}$ by $^{5}/_{16}$-inches angle iron lacing, flat side of lacing to flat side of corner angle.

When installing catenary over large yards, clearance between tracks is usually insufficient for the most economical columns. Many of the long trusses are supported with thin columns consisting of two *CB* legs, latticed. In some instances these columns are similar to the well-known *A* frame used for supporting signal towers. Riveted assemblies are almost entirely eliminated because of the additional space they would require in shipping. All steel will be bolted with three-fourth-inch bolts in the field. Angle iron for latticing will be connected to the corner angles with two bolts rather than one bolt.

A maximum fiber stress of 13,300 pounds per square inch was used in designing this steel. The point of zero moment for the column was located one third of the length of the unsupported column above ground. In addition to the dead loads, side loads, and wind loads, each structure was designed to withstand a single 2,000-pound longitudinal load at any catenary support. This longitudinal load was added to handle messenger breakage, the slipping point of the messenger clamp occurring somewhat below 2,000 pounds. In calculating steel for the 2,000-pound longitudinal load, it was felt that, since messenger breaks are extremely rare, the maximum fiber stress for these loads could be increased to approximately the yield figure.

Footings

The average depth of hole for the two-track concrete poles, tangent and curve, is six feet. Since all poles are guyed laterally, and since the longitudinal load caused by messenger break will not exceed 1,000 pounds, no additional concrete for side bearing will be cast around the base of these poles. However, precast bearing blocks will be placed in the bottom of all holes. For tangent poles, the bearing block will be 24 inches in diameter, and for curve poles it will be 30 inches in diameter.

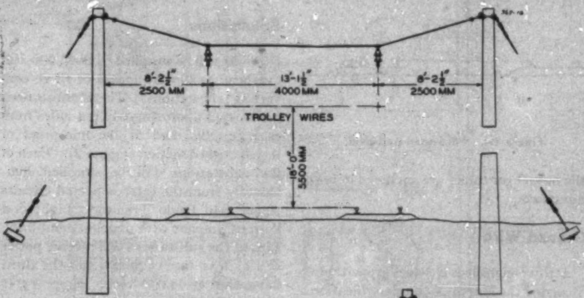


Figure 12 **Tangent structure**

The ten-inch *CB* columns for both tangent and curve portal structures, will be set on concrete pads similar to those used for concrete poles and, in addition, will be embedded in concrete to a point one foot above ground level. Since these structures are not guyed, the foundations must be constructed for side bearing. As a consequence, the tangent-structure foundations will be at least 18 inches in diameter, and the curve-structure foundations will be approximately 30 inches in diameter. The depth of foundations for two-track steel structures, including the bottom pad, is 7 feet below ground line.

Special footings are necessary for the larger structures, the cross sections and depths corresponding to the loads. All columns will be embedded in the concrete foundations, no anchor bolt structures being used.

Cross Spans

All cross spans or supporting spans are attached to pole clamps on concrete poles.

On the tangent structure, as illustrated in Figure 12, the pole clamp is also the attachment for the ground guy. The cross span is $^7/_{16}$-inch Siemens-Martin seven-wire steel cable, and, where it attaches to pole clamps or guy rods, it is protected by either a thimble or large radius curved section. Two-bolt clamps with a wave seat are used for light loads and three-bolt clamps with a similar seat for heavy loads. The two-bolt clamp corresponds to two Crosby clips with respect to slippage, and the three-bolt clamp has almost the strength of four Crosby clips. All guys are insulated near the top of the pole. Insulator strings are attached to the span wire with inverted suspension clamps.

Attachments to Steel Structures

On steel structures built with a single *CB* beam spanning one, two, or three tracks, the insulator string is attached with a beam clamp. On latticed structures an angle iron fitting is attached to the lower chord of the truss, and the insulator string is attached to the angle iron. Span attachments to the *CB* columns are made with structural attachments that provide vertical adjustment. Guy attachments to *CB* columns are made with a half section of *CB* column bolted to the structure column.

Catenary Spans

The main-line catenary span shown in Figure 15 is 60 meters (197 feet) in length The main messenger is a 300,000-circular-mil hard-drawn-copper cable and supports two 4/0 hard-drawn-copper contact wires. Each contact wire is supported by alternate hangers. This system, with the exception of dimensions and capacity, is similar to that used on the Chicago, Milwaukee, St. Paul, and Pacific Railroad as well as on the Paulista Railway in Brazil.

The normal messenger tension is 4,000 pounds and the normal trolley-wire tension is 2,000 pounds. With the low temperature range in the vicinity of the electrification, the messenger tensions will vary from 3,500 pounds at 100 degrees Fahrenheit to 4,800 pounds at 30 degrees Fahrenheit. The normal sag is 33 inches. The secondary catenary span, Figure 16, used in yards includes a $^7/_{16}$-inch Siemens-Martin messenger and a single 4/0 trolley wire. The normal sag of this span is also 33 inches.

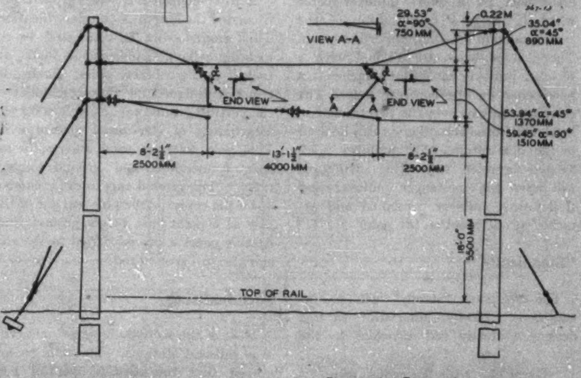


Figure 13. **Two-track curve structure**

A flexible-loop catenary hanger will support the trolley wire. On main-line spans this hanger will ride on a cast bronze shim or protecting sleeve that surrounds the copper messenger cable.

The sleeve is necessary to protect the messenger cable from mechanical wear and arcing.

Curve Construction

Chord construction was selected for this meter-gauge electrification. On severe curves a pullover pole is placed between supporting structures in order to provide a means for aligning the catenary systems with the center line of track. The supporting structure designed for curve construction, as shown in Figure 14, includes the same cross-span supporting cable as used for tangent spans. The side load produced by the messenger cables on curves is carried to the outside concrete pole by a short pullover cable which is really a continuation of the center portion of the supporting span. The contact wires are aligned with a separate pullover cable that attaches to a pole collar on the concrete pole. The catenary systems over the two tracks are insulated from each other as can be seen in the illustration. The pullover poles located between supporting structures will have the messenger pullover and trolley-wire pullover combined and attached at one point on the pole.

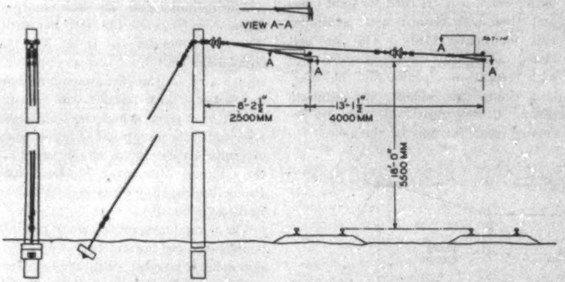


Figure 14. Mid-span pull-over

"Steadies"

The two-track "steady" will be installed at 1,200-foot intervals. In this design, a steady rod attached to the trolley wire over the left-hand track prevents sway of the trolley wires, because of its anchorage to a span running between the two messenger cables. The steady arm is attached to only one of the two-contact wires in the same span.

Anchors

Anchorages will be installed at 4,500-foot intervals. At each anchor point the messenger cables are anchored in both directions.

Ground Wire

A three-eighths-inch steel ground wire is carried the length of the electrification. This ground wire is connected to all metallic collars, spans, and so forth, attached to the concrete poles. Each pole collar is designed for the attachment of the vertical ground tap, as well as for the attachment of the aerial ground wire. This pole collar is used universally for span connections and ground connections. The ground tap to rail is carried down the center of the pole and out of the pole at a point just below ground line. At this point a tap is carried to the rail and also to a ground rod.

Sectionalizing

Most of the sectionalizing points occur near railroad stations. They are so arranged that the station operator can manipulate the switches in order to isolate a section of line. A new-type double-trolley-wire-section insulator has been used throughout the main line and, because of its extremely light weight, will be satisfactory for the maximum speeds. This section insulator is constructed with phenolic tubes and weighs only 29 pounds.

Bonds

All track joints are bonded with a 4/0 seven-inch U-shaped arc-weld bond. Since this system has 3,000 volts on the contact line, portable gasoline-driven welding equipment will furnish energy to the welders. In addition to the regular track-joint welding, cross bonds will be installed at 1,000-foot intervals.

Distribution system material is being supplied by the Ohio Brass Company.

Substations

Power will be supplied to the 3,000-volt overhead trolley by means of three converting substations. These substations are located approximately ten miles from each terminal and at the mid-point of the electrified section (Figure 2). Each of the substations will be supplied individually from the interconnected circuits of the São Paulo Tramway, Light and Power Company over double-circuit lines. Two of the substations will receive power at a high voltage of 88,000 and the third substation at 44,000 volts. However, it is anticipated that at the latter station the high voltage will also be 88,000 in the near future, and consequently it is designed for future operation at this voltage. Two of the substation locations will be served by existing lines, and the third location requires the construction of only 15 miles of transmission line. This will be constructed by the power company.

Each of the three substations (Figures 17 and 18) will be equipped with three 2,000-kw rectifier units, one of which serves as a spare. Naturally the substation located at the mid-point carries more load than either of the other two. However, it is anticipated that the rapid

Figure 15. Main-line contact span

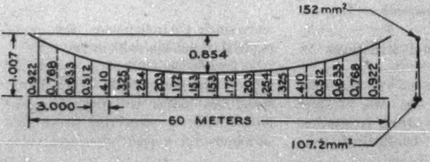
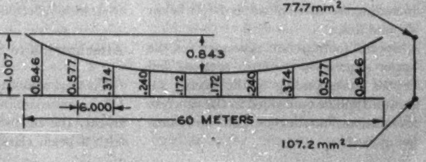


Figure 16. Side-track contact span



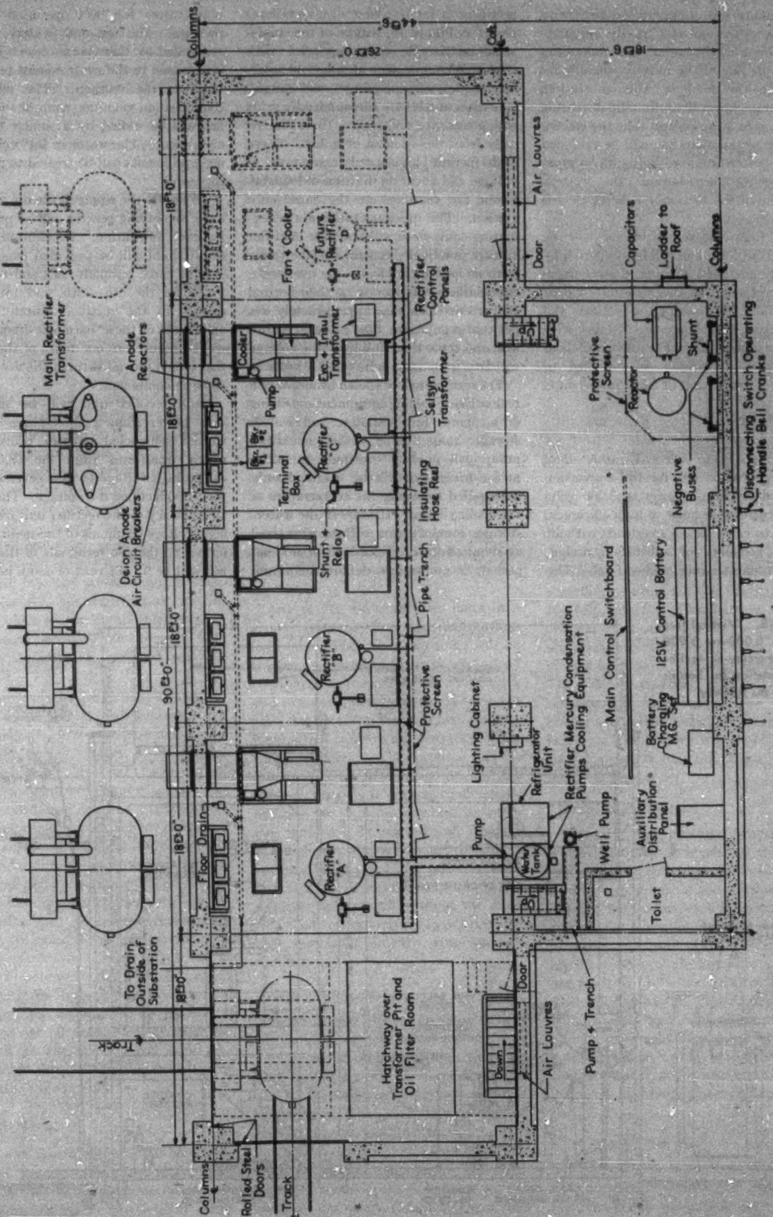

Columns
44'-6"
26'-0"
Col.
18'-6"
Main Rectifier Transformer
Anode Reactors
Deion Anode Air Circuit Breakers
18'-0"
18'-0"
90'-0"
18'-0"
18'-0"
18'-0"
Floor Drain
To Drain Outside of Substation
Track
Rolled Steel Doors
Columns
Track
Hatchway over Transformer Pit and Oil Filter Room
Door
Down
Air Louvres
Pump + Trench
Fan + Cooler
Future Rectifier "D"
Cooler
Pump
Exc. + Insul. Transformer
Rectifier Control Panels
Selsyn Transformer
Bkr. #1
Bkr. #2
Rectifier "C"
Terminal Box
Insulating Hose Reel
Shunt + Relay
Pipe Trench
Rectifier "B"
Protective Screen
Rectifier "A"
Lighting Cabinet
Refrigerator Unit
Rectifier Mercury Condensation Pumps Cooling Equipment
Pump
Water Tank
Well Pump
Main Control Switchboard
Auxiliary Distribution Panel
Battery Charging M.G. Set
125V. Control Battery
Toilet
Negative Buses
Protective Screen
Reactor
Capacitors
Ladder to Roof
Columns
Shunt
Disconnecting Switch Operating Handle Bell Cranks
Air Louvres
Door

Figure 17. Plan view of 6,000-kw 3,000-volt mercury-arc-rectifier d-c substations for Sorocabana Railway

and improved suburban service with electric operation will greatly stimulate the growth of the suburban communities along the line of the railway, thereby increasing the load on this substation. Then, too, with the continually increasing traffic, it is quite certain that the electrification of the main line will be extended from Santo Antonio to Botucatú, approximately 90 miles beyond the western terminal of the section now being electrified.

The high-voltage switchgear will be located outdoors and will be supported by a firmly anchored galvanized steel structure. Adjacent to this structure will be the building for housing the rectifiers and their associated apparatus together with the d-c switchgear. For ultimate economy, each substation is designed to receive a fourth unit at some future date.

RECTIFIERS

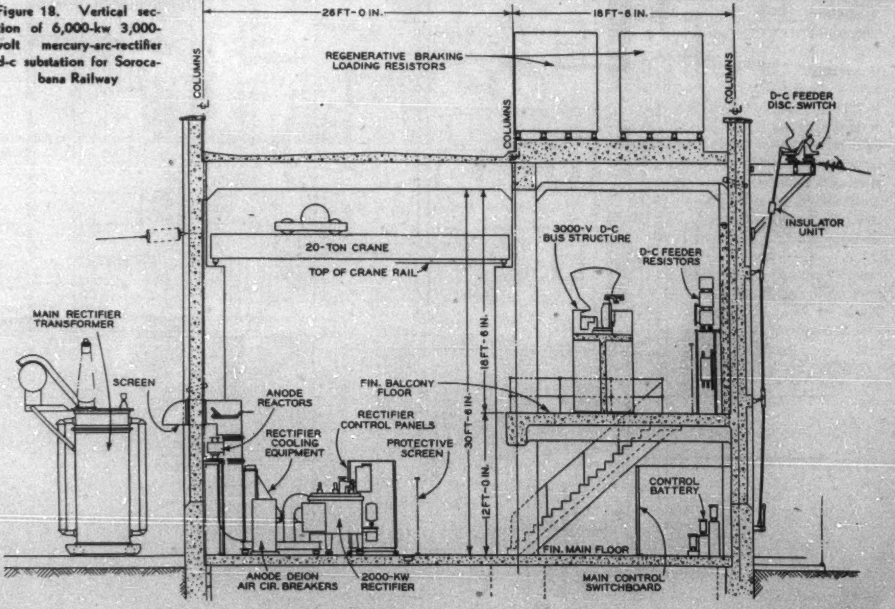
Rectifiers were selected rather than motor-generator sets for the conversion of the 60-cycle a-c power to 3,000 volts direct current, because of high efficiency, ability to carry heavy overloads without injury, economy of substation design, reduced maintenance, and so forth. The rectifiers (a factory view of a rectifier is shown in Figure 19) will be of the single-tank multianode type, equipped with the usual complement of main and auxiliary anodes, vacuum pumps, and gauges. The main anodes are surrounded by grids which separate and isolate them electrically from the vacuum chamber. These grids permit high-speed regulation of voltage and accurate division of load between the rectifiers in the same substation. The quick operation of the regulating equipment will hold the rectifier voltage practically constant in the range from no load to full load. On overloads, the rectifiers will operate on their normal characteristic having approximately six per cent regulation; that is, between full load and three times full load, the voltage will drop from 3,000 volts to 2,640 volts.

The rectifier will be cooled by means of a recirculating cooling equipment consisting of a pump, fan, radiator, and water-storage tank. The water-circulating pump will operate continuously. The motor-driven fan will be operated only as required to reduce the temperature of the cooling water. Heaters in the water-storage compartment will be energized as required during light-load or no-load periods to maintain a definite minimum temperature for best operation of the rectifier. The fan and heaters will be controlled by thermostats which operate in response to the control-point temperatures of the rectifier. The mercury-condensation vacuum pump of the rectifier will be cooled by a similar type of equipment. The water in the recirculating equipments will be treated to prevent corrosion.

To reduce the ripple in the d-c voltage, a d-c smoothing reactor will be provided for each substation. In addition, a resonant shunt will be provided which consists of three resonant coils and reactors tuned for the 6th, 12th, and 18th harmonics in the rectified current. The amplitude of these harmonic frequencies will be reduced to one tenth of the value that would prevail without the use of this filtering equipment.

Each rectifier unit will be supplied with power from a three-phase oil-immersed self-cooled outdoor transformer which transforms from the 88,000 or 44,000 high voltage to that necessary for the 3,000 rectified d-c voltage. The overall efficiency of the rectifier unit from the high-voltage terminals of the main transformer to the d-c terminals of the rectifier will be 97 per cent or more between

Figure 18. Vertical section of 6,000-kw 3,000-volt mercury-arc-rectifier d-c substation for Sorocabana Railway


26FT-0 IN.
18FT-8 IN.
REGENERATIVE BRAKING LOADING RESISTORS
℄ COLUMNS
℄ COLUMNS
℄ COLUMNS
D-C FEEDER DISC. SWITCH
INSULATOR UNIT
3000-V D-C BUS STRUCTURE
D-C FEEDER RESISTORS
20-TON CRANE
TOP OF CRANE RAIL
MAIN RECTIFIER TRANSFORMER
SCREEN
ANODE REACTORS
RECTIFIER COOLING EQUIPMENT
RECTIFIER CONTROL PANELS
PROTECTIVE SCREEN
FIN. BALCONY FLOOR
18FT-6 IN.
30FT-6 IN.
12FT-0 IN.
CONTROL BATTERY
FIN. MAIN FLOOR
ANODE DEION AIR CIR. BREAKERS
2000-KW RECTIFIER
MAIN CONTROL SWITCHBOARD

one-half load and 150 per cent load. The units will be capable of meeting the following overload ratings: 150 per cent load for two hours, 240 per cent load for three minutes, and 300 per cent load for one minute.

Regenerative-Power-Absorption Resistors

While the application of regenerative braking to the locomotives is readily justifiable, because of the serrated profile with its numerous steep grades, several factors contribute toward making it undesirable to arrange the substations to return this regenerated power back into the supply system. Traffic on the railway is moved by a reasonably large number of relatively small trains. This fact, together with the nature of the profile, will usually result in the power regenerated by a train on a descending grade being consumed entirely by a train ascending an adjacent grade. Detailed calculations based on a typical daily train sheet show that the surplus of regenerated power averages only about two per cent. Not only will the surplus of regenerated power be small, but also the railway would receive no credit if it were put back into the power-supply system. Consequently, the expenditure for inverter units or the complicated control apparatus necessary to change from rectifier to inverter operation was not justified.

In view of the preceding, a large resistor is provided at each substation for absorbing the excess regenerated power from the trolley system. This resistor is capable of absorbing 1,800 kw of energy and will be mounted on the roof of the substation building. It is divided into nine equal steps which are connected to or disconnected from the 3,000-volt system by automatic-control functioning upon indication of a high-voltage relay and a load-indicating relay connected to the substation bus. An excess of regenerated power will cause the substation bus voltage to rise above normal. This rise in voltage actuates the control which automatically cuts in the load resistor step by step as required to produce a relatively stable voltage condition at the bus. When the traction load again predominates, the resistor is automatically cut out of the circuit.

Switchgear

Each of the substations will be fed by one of two 60-cycle three-phase lines which are tapped off the power company's distribution system. At the point where these transmission feeders connect to the power system, the power company will install high-voltage oil circuit breakers with metering and protective equipment. Since this point of connection is practically adjacent to the substation at two of the locations, motor-operated disconnecting switches with deion fuses will be used in the transmission feeder lines. At the third location, the transmission feeders between the substation and the power system are approximately 15 miles long, and here high-voltage oil circuit breakers with overload protective relays will be provided. At all stations, motor-operated disconnecting switches and fuses will be used between the high-voltage bus and the primary side of the power transformers. For the protection of the secondary system against short circuits, circuit breakers will be provided in the rectifier anode leads. All d-c switching will be accomplished by high-speed breakers. The use of high-speed cathode breakers provides rapid disconnection of the rectifier from the substation bus in the event of an arc-back. The d-c feeder breakers are essentially a duplicate of the rectifier cathode breakers, and the control circuit is of the standard short-circuit-detecting automatic-reclosing type.

Any of the three rectifier units in a given station may be operated as the base load unit. The other two units will be started and stopped in sequence as load conditions demand. A selector switch and over- and underload relays form the essential parts of the automatic equipment which controls the operation of the three rectifiers. The position of the sequence switch determines which of the three rectifiers is to operate continuously and also the sequence in which the other two units will be started and stopped. The scheme of control and layout of wiring is so arranged that, with the addition of a fourth rectifier unit, a four-unit sequence will prevail without the addition of any other control apparatus except that associated directly with the new rectifier.

Control Energy

Control energy for the auxiliary devices of the mercury-arc rectifier, such as equipment for cooling, vacuum pumping, and excitation, will be supplied from a 100-kw three-phase 60-cycle operating transformer. The secondary voltage of this transformer is 220 volts.

Control energy for the automatic devices for closing and opening the high-voltage motor-operated disconnecting switches, the anode circuit breakers, and the contactors for the regenerative-power-absorbing resistor will be taken from a 125-volt battery. This battery will be charged by means of a motor–generator set which receives its energy from the previously mentioned operating transformer. The battery-charging motor–generator set normally will supply the control energy and deliver a trickle charge to the battery. The battery will supply short-time demands beyond the capacity of the motor–generator set.

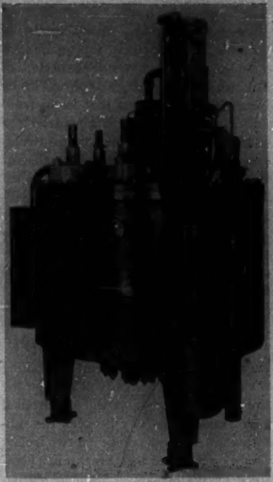


Figure 19. Multianode mercury-arc rectifie for railway service, rated 2,000 kw, 3,000 volts, type *RHW*

Metering Equipment

Metering and potential transformers will be installed as a part of the high-voltage equipment in each station. Recording demand meter and wattmeter will indicate the load on each individual substation. It is contemplated that remote metering equipment providing for totalizing the demand of all three substations at one point will be added at a future date. This will require the addition of contact-making watt-hour meters to transmit impulses over a direct-wire line to the dispatcher's office. In this office, an impulse totalizer will record the impulses and indicate the maximum power demand in the desired time interval.

Operator's Quarters

While these stations are provided with full automatic control, a watchman–inspector will be assigned to each station.

The inspector's duties, in addition to policing the grounds, consist merely of cleaning and inspecting the equipment. At any time that anything occurs to lock out the station, an alarm rings in the inspector's quarters so that he is available immediately to investigate the trouble and report to central headquarters. Since this contract calls for the electrification completely installed and ready to run, all installation materials, installation drawings, building design, and construction, and so forth are to be included. These include the design and construction of living quarters for the operator at each substation

TIE STATIONS

In order to reduce the conductivity required in the trolley circuit by taking advantage of the diversity of load on the two tracks, tie stations will be installed approximately midway between the substations. At these points the two trolley circuits will be tied together through high-speed circuit breakers. Each tie station will have a 3,000-volt positive bus, and four high-speed breakers will connect the trolley circuits to this bus. The high-speed breakers in the tie station are tripped by short-circuit currents flowing from the bus into the trolley system. A flow of power in the reverse direction will not cause the breaker to trip. In this manner, only the breakers feeding the faulty circuit will open and isolate the fault. A pilot-wire system is used connecting the control of the breaker in the tie station with that in the rectifier substation. The control scheme is so arranged that opening of a breaker at either end of the trolley will cause the breaker at the opposite end to open, regardless of whether or not it may be tripped by the fault current. This insures removal of energy from the faulty circuit. A few seconds after the breakers are opened, the automatic equipment in the rectifier substation will function to check the resistance of the load circuit, and, if the fault has been cleared, this breaker will reclose automatically. As soon as the rectifier-substation-feeder breaker closes, supplying energy to the trolley system, a voltage relay in the tie station will operate to deliver the closing impulse to the breaker in that station. The pilot-wire control scheme is so arranged that, if the circuits become open, both the substation and tie-station breakers will be tripped and will remain open until attendants at both ends of the trolley section transfer the control of the feeder breakers over to local operation independent of the pilot-wire circuits.

DIVISION OF APPARATUS

In order to meet the stipulation that apparatus in all substations must be duplicate, the division of apparatus to be supplied by the two manufacturers was made on the basis of partial equipment for each substation. The rectifiers with their associated apparatus and the main power transformers will be supplied by the General Electric Company and the a-c switchgear and d-c switchgear for both the substations and tie stations will be supplied by the Westinghouse company.

Inasmuch as this is an entirely new installation, there is no necessity for following existing practices or standards. Consequently, it was possible to provide a fully automatic scheme of control based on standard practices and proved circuits with minimum apparatus and maximum simplicity of circuits.

The contract for the manufacture of apparatus and equipment and the complete installation of the electrification was placed with the Electrical Export Corporation—a combination consisting of the International General Electric Company; the Westinghouse International Company and a Brazilian constructing company—Cobrazil (Companhia de Mineracao e Metalurgia Brasil). Substation apparatus, locomotives, and electrical equipment for the multiple-unit cars are being furnished by the General Electric and the Westinghouse Electric and Manufacturing Companies.

Distribution system material is being supplied by the Ohio Brass Company and by the American Bridge Company.

The Pullman-Standard Car Manufacturing Company is building the suburban multiple-unit rolling stock.

The Effect of Corona on Coupling Factors Between Ground Wires and Phase Conductors

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ASSOCIATE AIEE

Synopsis: Measurements of coupling factors between ground wires and phase conductors, as a function of ground-wire voltage, have been made for a sufficient number of cases to determine them for any of the standard transmission-line configurations. It was found that the effect of corona can be expressed in terms of an effective corona ground-wire radius to be used in the conventional coupling-factor equations. Curves are also presented for the quick determination of coupling factors for all standard line configurations. A photographic study of the visual characteristics of surge corona was made for a comparison with the effective radius and with 60-cycle corona.

Actual measurements were made for potentials up to about 2,000 kv, but the data were extrapolated to 6,000 kv with what is considered to be reasonably good accuracy. Coupling factors for positive polarity were found to be considerably higher than those for negative polarity and both higher than values previously calculated from purely theoretical considerations. For a potential of 1,000 kv the actual coupling is 1.2 to 1.5 and 1.4 to 1.8 times as great, for negative and positive polarity respectively, as the values obtained by assuming no corona. At 2,000 kv the corresponding figures are 1.4 to 2 and 1.6 to 2.7 respectively, and at 4,000 kv 1.7 to 2.9 and 2 to 4.

Ground-wire size was found to be unimportant above about 200 kv and, therefore, need not be considered. Coupling factors measured below the critical corona voltage checked calculations that assumed the actual earth's surface to be the effective ground plane. These measurements were made over soil whose resistivity was as high as 600 meter ohms. It is thought that the actual surface should apply for such electrostatic phenomena except for very high values of earth resistivity.

Measurements were also made of the critical flashover of rod gaps connected between the ground wire and floating phase conductor. The critical breakdown values of such gaps under these conditions were found, over the range of voltages studied, to be essentially the same as with the conductor grounded. These data also showed that, at heights of 30 feet or more above the earth, the influence of the earth plane is negligible. For symmetrical gap electrodes, breakdown is independent of polarity and

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close to the positive polarity breakdown of standard rod gaps.

A configuration consisting of a third ground wire placed below the phase conductors for increased coupling was also studied. Although the coupling for all measured voltages was greater than without the third ground wire, it did not increase as rapidly with voltage as calculations assuming the effective corona radii found for only one or two ground wires would indicate. This was thought to be due to mutual shielding produced by the ground wires tending to decrease corona streamers in the space between them.

THE lightning flashover performance of transmission lines provided with either overhead ground wires, or top conductors equipped with deion protector tubes and serving as shielding wires, is determined not only by the line insulation level and the tower top potential produced by a direct stroke but also by the voltage which appears simultaneously on the phase conductors through induction. One of the principal factors determining this induced voltage is the electrostatic coupling with the ground wire. This is commonly represented by the coupling factor K which is the fraction of the ground-wire potential induced by it on the phase conductor. The surge induced in this manner consists initially of only a voltage wave unaccompanied by current flow until a point of discontinuity on the conductor is reached by the propagating wave.[1] With the exception of strokes close to substations, the only discontinuity to cause a reflection and establishment of an accompanying current wave is provided by the line insulators. These, however, are of such low capacity and high impedance that they produce only a minor effect that can usually be neglected. In any refined analysis to treat such points of discontinuity, the initial induced voltage wave must be known and is a fundamental consideration.

Neglecting the effect of corona on the ground wire by virtue of the potential it acquires when struck, the coupling factor can be calculated readily from the geometric configuration of the line.[1] In the past it has been such values that were most commonly used. However, it has been recognized for some time[2,3] that the presence of corona on the ground wire causes the coupling factor to increase considerably with the ground-wire potential. Calculations of the effect of corona have been made by Torok and Ellis.[3] Its effect is to produce a corona sheath around the ground wire and thus increase its effective radius. The corona radius was determined by them on the basis that it would be of such a magnitude for each ground-wire potential and height above earth as to produce a gradient at this radius equal to 30,000 volts per centimeter (the normal critical breakdown value of air). However, actual reliable measurements of the corona effect have not been available.

Description of Measurements

The coupling factor measurements described in this paper were made at the Trafford High-Voltage Laboratory on relatively short lengths of line. The low impedance of long lines makes it impossible to obtain sufficiently high voltages to be of practical value. However, since the coupling factor is determined only by electric and not by magnetic effects, short lengths of line can be used, provided the voltage induced on the floating conductor can be reliably measured by a potentiometer of sufficiently high impedance that it will not influence the conductor voltage. The development of such recording means was the principal difficulty encountered.

For a preliminary study to determine the effect of various controlling factors, measurements were made inside the laboratory on a line 65 feet long with a ground-wire height of ten feet. This readily enabled the changing of the line configuration. For such a short line, the capacity to ground of the floating conductor is of the order of 150 to 200 micromicrofarads,

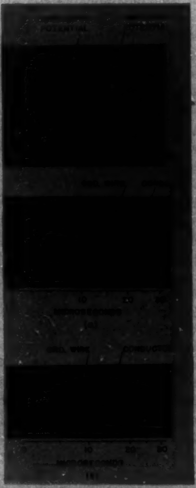
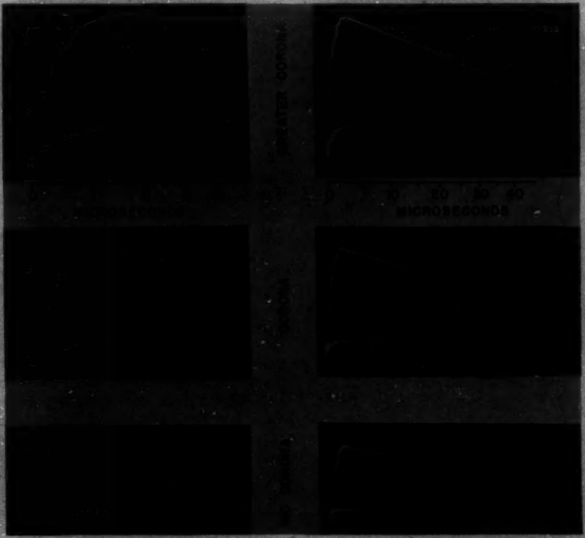


Figure 1. Wave shapes of the ground wire and induced conductor potentials measured on the outdoor line

Figure 2. Oscillograms showing effect of corona upon voltage induced on a floating electrode



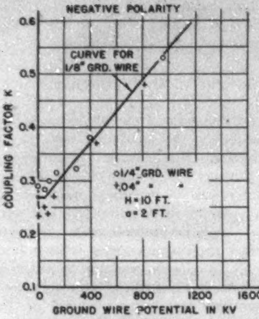


Figure 3. Curve showing that ground-wire size has no significant effect upon coupling factor when corona is present

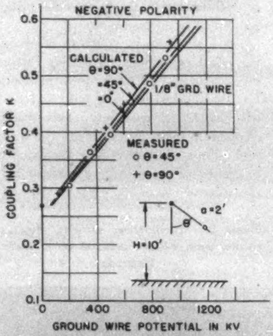
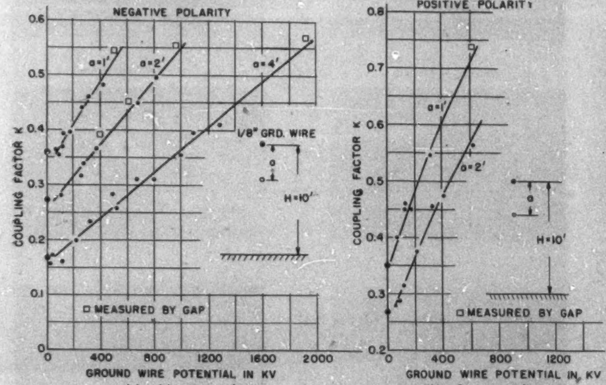


Figure 4. Curve showing that, for one ground wire, coupling factor is determined only by ground-wire potential, its height, and spacing between it and conductor

Figure 5 (below). Coupling factors as a function of ground wire potential. *H*=10 feet. Single ground wire



(*a*). Negative polarity

(*b*). Positive polarity

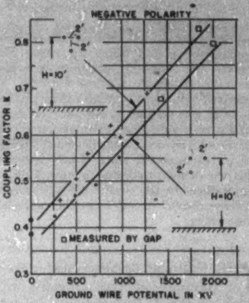


Figure 6. Coupling factors as function of ground-wire potential. *H*=10 feet. Two ground wires. Negative polarity

and it was not found possible to construct a purely capacitive divider of small enough capacitance that could be adequately shielded from the ground wire. A high-resistance potentiometer was developed that did not appreciably load the conductor. It had, however, a time constant of about one microsecond so that fronts of the induced voltage wave, if less than about five microseconds, could not be accurately recorded.

Measurements on lines of practical dimensions were made on a single span providing a conductor length of 350 feet and constructed outside the laboratory over a level section of terrain. Two ground-wire heights 30 and 50 feet were studied. For the mean height of 50 feet a seven foot sag was allowed. For the 30 foot ground-wire height the sag was about five feet. For both the indoor and outdoor lines, the phase conductors were kept equidistant from the ground wire at all points. They were supported from wood poles by long ropes or cords and the ground wire extended at least ten feet past the ends of the conductors for the indoor line and 40 feet for the outdoor line to minimize end effects. The capacity to ground of the floating conductors of the outdoor line was of the order of 700 to 800 microfarads, and a capacity divider was found most suitable. Its coupling with the ground wire did not exceed about ten per cent of the lower values of voltage induced on the conductors, and its capacity was never above about eight per cent of the capacity to ground of the floating conductor. The effects of these factors act in opposite directions, and the recording accuracy is considered to be within five per cent.

In Figure 1 are shown typical oscillograms of the voltages measured on the outdoor lines. These were always made at the surge generator end of the ground wire and phase conductor. For the oscillograms of Figure 1*a*, the initial low magnitude crest on the ground-wire voltage is produced by its initial low impedance which is its surge impedance. After a reflected wave propagates back from its far end, it is essentially a capacitor of about 900 micromicrofarads and allows the surge generator to increase the voltage as shown. For wave fronts this short, an accurate record of the front of the induced voltage could not be obtained, because it was necessary to place the cathode-ray oscillograph about 40 feet from the divider and connect them with a low capacity shielded cable. This produced the reflections shown in the conductor voltages of Figure 1*a*. Figure 1*b* shows a ground-wire voltage wave of slower front which was also used and for which the front of the induced voltage could be recorded.

Relation of Induced to Ground-Wire Potential for Different Points on Voltage Wave

To study the effect of corona on the front of the induced voltage wave for steeper wave fronts, a special setup was made inside the laboratory. It consisted of an electrode to which measured voltages were applied with a floating electrode underneath that was connected directly to the plates of the oscillograph only a few feet away. Typical oscillograms obtained both above and below

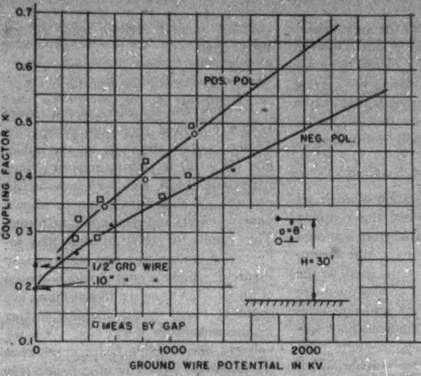
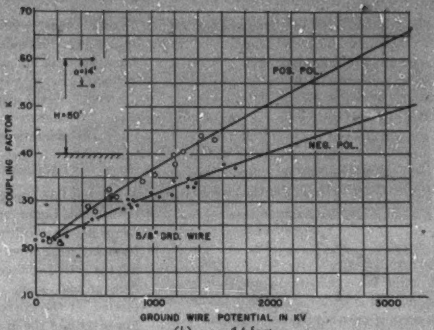
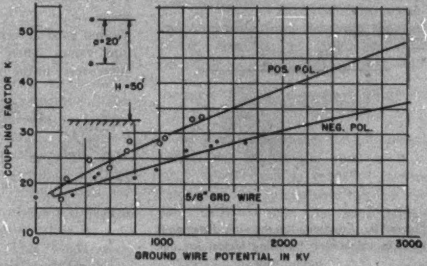


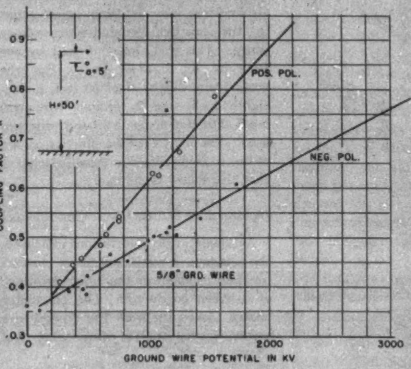
Figure 7. Coupling factors as function of ground-wire potential. *H*=30 feet. One ground wire 0.1 inch in diameter



(*b*). a=14 feet



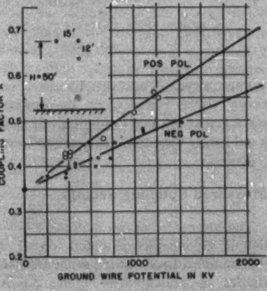
(*c*). a=20 feet



(*a*). a= 5 feet

Figure 8 (left and above). Coupling factors as function of ground-wire potential. One ground wire. *H*=50 feet

Figure 9a (right). Coupling factors for two ground wires 15 feet apart. *H*=50 feet



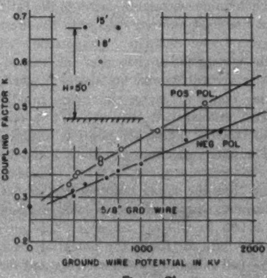


Figure 9b

corona voltage are shown in Figure 2. Comparison of the fronts of the waves at different magnitudes of applied voltage showed that at a given voltage of the upper electrode the induced voltage is essentially the same regardless of the time of occurrence, as long as the comparison is made on the front or crest of the wave. The same result was shown by a study of the slower ten-microsecond front of Figure 1*b* on the outdoor line. For all points on the wave up to its crest, the ratio of the induced voltage to the ground-wire voltage can be represented sufficiently by a coupling factor that is a function only of the ground-wire potential at that particular time.

However, because a small amount of current flows to the conductor when corona is present, the induced voltage wave of a short length of floating conductor does not decrease on the tail of the wave at a rate proportional to the ground-wire potential but is determined by the amount of charge acquired and the leakage impedance of th conductor. This effect is shown in Figures 1 and 2. For a long line where the induced voltage is propagating away from the point on the line contacted by the stroke, the amount of charge is so small as to produce negligible magnetic effects and conductor current, except that the charge should be carried away so rapidly that the tail of the voltage wave induced on the phase conductor by coupling with the ground wire should decrease at a rate determined by a coupling factor varying only with the ground-wire voltage magnitude. Thus, it appears evident that the wave shape and magnitude of the induced conductor voltage can be determined from the ground-wire potential and a coupling factor varying only with ground-wire potential. The higher this potential at any instant the greater the proportion of voltage induced on the conductor. This means, for instance, that the tail of the induced wave on a long line should decrease faster than that of the ground-wire voltage wave. Thus the difference between these voltages, or the voltage wave across the line insulation will have a tail longer than that of the ground-wire potential.

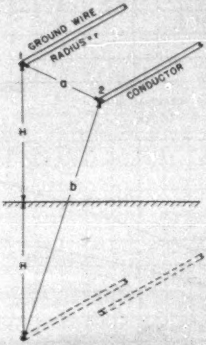


Figure 10. Configuration showing dimensions used in calculating coupling factors for one ground wire

$$\text{Coupling factor } (K) = \frac{\log \frac{b}{a}}{\log \frac{2H}{r}}$$

Measured Coupling Factors

The curves of coupling factor as a function of ground-wire potential for all of the standard ground-wire configurations studied are presented in Figures 3 to 9. Each value of coupling factor is the ratio of the crest induced voltage to the crest ground-wire voltage. Examination of the curves shows that after the ground-wire critical corona voltage is reached, the coupling factor increases rapidly with ground-wire potential. It is also appreciably higher for positive voltages. The maximum voltages which could be produced were about 2,000 kv. Considering that the upper limit of crest stroke current magnitudes is of the order of 150,000 to 200,000 amperes,[4] this provides data for tower top potentials over the full range of stroke currents and for towers having footing resistances up to 15 or 20 ohms. This is the most important range. However, as will be shown later, the data are extrapolated to higher voltages. Before considering the general character of the coupling-factor curves more thoroughly, a number of fundamental considerations will be discussed.

Coupling Factors for No Corona

For the line configuration of each set of coupling-factor curves, the coupling factor has been calculated from standard formulas neglecting corona and assuming the actual surface of the earth as the true ground plane. These values are plotted on the curves at zero potential. For several of the line configurations (see Figures 5*a* and 8*b*) measurements were made below the corona voltage which showed good agreement with the calculated values. There has been considerable controversy in the past as to the location of the effective ground plane in relation to earth resistivity for both electrostatic and magnetic phenomena. These measured data show that both the surface of the floor of the high-voltage laboratory and the ground under the outdoor line act as the effective ground plane for this electrostatic problem. During the period of these tests the earth resistivity under the outdoor line ranged from 400 to 600 meter ohms, as compared to the most common general average value of 100 meter ohms.[5]

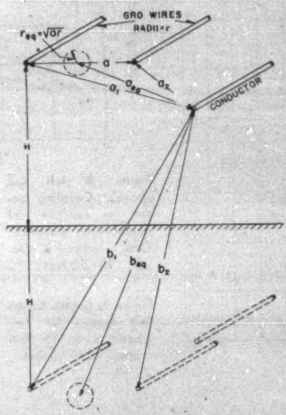


Figure 11. Configuration showing dimensions used in calculating coupling factor for two ground wires

$$\text{Coupling factor } (K) = \frac{\log \frac{\sqrt{b_1 b_2}}{\sqrt{a_1 a_2}}}{\log \frac{2H}{\sqrt{ar}}}$$

Effect of Size of Ground Wire

To determine the effect of the size of both the ground wires and phase conductors, tests were made on the indoor line (H = 10 feet) with smooth copper wires 0.04, 0.125, and 0.25 inch in diameter. The size of the conductor should have no effect, and none could be found. The results of tests to show the influence of ground-wire size are shown in Figure 3. Above a ground-wire potential of about 200 kv, no significant difference could be found in the coupling factors for this wide range of wire size. At lower voltages, the coupling factor approaches the calculated values for no corona. It is quite apparent that up to the critical corona voltage the coupling factor is constant, and above this value for the larger wire there is little difference. Ground-wire size is thus unimportant in the practical ground-wire voltage range. This was also confirmed by the tests on the outdoor lines. For the ground-wire height H of 50 feet, a five-eighths-inch smooth ground wire was used. For H of 30 feet a 0.1-inch wire was used. The analysis of the effective corona radius to be discussed later will also show that this factor is unimportant.

Presence of Other Phase Conductors

As theoretical considerations indicated, the presence of additional phase conductors had no effect on the induced voltage of a given conductor. The conductor charge picked up from the ground-wire corona was too small to cause any mutual effect.

Effect of Conductor Position With One Ground Wire

Calculations of coupling factor for no corona[1] have shown that for one ground wire and practical conductor configurations, the only important dimension relating the position of the conductor relative to the ground wire is the distance a between them (see insert of Figure 4). The data of Figure 4 show that this is also true at high voltages when corona is present.

Gap Flashover Measurements

As a check against the accuracy of the voltage measurements and also to determine the character of flashover conditions on an actual line, data were also obtained on the ground-wire potential necessary to flashover rod gaps connected between the phase and ground wires. These were gaps with one-half inch round electrodes of sufficiently low capacity to produce no appreciable effect on the induced voltage. The points on the coupling-factor curves of Figure 7 indicated as measured by gap flashover were determined from the minimum crest flashover value of the gaps with the conductor first floating and then grounded. The points at highest voltage of Figures 5 and 6 (for $H=10$ feet) were determined from flashover between the ground wire and conductor. The coupling-factor points determined by gap breakdown are from four to eight per cent higher than those determined from measurement of the conductor voltage.

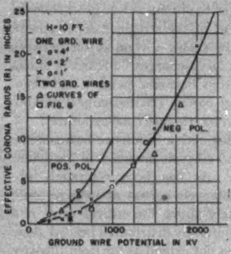
Figure 13. Ground-wire corona produced by surge voltage with $1\frac{1}{2}\times 40$ wave shape. $\frac{1}{8}$-inch wire, $H=10$ feet. Effective radius (from Figure 12a) is 11.5 inches

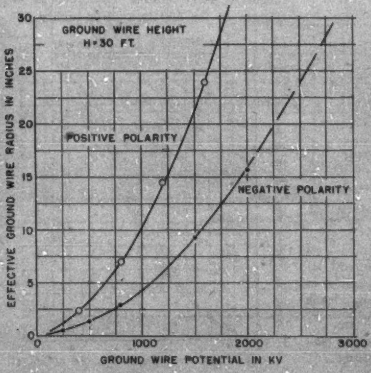
Two factors might cause this difference. The field configuration with the conductor floating and with it grounded may be somewhat different. With it floating, the discharge would usually be expected to start from the ground-wire electrode where the gradient is higher. This should make less difference when the ground wire is positive, since for equal field distortion the discharge will always be initiated from the positive electrode.[6] The other factor is the difference in the wave shape of the voltage across the gap under the two conditions. With the conductor grounded, the gap voltage is the ground-wire voltage which had a tail with 40 microseconds to half value. With it floating, it is (for the short lengths of line) the difference between this and the induced conductor voltage which had a longer tail as explained previously. The greater the ratio of the conductor voltage to the ground-wire voltage, the shorter the tail of the gap voltage wave. The shortest tail for the gap voltage in these tests was about 20 microseconds for which the critical breakdown voltage is about seven per cent above that for a 40 microsecond wave. This would give, therefore, an indicated coupling factor seven per cent too high. The fact that the coupling factor points determined in this way are high by this order of magnitude indicates this to be the principal factor.

It thus is evident that for the range of voltages studied here the probability of line flashover for direct strokes can be based upon the knowledge of the voltage across the insulation (which is the difference between the ground-wire potential and the conductor potential) together with the flashover strength of this insulation determined from tests with the conductor terminal of the insulation grounded.

Effect of Polarity on Gap Breakdown

The field configuration for line insulation is not exactly the same as it is when tested close to ground. It was found that for the flashover tests on the line with $H=30$ feet, the critical breakdown of the gaps was independent of polarity and lay close to the positive polarity breakdown of standard rod gaps on the ground. This is to be expected when the influence of the ground plane is negligible and the gap electrodes are similar. This difference is a secondary effect which cannot be taken into account until more gap breakdown data are available.





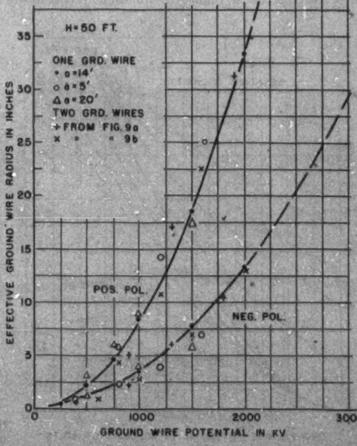


Figure 12. Effective corona radius as function of ground-wire potential

(a above left). $H=10$ feet. Calculated from measured coupling factors of Figures 5 and 6

(b left). $H=30$ feet. Calculated from coupling factors of Figure 7

(c right). $H=50$ feet. Calculated from coupling factors of Figures 8 and 9

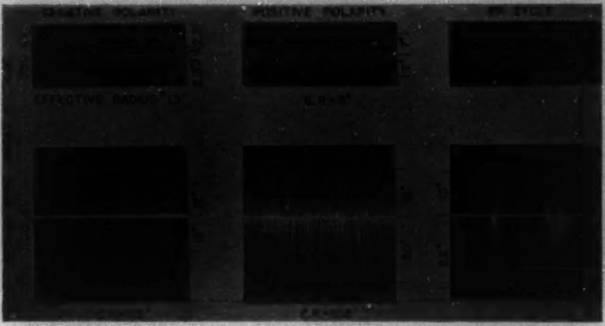


Figure 14. Ground-wire corona showing comparison between 60-cycle and both positive and negative surge voltages. $^1/_8$-inch wire. $H = 10$ feet

*Effective radii determined for Figure 12a

Effective Corona Radius

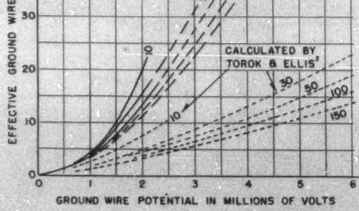
Standard equations for calculating coupling factors when corona is neglected are given in Figures 10 and 11 for the two most common types of ground-wire configuration. For any configuration of conductor and ground wires and ground-wire potential, there is a radius that can be substituted into these equations to give the correct coupling factor. If this "effective radius" is not a function of too many variables, considerable simplification is obtained from using it in the standard equations. Based upon the assumptions of Torok and Ellis,[3] this effective radius is a function only of the ground-wire height H and its potential.

With this thought in mind, values of the effective radius were calculated from the equations of Figures 10 and 11 and the coupling factors of Figures 3 to 9. These are plotted in Figure 12 for each ground-wire height and polarity as a function of ground-wire potential. These curves show that this effective radius is essentially a function of only the ground-wire height and potential. This would be expected for one ground wire as these are the only two factors affecting the field configuration around the wire, and the ground plane would not be expected to exert enough distortion to materially alter the effective center of the corona sheath. As shown by Figures 12a and c the effective radius for two ground-wire configurations deviates more from the mean curve. This is probably due to the mutual shielding effect of the two wires. However, 15 feet is near the lower limit for their separation in practice, and this effect should decrease with greater separations. Since the coupling factor varies with the logarithm of this radius the variations indicated by Figure 12 are not sufficient to cause more than a few per cent variation in the coupling factors.

Visual Characteristics of Surge Corona

To determine the visual characteristics of corona produced by impulse voltages and to compare the extent of streamer formation with the effective radii, still photographs were made of the indoor line with the one-eighth-inch ground wire. It was necessary to photograph about 20 discharges to obtain a clear print showing the extent of the streamer development. Figure 13 shows a comparison between one and 20 discharges. Figure 14 shows comparisons between positive and negative surge corona (20 discharges) and 60-cycle corona (three-second exposure).

The distinctive difference between the surge and 60-cycle corona is of particular interest. As has been commonly observed, 60-cycle corona at voltages only moderately above the critical value is a rather uniformly distributed beaded glow. This does not extend as far from the wire as the streamers for either positive or negative surge corona (see records of Figure 14 for 550 kv). For higher 60-



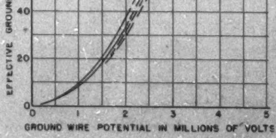


Figure 15

(a left). Effective radii for negative polarity compared to values calculated by Torok and Ellis[1]

(b right). Effective corona radii for positive polarity

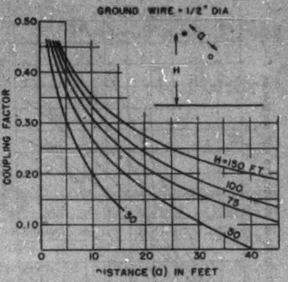


Figure 16. Coupling factors when corona is not present. One ground wire $^1/_2$ inch in diameter

cycle voltages brush discharges of about the same extent as for positive surges are formed (see Figure 14 for 1,100 kv). However, they are bunched at localized points on the wire. This is probably because streamers start first at these points, as the voltage increases relatively slowly on each half cycle, allowing sufficient time for them to develop and shield the other parts of the wire. The streamer formation is much more uniform for surge voltages probably because of the very rapid rate of rise of voltage tending to produce the more nearly instantaneous formation of streamers at all points along the wire. The photograph of Figure 13 for one surge shows about one or two streamers per inch of wire extending a mean distance of about 30 inches toward ground and 23 inches upward for the crest voltage of 1,670 kv. In between these are shorter streamers of varying length that probably did not start to develop as fast as the longer ones and were partly shielded by them.

As shown by Figure 14, the length and density of the positive corona streamers are considerably greater than for negative corona accounting for the greater effective radius for positive corona. Because of the influence of the ground plane, longer streamers are formed toward ground than upward. This dissymmetry increases with voltage. It also should be greater the lower the ground-wire height *H*. However, even the amount of dissymmetry shown in these photographs for *H* of ten feet does not cause the effective radius to change materially for the practical range of variation of conductor position. The mean distances that the longer streamers extend downward from the ground wire is roughly twice the effective radii of Figure 12*a* for both polarities.

EXTRAPOLATED CURVES OF EFFECTIVE CORONA RADIUS

In Figure 15 the effective radii determined from the measured coupling factors are compared to those calculated by Torok and Ellis. They are greater than the calculated values even for negative polarity. They also show somewhat less decrease with ground-wire height and greater rate of increase with ground-wire potential. The variation with ground-wire height is less for positive polarity. In this figure the portions of the curves established by measured data are drawn as solid lines while the extrapolated curves are dashed lines. The extrapolation was based upon the variation of the radii (over the measured range) with ground-wire height, potential, and field gradient. The curves are extrapolated as far as deemed possible with reasonable accuracy. This provides data over the full range of stroke currents for tower footing resistances up to 40 or 50 ohms.

Practical Curves for Determining Coupling Factor

The coupling factor for any practical line configuration can be obtained by substitution of the effective corona radii given by Figure 15 into any of the conventional forms of the coupling-factor equations, such as those of Figures 10 and 11. However, they can be more simply obtained from the curves of Figures 16 to 18. Figures 16 and 17 give values of coupling factor for one and two ground wires of one-half-inch diameter for no corona and are reproduced from reference 1. Figure 18 gives the proper multiplying factors to apply to the values read from these curves to account for corona. Since for the practical range of voltages the coupling factor is independent of actual ground-wire size, the coupling-factor data neglecting corona for any convenient wire size can be used as such a basis with the proper multiplying factors for this size. The analytical

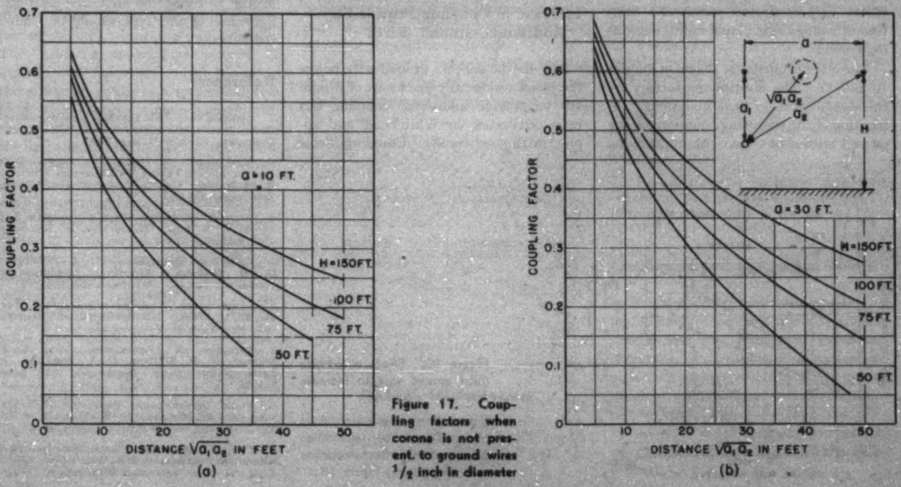


Figure 17. Coupling factors when corona is not present. to ground wires $^1/_2$ inch in diameter

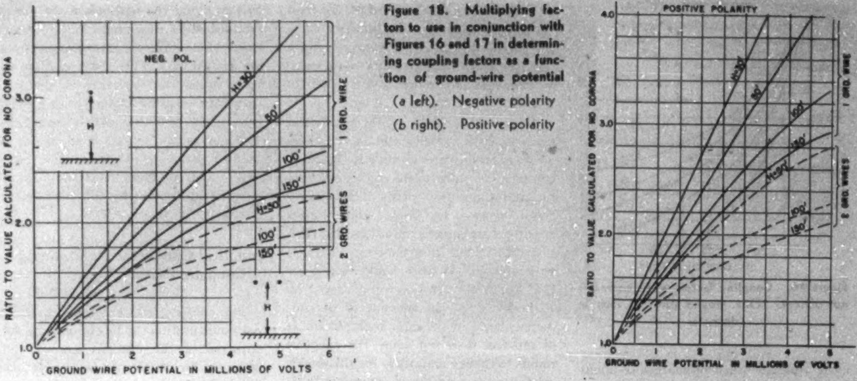


Figure 18. Multiplying factors to use in conjunction with Figures 16 and 17 in determining coupling factors as a function of ground-wire potential

(*a* left). Negative polarity

(*b* right). Positive polarity

character of these multiplying factors is shown by the following equations.

For one ground wire

$$K=\frac{\log b/a}{\log\frac{2H}{r(V)}}=\frac{\log b/a}{\log\frac{2H}{r_o}}\times\frac{\log\frac{2H}{r_o}}{\log\frac{2H}{r(V)}}$$

For two ground wires

$$K=\frac{\log\frac{\sqrt{b_1b_2}}{\sqrt{a_1a_2}}}{\log\frac{2H}{\sqrt{ar(V)}}}=\frac{\log\frac{\sqrt{b_1b_2}}{\sqrt{a_1a_2}}}{\log\frac{2H}{\sqrt{ar_o}}}\times\frac{\log\frac{2H}{\sqrt{ar_o}}}{\log\frac{2H}{\sqrt{ar(V)}}}$$

where $r(V)$ is effective radius as a function of voltage r_o is actual radius neglecting corona.

The other symbols are shown in Figures 10 and 11. The multiplying factors are the second terms in these equations. Inspection of the preceding equation for one ground wire shows that the multiplying factor is a function only of the ground-wire height and potential, so that a single curve for each height applies to all spacings between ground wire and conductor. For two ground wires, their spacing a exerts a slight influence, but the deviation of the multiplying factors from the mean curves drawn in Figure 18 is only about five per cent for spacings of a lying between 10 and 40 feet.

The data for negative polarity are, of course, of greatest importance since about 90 per cent of all strokes are entirely of negative polarity[7] and at least 95 per cent have negative polarity for the higher magnitude components.

Increase in Coupling Provided by Additional Ground Wires

The use of another ground wire below the phase conductors (see insert of Figure 19) to provide additional coupling has been advocated by Whitehead[8] and applied with good results. Coupling-factor measurements were made for such a configuration, and the measured values are compared in Figure 19 with those calculated by using the effective corona radii of Figure 15. The actual increase of coupling with voltage is less than calculated. This is probably due to the mutual shielding effect produced by the ground wires in the space between them. However, comparison of the measured coupling factors of Figure 19 with those of Figure 9*a*, where the configuration is identical except for the third ground wire, shows that an increased improvement of coupling for all voltages is provided by the third ground wire. Because of this shielding effect, the effective radii of Figure 15 cannot be used, and each configuration of this type should be studied individually.

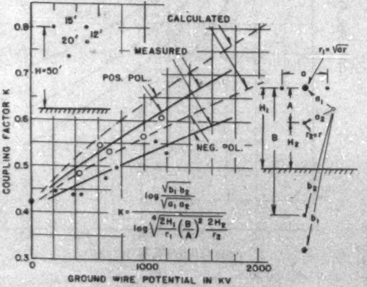


Figure 19. Effect of adding third ground wire to increase coupling

Comparison of measured values of coupling factors with those calculated from effective corona radii given by Figure 12c

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Pole-Face Loss in Solid-Rotor Turbine Generators

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ONE of the primary problems which confront the designer of electrical machinery is the predetermination of losses. This is peculiarly true for the designer of high-speed turbine-driven generators; the losses must be kept low, not only to increase the efficiency but also to keep the size of the apparatus to a minimum.

Losses are usually classified as

(*a*). Ventilation, windage, and friction.

(*b*). Open-circuit core losses.

(*c*). Armature and field copper loss.

(*d*). Load loss.

It is with one component of this last group of losses that this paper is concerned.

The term "load loss," as usually employed, includes all extra losses coincident with the operation of the machines which are not included in the other items. In test it is usually determined as the difference between the actual losses on short-circuit loss test and the more readily determined components of windage, friction, copper loss, and so forth.

In solid-rotor turbine-driven revolving field generators, one of the largest components of load loss is caused by the existence of eddy currents in the surface of the rotor. These eddy currents are caused by the armature-coil currents. The balanced multiphase current of fundamental frequency which flows in the armature produces a magnetomotive force which rotates synchronously with the rotor. It also produces, because of the imperfect distribution of the armature coils, magnetomotive forces which move with respect to the rotor, causing eddy currents and losses in the rotor surface. Unbalanced armature phase currents and armature currents of other than fundamental frequency can also give rise to these losses.

This paper concerns itself with the development of methods whereby the designer may predetermine the amounts of these pole-face losses.

In Appendix A the general mathematical relations governing the phenomena are written; equations for the loss are presented in forms which permit reasonable facility in calculation. Appendix B presents methods of determining winding factors which are fundamental to the determination of losses. Sample calculations are given in Appendix C. A brief list of references is added for those who are interested in a further study of various aspects of the problem. A summary of the results of the investigation, a recapitulation of the assumptions in the solutions presented, a discussion of the limitations of the method, and a list of nomenclature follow.

Results

It is found that the loss attributed to any harmonic of armature magnetomotive force which moves relative to the rotor may be written

$$\text{Loss} = (^1/_2)I_{on}^2\,(\text{area})4\pi 10^{-9} lfR \qquad (19)$$

An equivalent form, using quantities with which the designer is more familiar is given by equation 24. Losses caused by each of several coexistent harmonics are added to obtain the total loss.

The accuracy of the calculation for loss depends on the applicability of the assumptions and on the evaluation of the factor *R*. Use of the method indicates that the assumptions are valid for the usual case and that for the extremes where rotor end effects or magnetic saturation are appreciable, means are available to mitigate the inaccuracies attributable to these causes. The factor *R* may be determined from:

1. Equation 22.
2. Figure 2, where a minor limitation is placed on the value of permeability as explained in the text following equation 22.
3. Figure 3, which, in effect, assumes a permeability such that loss is maximum. Figure 3 may be used with good accuracy for the determination of those losses in turbine generators of usual design which are caused by armature magnetomotive force harmonics of the fifth to the fifteenth order. Experience has shown that substantially the entire pole-face loss in most machines is caused by harmonics within these limits.

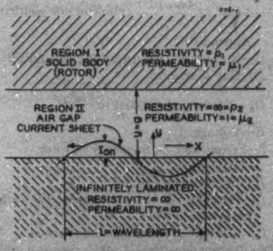

Figure 1. Idealized system

The current sheet on the stator magnetomotive-force wave moves relative to the rotor causing eddy currents and losses there. See Appendix A

Assumptions

The major assumptions used in this paper to simplify the determination of pole-face losses are summarized:

1. The fundamental solution is made in terms of rectangular, rather than cylindrical co-ordinates.
2. End effects are neglected; the rotor is assumed to be infinitely long.
3. The armature magnetomotive force of each harmonic order is represented in the calculations by a current sheet on the stator surface of sinusoidally varying density, which produces the same value of magnetomotive force in the air gap for that harmonic as the actual windings in the slots.
4. The resistivity and permeability of the materials are constant. Stator permeability and resistivity are assumed infinite.
5. The losses of all harmonics can be superposed; the total loss is the sum of the losses determined for each harmonic.
6. Discontinuities in the rotor surface because of wedges, and so forth, are neglected (but the value of permeability may be chosen to make allowances for this condition).
7. In utilizing the equations developed, maximum permeability is limited by the tangential component of flux in the rotor surface. (See Appendix C.)

Conclusion and Discussion

The methods of determining pole-face loss in turbine generators which are presented here are simple and reasonably accurate. For machines of normal design they make possible a determination of loss within a few minutes after winding factors are known; for exceptional cases, the accuracy of the method is less, and the calculations are not simple, but the utility is great.

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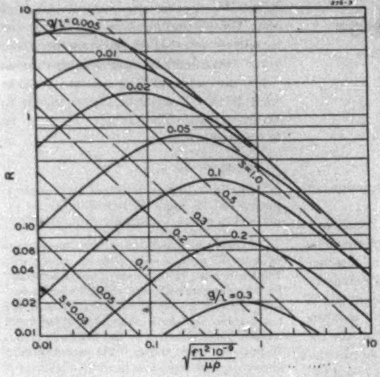

Figure 2. Plot of the resistance factor R and the flux density factor S with the parameters shown

See paragraph following equation 22

It has been gratifying, after investigating mathematically these complex phenomena, to find that the calculation of losses by means of relatively simple equations, without resort to empirical constants for the most frequently encountered cases, is corroborated by test results.

One fact which is apparent from a review of the data presented in this paper is that, for three-phase machines of usual design, high pole-face losses most often arise from the fifth and seventh magnetomotive force harmonics. It is evident from Figure 3 that the low harmonics are the most powerful in increasing the factor R. A glance at the winding constants of Table I, together with equation 24, will show that the fifth and seventh harmonics are most usually the cause of high pole-face losses. This is further illustrated by the third sample calculation of Appendix C.

It is interesting to note also that a winding with two-thirds pitch is by no means best from a standpoint of pole-face loss. In this connection it may be pointed out that the specification of the coil spacing on the rotor, together with the winding factors, determines the telephone influence factor of the open-circuit voltage of the machine. It is evident from the preceding remarks that any preference for a stator winding of given pitch, such as two-thirds pitch, except where all factors are adequately studied and quantitatively understood, is unwarranted.

The paper presents not only equations for calculating the losses, but also equations for determining the pitch and distribution factors and the effects of phase groupings of armature coils. This extension of the fundamental purpose of the paper was made so that the designer might have at hand all the tools with which to treat most cases.

A number of writers, in published and unpublished work, has treated the subject of induced losses in solid materials. The singular contribution of this paper is believed to be the framing of the results so that they can be readily applied in the design of turbine-driven generators.

Nomenclature

Centimeter-gram-second units are used, except where otherwise stated. Electrical units are in the "practical" system. English units are used in the sample calculation and in equation 24; the parts of the report probably most directly useful to the designer.

A, B, C—Arbitrary constants.
A_n—"Square wave" value of armature reaction, nth harmonic.
a—$2\pi/l$.
b—$\sqrt{a^2+j\frac{4\pi\mu\omega 10^{-9}}{\rho}}$ (also coil side width in Appendix B).
C—Number of coils in a group (Appendix B).
D—Gap diameter.
D_n—Factor proportional to $\frac{k_{pn}k_{dn}}{n}$.
E—Voltage.
f—Frequency; f_r, frequency in rotor.
g—Gap length.
H—Magnetic intensity.
I_{on}—Density of current sheet for nth harmonic.
I_m—Current of harmonic frequency m.
i—Current density.
j—$\sqrt{-1}$.
$k_{pn}k_{dn}$—Pitch and distribution factors for nth space harmonic, <1.
L—Active machine length (inches).
l—Wave length.
m—Order of time harmonic.
n—Order of space harmonic.
P—Number of poles.
p—Pitch.
Q—Number of phases, q = order of phase (Appendix B).
$\bar{q}$, q_i, q_r—See equation 21.
R—Resistance factor (equation 20).
S—Magnetic intensity factor (equation 25).
t—Time.
x, y, z—Co-ordinates (Figure 1).
β—Flux density (or electrical slot angle in Appendix B).
γ—Angle between coil groups.
θ—Electrical angle (Appendix B).
μ—Permeability.
ρ—Resistivity.
ω—$2\pi f$.

Appendix A. Development of the Equations for Loss

This appendix is divided into three sections. In the first, the region of the air gap is analyzed mathematically to determine the relations between the fundamental electrical quantities.* In the second section the equations governing the loss are written from those developed in the first section. The third section contains the development of equations for the magnetic intensity at the rotor surface. In some cases, a knowledge of this magnetic intensity is necessary because the loss is a function of the permeability of the rotor steel and the permeability depends on intensity of the magnetic field.

Fundamental Relations

The region of the air gap may be idealized as shown on Figure 1. One of the harmonics of stator magnetomotive force which moves with respect to the rotor is represented as a current sheet of infinitesimal thickness on the stator surface, which has wave length l and a maximum value of I_{on} per unit length

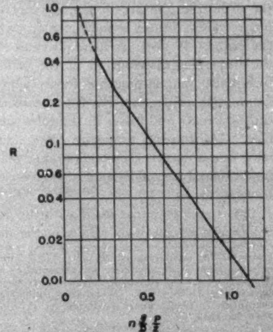

Figure 3. Simplest manner of determining R

For limitations see discussion following equation 22 of sheet. The stator is represented as a perfectly laminated semi-infinite solid of infinite permeability and resistivity. The rotor (region 1) at a distance $y=g$ from the stator, is represented by a semi-infinite solid of permeability $\mu=\mu_1$ and resistivity $\rho=\rho_1$. The air gap, region 2, has permeability $\mu=\mu_2=1$, resistivity $\rho=\rho_2=\infty$.

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* The method of analysis was outlined to the writer by Dr. H. Poritsky.

Maxwell's equations, in vector form, are written below.

$$\nabla\times\overline{H}=0.4\pi\overline{i} \qquad \rho\overline{i}=\overline{E}$$

$$\nabla\times\overline{E}=-10^{-8}\frac{\partial\beta}{\partial t} \qquad \overline{\beta}=\mu\overline{H} \tag{1}$$

Since the only voltage existent is that in the Z−direction, the second part of equation 1 reduces to

$$\frac{\partial E_z}{\partial y}=-10^{-8}\mu\frac{\partial H_x}{\partial t} \quad \frac{\partial E_z}{\partial x}=10^{-8}\mu\frac{\partial H_y}{\partial t} \tag{2}$$

Combination of equations 2 and 1 yields

$$\frac{\partial^2 E_z}{\partial x^2}+\frac{\partial^2 E_z}{\partial y^2}=\frac{4\pi\mu 10^{-9}}{\rho}\frac{\partial E_z}{\partial t} \tag{3}$$

The solutions for current and voltage, and so forth, are harmonic; they may be written:

$$i_z=i_z'\epsilon^{j(\omega t+ax)}$$

$$E_z=E_z'\epsilon^{j(\omega t+ax)}$$

$$H_x=H_x'\epsilon^{j(\omega t+ax)}$$

$$H_y=H_y'\epsilon^{j(\omega t+ax)} \tag{4}$$

Thus equations 2 and 3 become

$$\frac{\partial E_z}{\partial y}=-j10^{-8}\mu\omega H_x$$

$$\frac{\partial E_z}{\partial x}=jaE_z=j10^{-8}\mu\omega H_y$$

$$-a^2E_z+\frac{\partial^2 E_z}{\partial y^2}=\frac{j\omega 4\pi\mu 10^{-9}}{\rho}E_z \tag{5}$$

The controlling differential equation is written from equation 5,

$$\frac{\partial^2 E_z}{\partial y^2}=b^2E_z \tag{6}$$

where

$$b^2=a^2+\frac{j4\pi\omega\mu 10^{-9}}{\rho}$$

$$=\left(\frac{2\pi}{l}\right)^2+\frac{j4\pi\omega\mu 10^{-9}}{\rho} \tag{7}$$

The solution may be written in the form, for region 1,

$$E_{z1}=C\epsilon^{-b_1(y-g)} \tag{8}$$

for region 2,

$$E_{z2}=A\cosh a(y-g)+B\sinh a(y-g) \tag{9}$$

The constants A, B, and C are fixed by the conditions across the boundary between regions 1 and 2,

E_z is continuous

$$\frac{1}{\mu}\frac{\partial E_z}{\partial y} \text{ or } \frac{\partial H_x}{\partial t} \text{ is continuous} \tag{10}$$

and at $y=0$,

$$\frac{\partial E_z}{\partial y}=j4\pi 10^{-9}\mu_2\omega I_{on}$$

The resulting equations are written below

$$E_{z1}=-jI_{on}4\pi 10^{-9}lf\,\frac{\epsilon^{-b_1(y-g)}}{\sinh ag+\frac{b_1}{a\mu_1}\cosh ag} \tag{11}$$

$$E_{z2}=-jI_{on}4\pi 10^{-9}lf\times\frac{\cosh a(y-g)-\frac{b_1}{a\mu_1}\sinh(y-g)}{\sinh ag+\frac{b_1}{a\mu_1}\cosh ag} \tag{12}$$

At the current sheet, where $y=0$, the impedance (per square centimeter) met by the current is

$$Z=-\frac{E_z}{I_{on}}\bigg|_{y=0}=j4\pi 10^{-9}lf\times\frac{1+\frac{b_1}{a\mu_1}\tanh ag}{\tanh ag+\frac{b_1}{a\mu_1}} \tag{13}$$

The loss may be written from equation 13

$$\text{Loss}=\left(\frac{I_{on}^2}{2}\right)\text{(area of current sheet)}\text{(real part of }Z) \tag{14}$$

Equation 14 will be stated in more convenient forms in a subsequent section. Before this is done, the general equations for the magnetic intensities will be written.

The equations governing the magnetic intensities may be written directly from equations 2, 11, and 12.

In region 1 (rotor iron)

$$H_{X1}=-0.4\pi I_{on}\frac{b_1}{a\mu_1}\times\left[\frac{\epsilon^{-b_1(y-g)}}{\sinh ag+\frac{b_1}{a\mu_1}\cosh ag}\right] \tag{15}$$

$$H_{y1}=-j0.4\pi I_{on}\frac{1}{\mu_1}\times\left[\frac{\epsilon^{-b_1(y-g)}}{\sinh ag+\frac{b_1}{a_1}\cosh ag}\right] \tag{16}$$

In region 2 (air gap)

$$H_{X2}=0.4\pi I_{on}\times\left[\frac{\sinh a(y-g)-\frac{b_1}{a\mu_1}\cosh a(y-g)}{\sinh ag+\frac{b_1}{a\mu_1}\cosh ag}\right] \tag{17}$$

$$H_{y2}=-j0.4\pi I_{on}\times\left[\frac{\cosh a(y-g)\ \frac{b_1}{a\mu_1}\cosh a(y-g)}{\sinh ag+\frac{b_1}{a\mu_1}\cosh ag}\right] \tag{18}$$

The Equations for Loss

Equation 14 may be written

$$\text{Loss}=\frac{I_{on}^2}{2}\text{(area of inside stator surface)}\ 4\pi 10^{-9}lfR \tag{19}$$

The loss is in watts if other units are in the centimeter-gram-second system. R is defined by

$$R=\text{real part of } j\left[\frac{1+\frac{b_1}{a\mu_1}\tanh ag}{\tanh ag+\frac{b_1}{a\mu_1}}\right] \tag{20}$$

To obtain a more suitable expression for R, let

$$q_i=\text{imaginary part of }\frac{b_1}{a\mu_1}$$

$$q_r=\text{real part of }\frac{b_1}{a\mu_1}$$

$$\frac{b_1}{a\mu_1}=\sqrt{\frac{1}{\mu_1^2}+j\frac{l^2\omega 10^{-9}}{\pi\rho_1\mu_1}} \tag{21}$$

A combination of equations 20 and 21 yields

$$R=\frac{q_i(1-\tanh^2 ag)}{(\tanh ag+q_r)^2+q_i^2} \tag{22}$$

The preceding expression for R is general and holds for all conditions under the general assumptions of the analysis. Under most conditions encountered in the determination of pole-face losses, an examination of equations 21 shows that $q_r=q_i$. Under these conditions it is practicable to make a plot of R against a factor proportional to $l^2\omega/\rho\mu$. It is assumed that μ_1 is relatively large.

A plot of R, subject to the conditions stated in the preceding paragraph, can be made, similar to that illustrated in Figure 2. Subscripts are omitted, but it is to be understood that l is the wave length of the harmonic under consideration, and f is the frequency of the eddy currents generated in the rotor surface.

It will be noticed in reviewing Figure 2, that lines of constant g/l have maxima within the region plotted. In many of the calculations made by the writer, it has been found that the parameters are so related that the factor R is found at or near the peaks of the constant g/l contours.

This fact permits a further simplification. The values of the maxima may be plotted against g/l or an equivalent parameter. This has been done in Figure 3. The actual parameter chosen is $\pi(g/D)(P/2)$.

It is evident that Figure 3 shows the maximum possible value of the factor R for any harmonic. As mentioned previously, it has been found that the factor R falls at or near these maxima for many cases encountered in turbine generators of usual design. Departures occur at very low harmonics (the upper part of the curve) and at very high harmonics (below the range of the plot).

In applying equation 19 to the determination of loss attributable to the harmonic magnetomotive forces produced by currents of fundamental frequency in the armature, it is convenient to write the equation in terms of units more commonly used in design. The final equation results from application to equation 19 of the equations below and a departure from the centimeter-gram-second system.

$$\frac{A_n}{A_1}=\frac{1}{n}\frac{k_{pn}}{k_{p1}}\frac{k_{dn}}{k_{d1}}$$

$$A_n=\frac{\lambda}{8}I_{on}=\frac{\pi}{4}\frac{D}{pn}I_{on}\text{ (consistent units)} \quad (23)$$

The resulting equation may be written:

$$\text{Loss (kw)}=\left(\frac{A_1}{10{,}000}\right)^2\times$$
$$\left(\frac{k_{pn}k_{dn}}{k_{p1}k_{d1}}\right)^2\left(\frac{P}{2}\right)\left(\frac{L}{10}\right)\left(\frac{f_r}{n}\right)R \quad (24)$$

where

A_1 = square-wave value of fundamental armature mmf
= $\pi/4$ × peak value of fundamental armature mmf
$k_{pn}k_{dn}$ = pitch and distribution factors for nth harmonic of mmf, <1
P = number of poles
L = rotor length, inches
f_r = frequency of currents generated in the rotor attributable to nth mmf harmonic
R = factor from Figure 2 or Figure 3 or equation 22

In the preceding equation a "winding factor" is not included; for example, the fact that a symmetrical three-phase winding with symmetrical phase current does not allow the presence of a third harmonic magnetomotive force in the air gap is considered to be so well known that a factor to account for this phenomenon is not included in the aforementioned equation.

Magnetic Intensity and Permeability

From a consideration of the equations for loss and of Figure 2 it is evident that, for some calculations of loss, the loss may vary either directly or inversely as the square root of the rotor permeability. Since the permeability is a function of the magnetic intensity, a simple method of determining this factor is desirable.

As an index from which a value of rotor permeability can be estimated, it has been considered to be sufficient to determine the maximum value of H_x at the rotor surface. For almost all cases, this component of magnetic intensity is considerably larger than H_y, and it indicates reasonably well the degree of saturation of the surface.

Thus the present problem may be reduced to the determination of the absolute value of equation 15. Similar to the development of the equation for the resistance factor R, equation 22, we can define a magnetic intensity factor, S, such that

$$\text{Maximum value of } H_x|_{y=g}=I_{on}S \quad (25)$$

where

$$S=\frac{0.4\pi}{\cosh ag}\left[\frac{\sqrt{q_i^2+q_r^2}}{(\tanh ag+q_r)^2+q_i^2}\right] \quad (26)$$

This factor may be plotted together with R in Figure 2 as shown, placing the same limitation of the relation between q_i and q_r as is used in the plot of R.

The use of the preceding equations is illustrated in Appendix C.

Appendix B

Determination of the Harmonic Magnetomotive Forces

The harmonic components of the armature magnetomotive force may be said to depend on the coil pitch factor, the distribution factor, the relationship of the coils in the several phase groups, and the currents in the armature.

The Pitch Factor, K_{pn}

If the current in a single coil is assumed to flow in filaments on the stator surface, the distribution of the radial component of magnetomotive force attributable to the current in the single coil may be represented by the solid line in Figure 4.

It is readily established that the coefficients of the harmonic terms of Fourier Series representing this solid line are

$$C_n=\frac{1}{\pi}\int_{-\pi}^{\pi}f(\theta)\cos n\theta d\theta$$
$$=\frac{2}{n\pi}\left[\sin\frac{np\pi}{2}\right]=\frac{2}{n\pi}k_{pn} \quad (27)$$

The pitch factor, k_{pn}, is defined by the factor in the brackets.

It appears that for the lower harmonics the assumption of filamentary currents probably causes little error. For the higher harmonics this may not be true, and greater accuracy may be obtained by assuming that the current flows in a ribbon on the stator surface. If this ribbon has width, b, electrical angle, the magnetomotive force distribution may be represented by the dotted variation of the full line of Figure 4. Similar

Table I. $(K_{pn}K_{dn})/(K_{p1}K_{d1})$ for Pitch and Distribution Shown

42 Slots Per Pair of Poles

Definitions of Symbols in Appendix B

	Pitch	18/21	17/21	16/21	15/21	14/21	13/21	12/21	11/21	10/21
Distribution (7) (7)										
$K_{p1}K_{d1}$	1	0.930	0.911	0.888	0.859	0.826	0.788	0.744	0.700	0.652
$\frac{K_{pn}K_{dn}}{K_pK_{d1}}$	1	1	1	1	1	1	1	1	1	1
	3	0.540	0.440	0.315	0.167	0	0.182	0.376	0.573	0.774
	5	0.091	0.016	0.064	0.141	0.204	0.244	0.255	0.230	0.188
	7	0	0.078	0.139	0.166	0.150	0.091	0	0.102	0.190
	9	0.108	0.226	0.251	0.166	0	0.182	0.300	0.295	0.158
	11	0.081	0.105	0.061	0.026	0.102	0.117	0.056	0.052	0.138
	13	0.091	0.069	0.014	0.091	0.091	0.008	0.091	0.118	0.037
	15	0.166	0.039	0.139	0.166	0	0.181	0.167	0.051	0.232
	17	0.063	0.030	0.084	0.020	0.078	0.069	0.043	0.105	0.0159
	19	0.034	0.066	0.055	0.052	0.076	0.034	0.095	0.007	0.111
	21	0	0.157	0	0.167	0	0.182	0	0.209	0
Distribution (1′-5′-1) (1′-5′-1)										
$K_{p1}K_{d1}$	1	0.912	0.893	0.870	0.842	0.810	0.772	0.729	0.686	0.636
$\frac{K_{pn}K_{dn}}{K_{p1}K_{d1}}$	1	1	1	1	1	1	1	1	1	1
	3	0.441	0.359	0.258	0.136	0.008	0.149	0.307	0.467	0.634
	5	0.044	0.007	0.031	0.068	0.098	0.117	0.122	0.110	0.081
	7	0	0	0	0	0	0	0	0	0
	9	0.026	0.055	0.061	0.040	0	0.044	0.073	0.072	0.037
	11	0.095	0.123	0.072	0.031	0.119	0.137	0.065	0.061	0.162
	13	0.159	0.122	0.024	0.160	0.159	0.013	0.160	0.208	0.066
	15	0.383	0.090	0.320	0.383	0	0.417	0.385	0.117	0.536
	17	0.170	0.083	0.227	0.053	0.212	0.187	0.117	0.285	0.043
	19	0.102	0.197	0.166	0.154	0.227	0.103	0.283	0.022	0.333
	21	0	0.469	0	0.509	0	0.555	0	0.625	0
Distribution (3-3) (3-3)										
$K_{p1}K_{d1}$	1	0.920	0.902	0.878	0.850	0.818	0.780	0.736	0.692	0.642
$\frac{K_{pn}K_{dn}}{K_{p1}K_{d1}}$	1	1	1	1	1	1	1	1	1	1
	3	0.497	0.404	0.290	0.154	0	0.168	0.346	0.527	0.715
	5	0.029	0.005	0.020	0.044	0.064	0.077	0.080	0.072	0.053
	7	0	0.185	0.329	0.392	0.353	0.214	0	0.241	0.450
	9	0.207	0.434	0.483	0.318	0	0.348	0.576	0.567	0.295
	11	0.236	0.305	0.178	0.077	0.294	0.340	0.162	0.150	0.403
	13	0.071	0.054	0.011	0.071	0.071	0.006	0.071	0.092	0.029
	15	0.020	0.005	0.017	0.020	0	0.022	0.020	0.006	0.028
	17	0.068	0.033	0.091	0.021	0.084	0.075	0.046	0.114	0.017
	19	0.120	0.232	0.196	0.182	0.267	0.121	0.334	0.025	0.392
	21	0	0.369	0	0.392	0	0.427	0	0.481	0
Distribution (6-) (6-)										
$K_{p1}K_{d1}$	1	0.942	0.923	0.898	0.870	0.837	0.798	0.754	0.708	0.657
$\frac{K_{pn}K_{dn}}{K_{p1}K_{d1}}$	1	1	1	1	1	1	1	1	1	
	3	0.606	0.493	0.354	0.1877	0	0.204	0.423	0.642	0.872
	5	0.164	0.028	0.116	0.2538	0.367	0.438	0.459	0.414	0.303
	7	0	0	0	0	0	0	0	0	0
	9	0.097	0.203	0.226	0.1490	0	0.163	0.270	0.266	0.138
	11	0.183	0.237	0.138	0.0594	0.229	0.264	0.126	0.117	0.313
	13	0.091	0.069	0.014	0.0908	0.091	0.008	0.091	0.118	0.038
	15	0.083	0.020	0.069	0.0831	0	0.091	0.084	0.025	0.116
	17	0.141	0.069	0.189	0.04398	0.176	0.156	0.097	0.237	0.036
	19	0.061	0.118	0.100	0.0929	0.136	0.062	0.170	0.013	0.200
	21	0	0	0	0	0	0	0	0	0

(Table I continued on next page.)

Table I (Continued). $(K_{pn}K_{dn})/(K_{p1}K_{d1})$ for Pitch and Distribution Shown

	Pitch	18/21	17/21	16/21	15/21	14/21	13/21	12/22	11/21	10/21
Distribution (7) (3-3)										
$K_{p1}K_{d1}$	1	0.926	0.907	0.884	0.855	0.823	0.785	0.741	0.696	0.646
$\frac{K_{pn}K_{dn}}{K_{p1}K_{d1}}$	1	1	1	1	1	1	1	1	1	1
	2	0.036	0.048	0.059	0.070	0.081	0.091	0.101	0.110	0.119
	3	0.520	0.423	0.304	0.161	0	0.175	0.362	0.551	0.748
	4	0.065	0.079	0.087	0.088	0.081	0.067	0.045	0.016	0.018
	5	0.062	0.011	0.044	0.097	0.140	0.167	0.175	0.157	0.115
	6	0.081	0.083	0.068	0.039	0	0.043	0.0815	0.108	0.116
	7	0	0.127	0.226	0.270	0.243	0.147	0	0.166	0.310
	9	0.153	0.321	0.358	0.236	0	0.258	0.426	0.420	0.218
	11	0.150	0.193	0.113	0.049	0.187	0.216	0.103	0.095	0.255
	13	0.017	0.013	0.008	0.017	0.017	0.001	0.017	0.022	0.007
	15	0.081	0.019	0.068	0.081	0	0.088	0.081	0.025	0.113
	17	0	0	0	0	0	0	0	0	0
	19	0.073	0.142	0.120	0.112	0.163	0.074	0.204	0.016	0.240
	21	0	0.255	0	0.270	0	0.294	0	0.332	0
Distribution (7) (6-)										
$K_{p1}K_{d1}$	1	0.934	0.915	0.891	0.862	0.830	0.791	0.747	0.702	0.652
$\frac{K_{pn}K_{dn}}{K_{p1}K_{d1}}$	1	1	1	1	1	1	1	1	1	1
	2	0.036	0.047	0.059	0.070	0.080	0.090	0.100	0.110	0.118
	3	0.568	0.462	0.332	0.176	0	0.192	0.396	0.602	0.816
	4	0.064	0.078	0.086	0.087	0.080	0.066	0.044	0.016	0.018
	5	0.123	0.021	0.087	0.190	0.275	0.328	0.344	0.310	0.227
	6	0.080	0.082	0.066	0.039	0	0.042	0.081	0.107	0.115
	7	0	0.042	0.075	0.089	0.080	0.049	0	0.055	0.102
	9	0.111	0.233	0.259	0.171	0	0.187	0.309	0.304	0.158
	11	0.134	0.173	0.101	0.042	0.167	0.193	0.092	0.085	0.228
	13	0.013	0.010	0.002	0.013	0.013	0.001	0.013	0.017	0.005
	15	0.121	0.028	0.101	0.121	0	0.132	0.122	0.037	0.169
	17	0.088	0.043	0.118	0.027	0.109	0.097	0.060	0.147	0.022
	19	0.030	0.057	0.048	0.045	0.066	0.030	0.082	0.006	0.097
	21	0	0.084	0	0.089	0	0.097	0	0.110	0
Distribution (6′1)(1′6)										
		0.921	0.902	0.878	0.849	0.818	0.780	0.737	0.692	0.642
$K_{p1}K_{d1}$	1	1.0	1.0	1.0	1.0	1.0	1.0	1.0	1.0	1.0
$\frac{K_{pn}K_{dn}}{K_{p1}K_{d1}}$	2	0.011	0.013	0.017	0.020	0.023	0.026	0.028	0.032	0.034
	3	0.493	0.402	0.288	0.150	0	0.167	0.343	0.524	0.710
	4	0.036	0.043	0.048	0.048	0.044	0.037	0.024	0.009	0.009
	5	0.067	0.011	0.048	0.105	0.152	0.181	0.189	0.170	0.125
	6	0.131	0.134	0.112	0.064	0	0.069	0.132	0.175	0.189
	7	0	0.039	0.071	0.084	0.076	0.045	0	0.051	0.097
	9	0.024	0.050	0.056	0.036	0	0.040	0.067	0.065	0.034
	11	0.005	0.008	0.005	0.002	0.007	0.009	0.004	0.004	0.011
	13	0.034	0.026	0.005	0.034	0.034	0.003	0.034	0.045	0.014
	15	0.106	0.026	0.089	0.106	0	0.115	0.107	0.033	0.148
	17	0.052	0.026	0.071	0.016	0.066	0.058	0.037	0.088	0.014
	19	0.033	0.063	0.054	0.049	0.073	0.033	0.091	0.006	0.011
	21	0	0.159	0	0.168	0	0.183	0	0.208	0

to equation 27 the coefficients of the Fourier series describing this distribution are

$$C_n=\frac{2}{n\pi}\left[\sin\frac{n\pi P}{2}\right]\left[\frac{2}{bn}\sin\frac{nb}{2}\right]$$
$$=\frac{2}{n\pi}k_{pn}(\text{adjustment factor}) \qquad (28)$$

Thus for the higher harmonics the pitch factor may be modified by the "adjustment factor" as an approximate means of compensating for some of the errors in the simpler method of determining the pitch factor.

For the condition that the width, b, of the current-carrying ribbon is one half of the slot pitch, a few values of the aforementioned adjustment factor, (values of $[2/bn]\sin[bn/2]$), are tabulated here:

Harmonic	Slots Per Pair of Poles		
	36	48	66
1	1.0	1.0	1.0
5	0.992	0.995	0.997
11	0.961	0.977	0.988
17	0.91	0.95	0.97
25	0.81	0.89	0.94
35	0.65	0.79	0.89

Since the factors in the preceding tabulation are of interest only where a large proportion of the loss arises from the higher harmonics, and because it is an approximate correction to the usual method of determining pitch factor, it is not included in the tabulations of pitch and distribution factors.

The Distribution Factor k_{dn}

By means of the pitch factor, the value of the nth harmonic magnetomotive force because of the current in any one coil may be determined. By means of the distribution factor, the resultant magnetomotive force of a number of coils is found. Equations are developed in the succeeding paragraphs which cover most of the cases encountered.

In Figure 5 the unit magnetomotive force vectors of each of a group of uniformly spaced coils are shown within the circle which they define.

The radius of the circle, r, may be written in terms of the electrical slot angle, β; the number of the harmonic, n; and the number of coils in the group C.

$$r={}^1/_2\left(\frac{1}{\cos\frac{\pi-n\beta}{2}}\right)$$

The length of the resultant vector, divided by the numerical sum of the individual vectors, is the distribution factor

$$k_{dn}=\frac{1}{C}\left(\frac{\cos\frac{\pi-cn\beta}{2}}{\cos\frac{\pi-n\beta}{2}}\right)=\frac{1}{C}\left(\frac{\sin\frac{cn\beta}{2}}{\sin\frac{n\beta}{2}}\right) \qquad (29)$$

If the center of the coil group is taken as the point of reference for the group, the resultant vector is either directly in phase or out of phase with the central coil (with an even number of coils the central coil is hypothetical). Because it is sometimes necessary to obtain the resultant of several dissimilar groups of coils, it is necessary to determine the sign of this resultant, which is the sign of equation 29.

It is evident from the foregoing paragraph that in the determination of the net distribution factor for any number of groups of coils connected in series, a graphical solution is readily made. It is also practicable to make an analytical solution as is shown in the following paragraphs.

Consider m groups of coils connected in series, each group having a given number of coils ($C_{\mathrm{I}}, C_{\mathrm{II}}, \ldots C_m$) and a distribution factor ($k_{d\mathrm{I}n}, k_{d\mathrm{II}n}, \ldots k_{dmn}$). The angles, γ, between the center lines of the groups are known; for the harmonic, n, these angles are: $n\gamma_{(\mathrm{I-II})}$, $n\gamma_{(\mathrm{I-III})}$, and so forth.

If the angle of the resultant of the first group is considered to be the origin, it may be shown that the net distribution factor is

$$k_{dn}=\frac{1}{c_{\mathrm{I}}+c_{\mathrm{II}}+\ldots+c_m}\times\sqrt{\begin{bmatrix}c_{\mathrm{I}}k_{d\mathrm{I}n}\pm\\ \pm c_{\mathrm{II}}k_{d\mathrm{II}n}\cos n\gamma_{(\mathrm{I-II})}\\ \ldots\ldots\ldots\ldots\\ \pm c_m k_{dmn}\cos n\gamma_{(\mathrm{I}-m)}\end{bmatrix}^2+\begin{bmatrix}\pm c_{\mathrm{II}}k_{d\mathrm{II}n}\sin n\gamma_{(\mathrm{I-II})}\\ \ldots\ldots\ldots\ldots\\ \pm c_m k_{dmn}\sin n\gamma_{(\mathrm{I}-m)}\end{bmatrix}^2} \qquad (30)$$

where the plus sign is used for connection in a direction the same as the first group, and the minus sign for connection in the opposite direction. The sign of the distri-

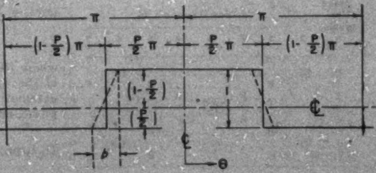
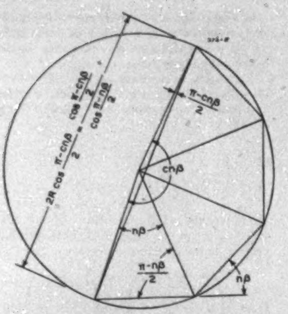


Figure 4. Determination of pitch factor

See Appendix B



$2R\cos\frac{\pi - cn\beta}{2}$
$\frac{\cos\frac{\pi-cn\beta}{2}}{\cos\frac{\pi-n\beta}{2}}$
$\frac{\pi-cn\beta}{2}$
$cn\beta$
$n\beta$
$\frac{\pi-n\beta}{2}$
$n\beta$

Figure 5. Determination of distribution factor

See Appendix B

bution factor for each group as found in the previous section must be carried into equation 30.

For two identical groups equation 30 reduces to

$k_{dn} = k_{dI_n} \cos {}^1/_2 n\gamma_{(I-II)}$ for connection in the same direction

$k_{dn} = k_{dI_n} \sin {}^1/_2 n\gamma_{(I-II)}$ for connection in the opposite direction (31)

For any two groups,

$$k_{dn} = \frac{1}{c_I + c_{II}} \sqrt{c_{II}^2 k_{dII_n}^2 + c_I^2 k_{dI_n}^2 \pm 2c_I k_{dI_n} c_{II} k_{dII_n} \cos n\gamma_{(I-II)}} \quad (32)$$

where the sign convention is the same as that stated previously.

Table II A. Magnetomotive-Force Harmonics, Three-Phase Machine

F Is Forward Moving Wave; B Backward

Space Harmonic, n	Current Harmonic, m 0	1	2	3	4	5	6	7
0	BF			BF			BF	
1		F	B		F	B		F
2		B	F		B	F		B
3	BF			BF			BF	
4		F	B		F	B		F
5		B	F		B	F		
6	BF			BF			BF	
7		F	B		F	B		F

Table II B. Magnetomotive-Force Harmonics, Six-Phase Machine

F Is Forward Moving Wave; B Backward

Space Harmonic, n	Current Harmonic, m 0	1	2	3	4	5	6	7
0	BF						BF	
1		F				B		F
2			F		B			
3				BF				
4			B		F			
5		B				F		
6	BF						BF	
7		F				B		F

Table II C. Magnetomotive-Force Harmonics, Two-Phase (90-Degree) Machine

F Is Forward Moving Wave; B Backward

Space Harmonic, n	Current Harmonic, m 0	1	2	3	4	5	6	7
0	BF	0.707BF	0	0.707BF	BF	0.707BF	0	0.707BF
1	0.707BF	F	0.707BF	B	0.707BF	F	0.707BF	B
2	0	0.707BF	BF	0.707BF	0	0.707BF	B	0.707BF
3	0.707BF	B	0.707BF	F	0.707BF	B	0.707BF	F
4	BF	0.707BF	0	0.707BF	BF	0.707BF	0	0.707BF
5	0.707BF	F	0.707BF	B	0.707BF	F	0.707BF	B
6	0	0.707BF	BF	0.707BF	0	0.707BF	BF	0.707BF
7	0.707BF	B	0.707BF	F	0.707BF	B	0.707BF	F

In Table I are given the values of the ratio $(k_{pn}k_{dn})/(k_{p1}k_{d1})$ for several values of pitch for each of several distributions for machines with 42 slots per pair of poles. The tabulation is presented to permit a few generalizations concerning the effect of various winding arrangements on the pole-face loss. The shorthand notation for the distribution is explained by the following examples: (7) is seven coils connected in series in consecutive slots; (7) (6-) is the same as (7) under one pole, with six in series and one slot open under the opposite pole connected in series in the opposite direction to the (7); (1-5-1) represents interspersion of the two end coils of a phase group with the end coils of adjacent phase groups.

The Phase Relationships

Where the currents in each of the phases of a multiphase system are equal and symmetrically displaced with respect to the system, the currents in each of the phases may be represented by

$$i_1 = \sum I_m \cos m(\omega t)$$

$$i_2 = \sum I_m \cos m\left(\omega t - \frac{2\pi}{Q}\right)$$

$$\cdots\cdots\cdots\cdots\cdots\cdots \quad (33)$$

$$i_q = \sum I_m \cos m\left(\omega t - \frac{q-1}{Q} 2\pi\right)$$

where i_q is the current in the qth phase, Q is the total number of phases, and I_m is the value of the current of harmonic order, m.

Where these currents flow in the armature of a machine, symmetrically wound with respect to the phases, the magnetomotive force acting on the air gap will be:

$$\text{mmf}_1 = \sum_{n=1}^{n=\infty} \sum_{m=1}^{m=\infty} i_1 D_n \cos n\theta$$

$$\text{mmf}_2 = \sum_{n=0}^{n=\infty} \sum_{m=0}^{m=\infty} \frac{i_2}{i} D_n \cos n\left(\theta - \frac{2\pi}{Q}\right)$$

$$\cdots\cdots\cdots\cdots\cdots\cdots$$

$$\text{mmf}_q = \sum_{n=0}^{n=\infty} \sum_{m=0}^{m=\infty} i_q D_n \cos n\left(\theta - \frac{q-1}{Q} 2\pi\right) \quad (34)$$

where the subscript, n, indicates the order of the space harmonic, mmf_q is the magnetomotive force produced by the qth phase, and D_n may be defined by

$$D_n \text{ is proportional to } \frac{k_{pn}k_{dn}}{n} \quad (35)$$

The equations 34 may be written in a more convenient form, as follows:

$$\text{mmf}_1 = \sum_{n=1}^{n=\infty} \sum_{m=1}^{m=\infty} \frac{I_m D_n}{2} \times \cos[(m\omega t + n\theta) + (m\omega t - n\theta)]$$

$$\text{mmf}_2 = \sum_{n=1}^{n=\infty} \sum_{m=1}^{m=\infty} \frac{I_m D_n}{2} \times \cos\left(m\omega t - \frac{m2\pi}{Q} + n\theta - \frac{n2\pi}{Q}\right) + \cos\left(m\omega t - \frac{m2\pi}{Q} - n\theta + \frac{n2\pi}{Q}\right)$$

$$\cdots\cdots\cdots\cdots\cdots\cdots$$

$$\text{mmf}_q = \sum_{n=1}^{n=\infty} \sum_{m=1}^{m=\infty} \frac{I_m D_n}{2} \times \cos\left(m\omega t - \frac{q-1}{Q} m2\pi + n\theta - \frac{q-1}{Q} n2\pi\right) + \cos\left(m\omega t - \frac{q-1}{Q} m2\pi - n\theta + \frac{q-1}{Q} n2\pi\right) \quad (36)$$

A study of equation 36 shows that a forward (the same direction as the fundamental) revolving wave of magnetomotive force exists where

$$m - n = 0, \pm Q, \pm 2Q \text{ and so forth} \quad (37)$$

and a backward revolving magnetomotive force wave exists when

$$m + n = 0, \pm 2Q, \text{ and so forth} \quad (38)$$

The magnitudes of these revolving waves is

$$(Q/2)I_m D_n \quad (39)$$

The following ratio may therefore be written

$$\frac{\text{Magnitude of any mmf wave}}{\text{Magnitude of fundamental mmf wave}} = \frac{I_m}{I_1} \frac{1}{n} \frac{k_{pn}k_{dn}}{k_{d_1}k_{d_1}} \quad (40)$$

If the rotor is revolving at the speed of the fundamental, the speed of the traveling wave with respect to the rotor is

$$\left(\frac{m}{n} \pm 1\right)\omega \text{ radians per second} \quad (41)$$

where the plus sign is used with a backward revolving wave, and the minus sign is used with a forward revolving wave. The forward direction is the direction of rotor rotation.

The frequency of the currents generated in the rotor surface by these traveling

Table II D. Magnetomotive-Force Harmonics, Four-Phase Machine

F Is Forward Moving Wave; B Backward

Space Harmonic, n	Current Harmonic, m 0	1	2	3	4	5	6	7
0	BF				BF			
1		F		B		F		B
2			BF				BF	
3		B		F		B		F
4	BF				BF			
5		F		B		F		B
6			BF				BF	
7		B		F		B		F

magnetomotive force waves may be written,

$$\frac{\omega}{2\pi}\left(\frac{m}{n}\pm 1\right)n \qquad (42)$$

with the same sign covention as in equation 41.

In Tables II are shown the magnetomotive force harmonics which can exist in three-, six-, two- (90°), and four-phase symmetrically wound machines. The two-phase machine does not fulfill the general conditions established for the preceding equations, and special rules must be applied. The exceptional case of the four-phase machine II-D is included for reference in reviewing the tabulation for the two-phase winding. Similarly, the six-phase machine can be considered to represent a three-phase machine with six symmetrically wound phase groups.

In Tables II the letter *F* indicates a magnetomotive force wave revolving forward, in the same direction as the fundamental; *B*, a backward wave. The coexistence of both is equivalent to a standing pulsating wave. All harmonics have a strength of

$$\frac{I_m}{I}\,\frac{k_{pn}k_{dn}}{k_{p1}k_{d1}}\,\frac{1}{n}$$

times the fundamental except where stated in the case of the two-phase machine.

In the cases usually encountered, the armature currents are substantially of single frequency, and only that part of the tabulations is applicable which refers to fundamental frequency currents. The equations for pole-face loss as written in Appendix A are then directly applicable.

Where harmonic currents are sufficiently large to cause appreciable loss, the loss equations are applicable by multiplying them by $(I_m/I_1)^2$ for those portions of the loss caused by the harmonic currents. In the case of the two-phase machines, a factor $(0.707)^2$ must also be included in the loss equations where it applies.

Appendix C

Sample Calculations and Tests

When the equations which have been written are used to calculate losses in actual machines, it is necessary to make allowances for departure from the idealized case used in the development of the equations.

The length of the air gap for the determination of pole-face losses is chosen in the calculations which follow as that used by designers in the determination of the relation between fundamental air gap magnetomotive force and flux density.

The slots in the rotor surface affect the permeability as used in the equations. The eddy current losses occur only in the skin of the rotor (except for very low frequencies); the magnetic fluxes which coexist with these eddy currents are directed primarily around the periphery of the machine. In the regions near the pole centers they lie wholly in solid iron; in the region of the slots they encounter wedges, which, if they are magnetic, are usually saturated by the main field current. For this reason the effective permeability of the rotor to these stray fluxes should probably never be assumed to exceed a value greater than several hundred times that of air.

As is evident in the examples considered below, where high pole-face losses occur, the stray fields themselves tend to saturate the rotor surface. Because the density of the eddy currents decreases rapidly and exponentially with depth below the rotor surface, a nonuniform permeability exists. When losses are caused by simultaneous action of magnetomotive forces of more than one frequency, the eddy currents of higher frequency, because they lie closer to the surface, are associated with a lower permeability. The device used below to treat, or rather to obviate this complex situation is to assume a permeability (if it is less than about 400) such that the flux density at the surface is 200,000 to 300,000 lines per square inch, about twice the saturation point of common steels, and to assume that losses attributable to the different harmonics can be calculated separately and added directly.

Zero-Sequence Connection

In this test a machine was operated at 1,800 and 3,600 revolutions per minute and with direct current applied to the armature winding connected open delta. Thus only the odd triple harmonics of armature magnetomotive force appear. From equations 36 and 23, the following relation is written.

$$I_{on}=\frac{A_{(\text{rated})}P}{D}\,\frac{4}{\pi}\,\frac{k_{pn}k_{dn}}{k_{p1}k_{d1}}\,\frac{I_{dc}\sqrt{2}}{I_{\text{rms(rated)}}} \qquad (43)$$

Data concerning the machine follow:

Inside stator diameter	20 inches
Active length	30 inches
Air gap	0.375 inch and increased to 0.500 inch
Effective gap	0.437 and 0.562
Rated rpm	3,600
Rated armature reaction, *A*	9,000 ampere turns per pole
Rated stator winding current	451 amperes
Phases, 3	36 coils in 36 slots
Pitch	11/18

$\frac{k_{pn}k_{dn}}{k_{p1}k_{d1}}$ for 3rd, 9th, 15th, and so forth, harmonics $=0.213$

For the calculation of losses gap diameter, *D*, is taken as 19.5 inches; $\rho=20\times10^{-6}$ ohm-centimeters.

For convenience in calculation, the following equations may be written for the specific problem from equations 43 and 19:

$$\text{Kw loss}=7.00\left(\frac{I_{on}}{100}\right)^2\left(\frac{\text{rpm}}{3{,}600}\right)\times\sum^{3,\,9,\,15,\text{ etc.}} R_n \qquad (44)$$

I_{on} (amperes per centimeter) $=0.308I_{dc}$

The factor I_{on} is the same for all triple *n* harmonics in this special case, which simplifies the computation.

The results are shown in Table III. In the calculation *R* is determined both from Figure 2 and by the simpler method of Figure 3. It should be noted that calculations based on the simpler method of determining *R* always show the highest value, as

Table III. Sample Calculation of Pole-Face Loss, Open-Delta Connection; See Appendix C

Method of Calculation	I_{dc} (Amperes)	Gap (Inches)	Revolutions Per Minute	I_{03} (Amperes Per Centimeter)	$\frac{G_{eff}}{l_3}$	μ	$\sqrt{\frac{f_3 l_3^2 10^{-9}}{\mu\rho}}$	R_3	R_9	R_{15}	S_3	B_3 Lines (Square Centimeters)	Calculated Pole-Face Loss (Kilowatts)	Test Load Loss (Kilowatts)
Figure 2	250	0.375	3,600	77	0.021	100	0.49	0.75	0.44	0.21	1.1	8,500	5.8	9.7
Figure 2	250	0.375	3,600	77	0.021	400	0.25	1.1	0.44	0.15	1.0	31,000	7.0	9.7
Figure 3	250	0.375	3,600	77	0.021			1.6	0.45	0.22			9.4	9.7
Figure 2	350	0.375	3,600	108	0.021	100	0.49	0.75	0.44	0.21	1.1	11,900	11.5	19.6
Figure 2	350	0.375	3,600	108	0.021	200	0.35	0.95	0.45	0.15	1.0		12.7	19.6
Figure 2	350	0.375	3,600	108	0.021	400	0.25	1.1	0.44	0.15	1.0	43,300	13.8	19.6
Figure 3	350	0.375	3,600	108	0.021			1.6	0.45	0.22			18.6	19.6
Figure 2	350	0.375	1,800	108	0.021	200	0.25	1.2	0.45	0.12	1.0	21,600	7.5	10.3
Figure 2	350	0.375	1,800	108	0.021	400	0.17	1.35	0.45	0.10	0.9	39,000	7.8	10.3
Figure 3	350	0.375	1,800	108	0.021			1.6	0.45	0.22			9.3	10.3
Figure 2	350	0.500	3,600	108	0.027	400	0.25	1.1	0.27	0.08	0.90	39,000	11.9	12.8
Figure 3	350	0.500	3,600	108	0.027			1.2	0.33	0.15			14.1	12.8

Table IV. Sample Calculation of Pole-Face Loss, Line-to-Line Short Circuit, See Appendix C

Revolutions Per Minute	Amperes	μ	$\sqrt{\frac{f_1^2 10^{-9}}{\mu\rho}}$	R	S	Calculated Kilowatts	Machine Lines Per Square Centimeters	Test Load Loss (Kilowatts)
3,600	750	50	2.04	0.24	1.25	4.1	4,900	16.6
3,600	750	200	1.02	0.47	1.25	8.0	19,500	16.6
3,600	750	400	0.725	0.65	1.25	11.0	39,000	16.6
3,600	1,250	50	2.04	0.24	1.25	11.4	8,200	35.5
3,600	1,250	200	1.02	0.47	1.25	22.0	33,000	35.5
3,600	1,250	400	0.725	0.65	1.25	30.5	65,000	35.5
2,000	1,250	50	1.52	0.31	1.25	8.2	8,200	23.5
2,000	1,250	200	0.75	0.63	1.25	15.6	33,000	23.5
2,000	1,250	400	0.54	0.83	1.20	22.0	64,000	23.5

Table V. Sample Calculation of Pole-Face Loss, Normal Machine, See Appendix C

n	K_p	K_d	$\left(\frac{k_{pn}k_{dn}}{k_{p1}k_{d1}}\right)^2$	$n\frac{q}{D}\frac{P}{2}$	R (Figure 3)	$\frac{f_{nr}}{nf_1}$	$\frac{f_{nr}}{nf_1}\left(\frac{k_{pn}k_{dn}}{k_{p1}k_{d1}}\right)^2 R$
1	0.866	0.958	1.0	0.037	—	—	—
2	0.866	0.077	0.0065	0.074	0.65	3/2	0.0063
4	0.866	0.077	0.0065	0.148	0.56	3/4	0.0027
5	0.866	0.264	0.0760	0.185	0.51	6/5	0.0465
7	0.866	0.077	0.0065	0.259	0.33	6/7	0.0018
11	0.866	0.160	0.028	0.407	0.165	12/11	0.0050
13	0.866	0.013	—	0.481	0.120	12/13	—
17	0.866	0.105	0.011	0.628	0.066	18/17	0.0008
19	0.866	0.063	0.0043	0.702	0.049	18/19	—
						Total	0.0631

Loss = total × $\left(\frac{A}{10{,}000}\right)^2\frac{L}{10}\frac{P}{2}f_0 = 0.0631\times 740 = 46$ kw

they should, and that the simpler method is in reasonable agreement with the more complex method for harmonics of higher order than the third and lower order than the fifteenth.

The test loss as shown is the total loss of the machine on short circuit, less the windage and friction and less the total copper loss as calculated. It therefore includes stray losses other than pole-face loss, and should be consistently larger than the calculated pole-face loss.

Single-Phase Short Circuit

When a machine is operated on single-phase short circuit, the armature reaction may be considered to have two fundamental components, one revolving in the same direction as the rotor and stationary with respect to the rotor, the other revolving in a direction opposite to the rotor and inducing in its surface eddy currents of twice armature current frequency. This second component produces the pole-face loss, the subject of calculation below; because the fundamental of armature magnetomotive force is so large compared with the harmonics, the losses attributable to these harmonic magnetomotive forces are neglected.

From equations 23 and a consideration of the development leading to equation 36, for a line-to-line short circuit

$$I_{01}=A_{(\text{rated})}\frac{P}{D}\frac{4}{\pi}\left(\frac{I\text{ actual}}{I\text{ rated}}\;\frac{1}{\sqrt{3}}\right) \qquad (45)$$

Pertinent data concerning the machine tested follow:

Inside stator diameter......24 inches
Active length..............$37^1/_2$ inches
Air gap...................0.687 inch; effective gap, 0.785 per cent
Rated rpm................3,600
Rated armature reaction, A. 10,140 ampere turns per pole
Rated stator winding current 2,170 amperes
Three-phase line-to-line short circuit on wye connection
For calculations, diameter = 23.25 inches, $\rho = 20\times 10^{-6}$ ohm-centimeters

From equations 45 and 14 the following equations may be written for calculation

$$\text{Kw loss}=145\left(\frac{I_{rms}}{2{,}170}\right)^2 R \qquad (46)$$

Maximum flux lines per square centimeter in

$$\text{rotor surface}=238\left(\frac{I_{rms}}{2{,}180}\right)S\mu$$

The simpler method of Figure 3 for determining R is not applicable; both R and S are determined from Figure 2.

The results of the calculations and tests are presented in Table IV. Here, again, the test loss is the so-called load loss less the stray loss in the copper; it therefore includes stray losses in addition to pole-face loss. Under the assumption, discussed elsewhere, that the permeability should not exceed a value which permits calculated flux densities in excess of about 250,000 lines per square inch (35,000 per square centimeter) in the skin of the rotor the calculations are seen to be consistent at about two thirds the value of the test loss. This difference may indicate that the method requires an empirical factor in the equations or that the other stray losses are large. In either case, it is gratifying to note that the results are of the correct order of magnitude, even for cases where the departures from the fundamental assumptions are great.

Normal Machine

For machines of normal design, the calculation is most readily made if equation 24 is written in the form

$$\text{Kw}=\sum^{n}\left[\left(\frac{A}{10{,}000}\right)^2\frac{L}{10}\frac{P}{2}f_1\right]\times\left[\left(\frac{f_{nr}}{nf_1}\right)\left(\frac{k_{pn}k_{dn}}{k_{p1}k_{d1}}\right)^2 R\right] \qquad (47)$$

where

f_1 = fundamental armature frequency
f_{nr} = time order of harmonic of current generated in rotor
R—from Figure 3

The items in the second bracket are those which vary with the harmonic.

For the machine under study:

A = 14,330 ampere turns per pole
Pitch = 14/21
Distribution, (7) (6-) one coil omitted at end of one group
Revolutions per minute, 3,600; air gap $^{11}/_{16}$ inch (mechanical); length 60 inches; rotor diameter, $27^1/_8$ inches; 2 poles.
The calculation of loss is made in Table V.

From the tabulation it is apparent that the major part of the loss is caused by the fifth harmonic and that the loss attributable to the very low order harmonics is small. Since the simpler method of Figure 3 is sufficiently accurate for harmonics greater than the third and less than the fifteenth, no attempt for greater accuracy by use of the method of Figure 2 would be fruitful.

From a perusal of Table I the reader will note that there is good possibility of reducing the pole-face loss by changing the armature winding. Calculation shows that a distribution (7) (3-3) would reduce the loss by about 30 per cent. An even greater reduction is possible by using a (6-) (6-) distribution (and a longer pitch—to maintain flux density substantially constant).

The tests on this machine (7) (6-) distribution, showed a total load loss of 45 kw. It has been found that usually the pole-face loss calculation is about 75 per cent of the tested load loss. That the calculated pole-face loss is so nearly equal to the total load loss may be attributed to testing and calculation errors and in part to a stator end structure specially designed for low load loss in that region.

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Application of Carrier to Power Lines

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DURING the 20 years or so that have elapsed since power-line carrier was first introduced on a commercial basis, enormous advances have been made in the communication arts. In the somewhat specialized branch of power-line carrier, advances in technique and apparatus design have not been as highly publicized as those in some other branches of the art; nevertheless, they have kept pace with the field, with the result that power-line carrier is today one of the most useful and versatile tools available to the power-utility engineer. There are now over 1,300 terminals of power-line carrier in operation in the United States. These terminals provide channels for telephone circuits, pilot relaying, telemetering, load-control, supervisory, and other remote-control functions over transmission lines of all voltages, totaling over 40,000 channel-miles. The last several years have seen a rapid increase in the number of terminals and in channel-miles, and there is every indication that this rate of growth will continue to increase for some time. With very few exceptions, every new transmission line of any importance is now engineered to include power-line carrier for one or more functions. Many large integrated systems[1] have already made such extensive use of carrier circuits that the problem of channel space and frequency assignment is rapidly becoming of major importance.

A considerable portion of the more recent increase in the use of power-line carrier is directly connected with the increased load the war production has placed on the power industry. High-speed protection, obtainable on long lines only through the use of carrier pilot relaying, increases the stability limit of transmission circuits so as to permit increased loading of existing lines. This factor, alone, provides large savings in copper and other transmission-line materials which, otherwise, would be required by new construction or by the use of double-circuit lines with slower relaying. In addition, the reliability and convenience of carrier telephone circuits for dispatching permit existing generating and substation equipment to be operated nearer maximum load and efficiency conditions. The increase in the number of system interconnections to pool generation and transmission facilities for new or increased war production loads has also resulted in a large increase in the use of carrier channels for telemetering and load control.

This growth in the use of power-line carrier has confronted the power-utility engineer with many new problems. To guide him in their solution, it is desirable to make available much of the application information which has been accumulated by those who have concentrated on this work since the initial installation of power-line carrier in 1921.

In answer to the growing demand for application information, it is the principal purpose of this paper to present a description of methods successfully used in calculating transmission losses and applying power-line-carrier equipment. These methods are largely based on experience accumulated over a period of some 20 years in applying carrier to all types of power-transmission lines. They are supported by a considerable amount of field and laboratory data and have been corrected and revised from time to time as new data and experience have indicated. In general, they are somewhat on the conservative side. However, the factors of safety incorporated have not burdened the equipment design, and, in numerous instances, have proved to be essential in maintaining a high degree of reliability under adverse conditions.

Since the behavior of high-frequency currents, when applied to power lines, is sufficiently complex and the difficulties of obtaining quantitative data are sufficiently great, we find that, even at this late date, the process of application is still more of an art than the exact science many would expect it to be. It is not intended, therefore, that the material presented herein be interpreted as the complete and final word on the subject. This paper is intended more as a guide for the power-utility engineer in the preliminary work of laying out carrier channels on a power-transmission system.

For the sake of simplicity, the subject matter is confined entirely to the problems of establishing the carrier circuits; the choice and application of the terminal equipment using these circuits is not covered.

The Problem

Basically, the problem of applying carrier to power lines is one of utilizing circuits that have been designed for the reliable and efficient transmission of power in terms of kilowatts, kilovolts, and hundreds of amperes at very low frequencies and adapting them for the transmission of power in terms of watts, volts, and milliamperes at frequencies of 50 kilocycles or higher. More specifically, for a given carrier channel, the problem resolves itself into one of establishing and maintaining a carrier-frequency path or circuit between two or more transmitting and receiving stations within certain limits of attenuation or transmission loss. The fundamental requirement imposed by the power system is that the means used to adapt the circuits for carrier channels shall not in any way interfere with the primary function of power transmission. At the same time, to attain the desired degree of reliability, the carrier channels established should be free from interference or interruptions caused by switching or normal operation of the power system and transient disturbances attributable to lightning or other causes. It is also essential that the carrier circuits shall be safe; that is, they should be adequately insulated and protected against normal and abnormal voltages and currents so as to impose no hazard to connected apparatus or to personnel using and maintaining the equipment.

One of the most important factors in the problem is the wide differential between power frequencies and frequencies normally used for the carrier circuits. Circuit elements and power apparatus which offer very low impedance paths to power frequencies may appear as very high impedances at carrier frequencies, and high impedances or open circuits for power frequencies may appear as low impedances at carrier frequencies. This phenomenon is largely responsible for the "gap-jumping" myth that has attached itself to power-line carrier. There are numerous instances of the carrier channel being maintained through what appeared to be gaps or breaks in the line, but a painstaking analysis of such cases would doubtless reveal a carrier-frequency path of some sort. Through this potentiality, carrier has rendered invaluable service in many serious emergencies, but this highly desirable faculty is unpredictable and can hardly be given practical treatment in application engineering.

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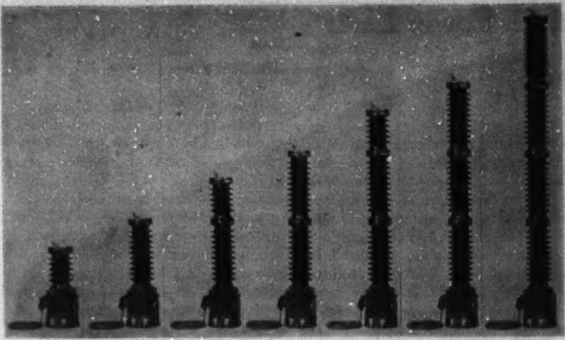


Figure 1. Carrier-current coupling capacitors, type CW

Typical base-mounted assemblies for 34.5 kv, 69 kv, 115 kv, 138 kv, 161 kv, 230 kv, and 287 kv, left to right

In laying out a carrier circuit, to obtain the reliability usually required, it is essential that all obstructions or possible breaks in the carrier path between the channel terminals be removed by definite provision of alternate paths or by-passes for the carrier frequency. Intervening transformers, autotransformers, current-limiting reactors, as well as circuit breakers or disconnect switches that may be opened, usually require by-passing. Low-impedance shunts at the carrier frequency such as some spur lines or parallel branches, cable circuits, and grounds applied when the circuit is de-energized for repairs, usually require trapping.

Another important point to keep in mind is that transmission at carrier frequencies along a transmission line, in common with transmission at lower frequencies, requires a complete circuit; that is, in terms of the simplest concept, there must be a "return path" as well as a "forward path." It is also just as important that this return path have low carrier-frequency loss as it is for the forward path. If one conductor of a transmission line is selected for one side of the circuit or the forward path, the other side of the circuit or the return path is usually provided by one or more of the other conductors, the overhead ground wire, the earth itself, or a combination of these. The selection of this return path and the provision made for its continuity may have considerable bearing on the efficiency of the carrier circuit. The frequent suggestion for the use of oil pipe lines or other buried pipe lines as carrier circuits is an illustration of neglecting this return-path requirement.

Table I. Standard Coupling-Capacitor Assemblies

Standard Capacitor Assemblies		Withstand Test Voltages (Based on Proposed AIEE Standard 31)		
			Low Frequency	
Maximum Rated Apparatus Voltage (Kilovolts)	Capacitance (Microfarads)	Impulse (Kilovolts)	Dry, 1 Minute (Kilovolts)	Wet, 10 Seconds (Kilovolts)
15	0.006	110	50	45
25	0.006	150	65	60
34.5	0.006	200	85	80
46	0.003	250	110	100
69	0.003	350	165	145
92	0.002	450	215	190
115	0.002	550	265	230
138	0.0015	650	320	275
161	0.0012	750	370	315
196	0.001	900	450	385
230	0.001	1,050	525	445
287	0.00075	1,300	655	555
345	0.0006	1,550	785	665

Fortunately, the devices or tools available and actually used in establishing carrier circuits on power lines are relatively simple and few in number. The physical and economic restrictions imposed by a high-voltage power system limit the devices that can be inserted directly in the power line to essentially only two: the coupling capacitor and the line trap. These two, together with line-tuning apparatus associated with the low-voltage side of the coupling capacitor, are the basic tools used. In order to apply these tools most effectively, it is important that their functions, characteristics, and limitations be thoroughly understood.

Coupling Capacitors

In some of the early concepts, power-line carrier was considered as a special case of radio transmission in which the power line was caused to guide the radio waves by locating the antennas associated with the transmitting and receiving equipment as close to the line as possible. The antennas so located were thought of as "coupling" the equipment to the line and were thus called "coupling wires." A great deal of experimental work was done in determining the most effective location and arrangement of these coupling wires, and from this work it soon became apparent that the capacitance between the coupling wires and the line conductors was the most important factor in obtaining the desired end. However, the inefficiency, hazard, and inconvenience of the coupling wire soon led to the development of a high-voltage capacitor for obtaining this capacitance more directly and efficiently, and it naturally followed that this device was called a "coupling capacitor."

After passing through numerous stages in development, covering a considerable range in dielectric material and physical shape, the coupling capacitor has attained the status of a standardized and highly dependable piece of equipment, universally accepted as the standard means for connecting carrier circuits to high-voltage lines. In its modern form, as illustrated by Figure 1, it consists of an assembly of one or more porcelain-shell oil-filled paper-dielectric capacitor units together with a base unit containing a grounding switch and protective equipment. The individual capacitor units are co-ordinated as to capacitance and voltage rating so that they may be connected in series to form assemblies for any standard circuit voltage. Table I shows the capacitances available for various standard apparatus voltage ratings together with test voltages which these assemblies will withstand.

The inclusion of the protective equipment in the capacitor base is now standard practice. This protective equipment usually consists of a gap and a power-frequency drainage coil. The drainage coil ordinarily consists of an inductor connecting the low-voltage side of the capacitor to ground. This coil which has an impedance of over 50,000 ohms at carrier frequencies and less than 100 ohms at power frequencies, serves to provide a path for the power-frequency current of 50 milliamperes or so through the capacitor to ground and thus maintain the lead to the carrier equipment at a normal power-frequency voltage of less than five volts. The gap, which is connected across the drainage coil, limits the voltage which can be built up on the carrier lead during transient voltage disturbances.

Frequently, the coupling capacitor is

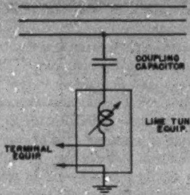


Figure 2. Simple single-frequency tuning

used with a capacitor potential device to provide potential for instruments and relays. The potential device usually forms the base for the capacitor assembly in these cases, replacing the capacitor base unit. A choke coil, having impedance characteristics similar to the drainage coil, is used between the capacitor and potential device to maintain a high shunt impedance for carrier frequencies on the carrier lead. The use of the potential device also inserts an auxiliary series capacitor in the carrier lead. However, since the capacitance of this auxiliary capacitor is large compared to that of the coupling capacitor, the slight reduction in total capacitance resulting from the use of the potential device can usually be neglected.

From the standpoint of the power-transmission system, it is essential that the insulation level of the capacitor assembly, in terms of impulse and wet and dry flashover withstand voltages, be co-ordinated with that of associated power-line apparatus. From the standpoint of the carrier circuit, it is desirable that the capacitance be as large as possible, consistent with an economic design, preferably at least 0.001 microfarad. Also, from the carrier-circuit standpoint, it is essential that the carrier lead brought out from the base of the capacitor assembly be well insulated so as to maintain low capacitance and high leakage resistance to ground between the capacitor and the line-tuning equipment. The variation in capacitance with temperature should be sufficiently small as to have negligible effect on the carrier-frequency circuit.

Line-Tuning Equipment

Associated with each coupling capacitor in its function of connecting carrier circuits into the power lines, certain auxiliary apparatus, generally known as line-tuning equipment, is usually required. This equipment, located in the circuit betwen the coupling capacitor and the carrier-terminal equipment, performs the primary function of tuning or resonating with the capacitive reactance of the coupling capacitor to provide a low-loss path for one or more carrier frequencies or bands of carrier frequencies between the terminal equipment and the power line. The combination of line-tuning equipment and coupling capacitor may be thought of as forming a filter of the band-pass type, passing or offering a low-loss path for the desired carrier frequencies or channels and rejecting or offering a high-loss path for the power frequency and undesired frequencies in the carrier band.

Line-tuning equipment may be very simple or fairly complex, depending on the number of carrier frequencies or channels to be passed through the coupling capacitor. The question of the number of frequencies or channels that can be handled through a single coupling capacitor is primarily one of balancing the cost of additional capacitors against the cost and the inconvenience of additional complexity in the line-tuning equipment. The most commonly used line-tuning equipment is available in standard units, having various arrangements of variable inductors and capacitors that can be combined to form series and parallel resonant circuits for one or more channels in the normal range of 50 to 150 kilocycles.

Single-Frequency Line-Tuning Equipment

In its simplest form, for a single frequency or channel, the line-tuning equipment may consist of a single variable inductor connected in series with the coupling capacitor to form a series-resonant circuit for the carrier frequency used. Such a circuit is shown schematically in Figure 2, where, for simplicity, a ground-return circuit is assumed. Since the portion of the circuit between the capacitor and the line-tuning inductor operates at highest carrier-frequency impedance (to ground), it is highly desirable that shunt capacitance or leakage to ground in this lead be kept at a minimum for maximum efficiency. For this reason it is usually desirable to locate the line-tuning equipment as close to the coupling capacitor as possible, in order to keep the length of this lead-in conductor to a minimum. However, by using a well-insulated conductor, supported by a minimum number of insulators, lead-in lengths of up to 100 feet or so may be used without excessive losses in this portion of the circuit.

Table II. Recommended Maximum Concentric-Cable Lengths When Outdoor Line Tuning Is Omitted

Size of Coupling Capacitor (Microfarads)	Recommended Maximum Cable Lengths in Feet From Coupling Capacitor to Terminal Equipment (Kilocycles)					
	1-Decibel Attenuation			2-Decibel Attenuation		
	50	85	150	50	85	150
Impedance Match Transformer Not Used						
0.00075	40	60	75	90	120	140
0.001	70	100	110	140	160	190
0.0012	110	120	130	200	200	210
0.0015	120	120	160	230	240	250
0.002	210	220	230	340	340	340
0.003	320	320	310	500	480	420
0.006	500	480	370	800	700	550
Impedance Match Transformer Used						
0.00075	150	180	200	300	350	410
0.001	200	230	250	390	430	600
0.0012	240	270	300	430	500	750
0.0015	260	300	340	500	600	800
0.002	360	390	460	680	800	1,000
0.003	450	450	475	900	1,000	1,000
0.006	800	800	800	1,000	1,000	1,000

For some years it has been standard practice to mount some types of carrier-terminal equipment outdoors near the coupling capacitor; in such cases the line-tuning equipment is included in the same cabinet with the terminal equipment. While this arrangement eliminates the losses in the circuit between the line-tuning and terminal equipments, and, in some cases, simplifies the installation somewhat, general experience has shown these advantages to be outweighed heavily by the inconvenience of maintaining the terminal equipment under extreme weather conditions. More recent practice is to install all terminal equipment indoors and to carry the circuit in from the line-tuning equipment through a special low-loss concentric cable. The transmission losses in this special cable, as will be noted in Table II, are sufficiently low as to permit this portion of the circuit to be carried several thousand feet without excessive loss. Since the concentric cable has a characteristic impedance of approximately 70 ohms and the line-tuning coupling-capacitor circuit an impedance in the order of 400 to 800 ohms, an impedance-matching transformer is usually used between the cable

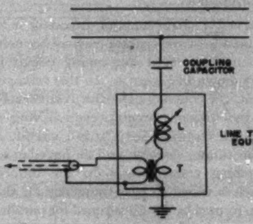


Figure 3. Single-frequency tuning with impedance-matching transformers

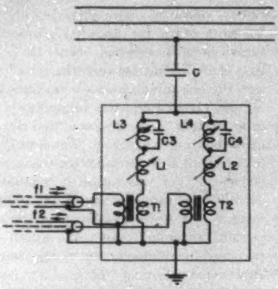


Figure 4. Two-frequency tuning of single coupling capacitor with separate lead-in cables

and the line-tuning equipment, as shown in Figure 3.

MULTIFREQUENCY LINE-TUNING EQUIPMENT

When a single coupling capacitor is to be used for two separate carrier channels, a commonly used line-tuning circuit is that shown in Figure 4. In this arrangement, the coupling capacitor is connected to two branch circuits, each having a parallel resonant, or "trap" circuit, a series tuning element, and an impedance-matching transformer. In the branch which is tuned to pass frequency $f1$, the trap formed by $L3$-$C3$ is tuned for parallel resonance at frequency $f2$ so as to offer a high impedance for $f2$. The series tuning element $L1$ is tuned for series resonance at $f1$ with the coupling capacitor C and the reactance which the trap $L3$-$C3$ presents at $f1$. Similarly, the other branch is tuned to pass frequency $f2$ with $L4$-$C4$ shunt tuned to $f1$.

By adding a third branch and an additional shunt-tuned trap in each branch, a single coupling capacitor can be used in common for three separate channels as shown in Figure 5. In this arrangement, $L6$-$C6$ and $L9$-$C9$ are tuned to $f1$, $L3$-$C3$ and $L8$-$C8$ are tuned to $f2$, and $L2$-$C2$ and $L5$-$C5$ are tuned to $f3$. The series elements $L1$, $L4$, and $L7$ may be simple variable inductors as indicated, or they may include additional series capacitors, depending on the frequencies involved, the residual series reactance of the trap circuits, and the capacitance of the coupling capacitor.

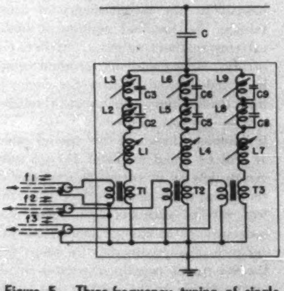


Figure 5. Three-frequency tuning of single coupling capacitor with separate lead-in cables

Because of the excessive losses introduced into each channel and the difficulty of adjustment in the field to obtain satisfactory band-pass characteristics for each channel, it has not been considered practical to extend the arrangements shown in Figures 4 and 5 beyond a maximum of three channels per coupling capacitor. When additional channels are to be coupled to the same line conductor, additional coupling capacitors are generally used.

Frequently, when tuned circuits or filters are included in the terminal equipment for separating the channels of a multichannel system, it is desirable to carry the several channels between the line-tuning equipment and the terminal equipment over a single concentric cable. A single coupling capacitor can be tuned to pass two frequencies through a single cable by using the line-tuning arrangement shown in Figure 6. In this circuit, the inductor $L1$ and the coupling capacitor C are tuned for series resonance at the upper frequency $F1$. $L2$ and $C2$ are also tuned for series resonance at $F1$. By means of the inductor $L3$, the entire circuit can be adjusted for series resonance at the lower frequency $F2$. While this arrangement can be extended to handle as many as three frequencies with standard line-tuning units, here again it is common practice to use additional coupling capacitors if more than three channels are involved.

When the several frequencies of a multichannel system have been selected definitely and are known in advance, it may be advantageous to use conventional band-pass filter circuits such as shown in Figure 7. To use this type of circuit it is necessary that the several channels be grouped together in one part of the frequency spectrum. The capacitance available in the coupling capacitor is an important factor in the maximum band width provided by this type of circuit, particularly at the lower end of the frequency spectrum, and in some cases two coupling capacitors in parallel may be required. While commercial designs of this type of line-tuning equipment are not generally available as yet, the rapid increase in the number of channels being applied indicates that their general use in the near future can be predicted safely.

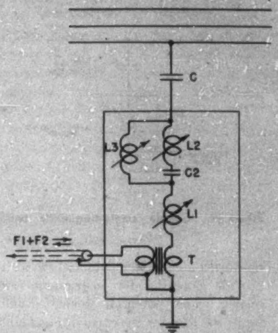


Figure 6. Two-frequency tuning of single capacitor with common lead-in cable

OMISSION OF OUTDOOR LINE-TUNING EQUIPMENT

In the interest of maximum theoretical efficiency, it is usually assumed that line-tuning circuits, such as have been described thus far, are adjusted and operated so that the reactance of the coupling capacitor and the reactive components presented by the transmission line itself are largely compensated or "tuned out," and that the concentric cable is terminated thus in a resistive load approximately equal to its characteristic impedance. While this maximum theoretical efficiency is generally desirable, there are many instances in which it is not essential. Carrier circuits, having total transmission losses well within the maximum range of the terminal equipment used, can frequently accommodate a considerable amount of loss in the line-tuning and lead-in circuit with no appreciable decrease in reliability. Some

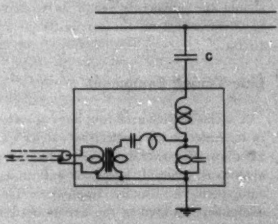


Figure 7. Band-pass tuning of single coupling capacitor

consideration may therefore be given to arrangements that strive for something appreciably less than maximum efficiency.

If the concentric-cable lead-in is brought into the base of the coupling capacitor and connected directly to the low side of the capacitor, without benefit of a line-tuning unit, as shown in Figure 8, there will be a considerable mismatch in impedance at the junction of the cable and capacitor. The resulting reflections or standing waves will cause the losses in the cable to exceed considerably those shown in Figure 22 for ideal termination conditions. However, under favorable conditions and depending on the size of the coupling capacitor and the carrier frequency involved, the cable may be several hundred feet in length and still not add over two decibels loss to the circuit. Compared with a total allowable attenuation varying from 30 to 60 or more decibels for various transmitter-receiver combinations, this loss is small and frequently can be allowed. If an impedance-matching transformer is added to this circuit, as shown in Figure 9, to step down the impedance of the capacitor and the line to a value numerically approximating the cable-characteristic impedance, the effect of the mismatch will be considerably reduced and a greater length of cable can be used with the same loss. Table II shows the lengths of cable that may be used with these two arrangements for attenuation figures of one and two decibels. Because of complications introduced in tuning, it is not recommended that these cable lengths be exceeded. With either of these arrangements, it should be noted that line-tuning equipment or its equivalent must be provided in the terminal apparatus or at the other end of the cable to handle the reactance components transmitted through the cable. However, since the impedance-matching transformer may be mounted in the base of the coupling capacitor, these arrangements do result in eliminating the outdoor-mounted line-tuning unit.

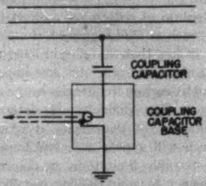


Figure 8. Coupling capacitor with concentric-cable lead-in brought directly into base

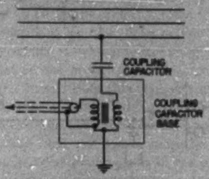


Figure 9. Coupling capacitor with impedance-matching transformer in base

BY-PASSES

In addition to their primary functions of connecting the carrier-terminal equipment to transmission lines, coupling capacitors and line-tuning equipment are also used extensively in maintaining carrier circuits around discontinuities such as transformers (including autotransformers) and open switches. This application is generally referred to as a by-pass.

Most of the problems encountered in applying by-passes are similar to those encountered at the terminals. If the coupling capacitors associated with the two sides of a simple by-pass are physically separated by a distance of over 100 feet, it is the usual practice to use a separate line-tuning unit with each capacitor to avoid excessive lengths in the leads from the coupling capacitors. Such an arrangement for a single frequency by-pass is shown in Figure 10. Here, it should be noted, the usual practice is to carry the connections between the two tuning units through a concentric cable. Where two frequencies are to be by-passed, the arrangement used in Figure 6 is used in place of each of the single-frequency units shown in Figure 10. Where more than two frequencies are to be by-passed, it is common practice to use additional coupling capacitors and line-tuning equipment. However, band-pass circuits such as shown in Figure 7 should be considered for three or more frequencies.

Frequently, in connection with party-line or system-broadcast types of carrier circuits, it is necessary to by-pass around possible open switches at the junction of several lines so that carrier circuits are maintained through more than two branches. Such an arrangement is the three-way by-pass shown in Figure 11. Here, it will be noted that the concentric cable leads from the three line-tuning units are simply paralleled. If a terminal station on the same channel is also located at the by-pass point, the connection into the circuit is made by paralleling the cable from the terminal equipment with the line-tuning units. In both of these cases there is an unavoidable impedance mismatch at the junction of these paralleled cables which introduces a small loss.

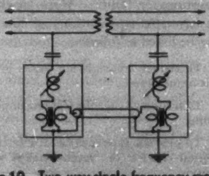


Figure 10. Two-way single-frequency ground-return by-pass

When the coupling capacitors associated with a two-way by-pass are physically separated by less than 100 feet, it is usually possible to simplify the by-pass arrangement considerably by including all of the line-tuning equipment in one cabinet, preferably located halfway between the two coupling capacitors. For a single frequency, the arrangement shown in Figure 12 is all that is required. For the higher carrier frequencies or for the higher values of coupling capacitance, a single variable inductor is often sufficient. The by-passing of two frequencies under the same condition involves the use of a two-frequency pass circuit similar to the one shown in Figure 6, omitting the impedance-matching transformer. For by-passing three or more frequencies through one tuning unit, a band-pass circuit such as Figure 13 may be considered.

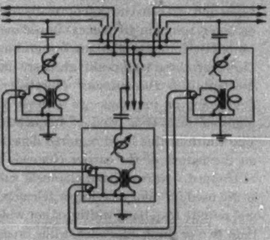


Figure 11. Three-way single-frequency ground-return by-pass

Line Traps

The third important tool used in applying carrier circuits to power lines is the line trap. In its simplest form, this device consists of an air-core reactor with a fixed or tapped capacitor connected in parallel to form a parallel resonant circuit. It is usually used in series with a conductor of the transmission circuit to provide a high impedance or "trap" for currents of the carrier frequency to which the resonant circuit is tuned. In this series position, the reactor of the trap must carry not only the normal line current but all abnormal or short-circuit currents that the line itself is called upon to carry. The conductor with which the reactor is wound must therefore be comparable in size or cross section with the line conductor, and the construction must be such as to withstand the mechanical stresses imposed by short-circuit currents. Its position in the line also exposes it to transient high-voltage disturbances attributable to lightning and switching, and adequate lightning arresters must be provided across both reactor and capacitor.

The impedance developed in the trap, at the resonant carrier frequency, is a function of the coil inductance and the carrier-frequency losses in the coil and its shunt capacitor. With capacitor types and coil construction normally used, peak or resonant-frequency impedances of well over 10,000 ohms are not unusual. However, these high peak values are only incidental to the primary objective of developing an impedance of at least 500 ohms or so over a band of frequencies. The width of this band required depends on the nature of the carrier channels to be trapped. Narrow-band channels, such as are used for pilot relaying or telemetering, require trap band widths of not wider than two or three kilocycles, while broad-band channels, carrying voice or audio-tone-modulated signals, require band widths of five or six kilocycles. Frequently, narrow-band channels can be located close enough together in the frequency spectrum to use a single broad-band trap to cover several channels.

The band width developed in the trap is largely a function of the total inductance available in the coil and for general use with both narrow-band and broad-band channels, a single-frequency trap requires a minimum coil inductance of approximately 150 microhenrys. If the total inductance available is in the order of 250 microhenrys, the coil may be double tuned to provide two high-impedance bands in the normal frequency range, each wide enough for a single broad-band channel. This two-frequency trap requires a more complex tuning arrangement, consisting of additional tapped capacitors and one or more auxiliary variable inductors. However, since the coil is the major item of cost in the line trap, this additional complexity is usually justified when two channels are to be trapped. Practical considerations of size, weight, and cost have limited largely the maximum inductance available in the line-trap reactor to approximately 250 microhenrys. This fact, together with the complexity and difficulty in adjustment involved, has made it impractical to tune a single trap coil to more than two frequency bands. Where it is necessary to trap more than two widely separated channels in a single line conductor, additional traps are generally used.

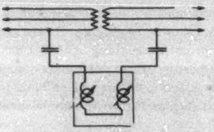


Figure 12. Simple single-frequency by-pass

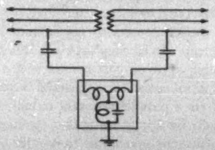


Figure 13. Band-pass by-pass circuit

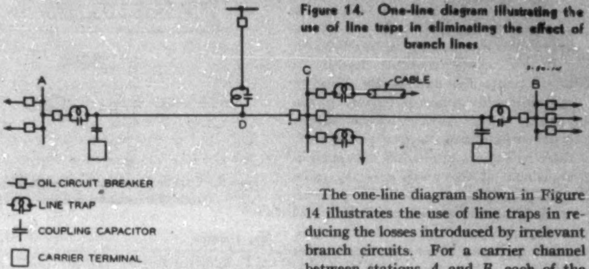


Figure 14. One-line diagram illustrating the use of line traps in eliminating the effect of branch lines

In general, the function of the line trap is to insure the reliability and improve the efficiency of carrier channels applied to a transmission line by confining the carrier-frequency currents to desired portions of the system or by minimizing the losses in low impedance loads or irrelevant branch lines and preventing interruption or interference from normal switching operations or faults. More specifically, the principal applications of line traps may be listed as follows:

1. To reduce transmission losses in irrelevant branch lines.
2. To minimize the effect of low impedance shunts.
3. To prevent interruption attributable to external faults or intentionally applied grounds.
4. To isolate the carrier channel effectively.

The one-line diagram shown in Figure 14 illustrates the use of line traps in reducing the losses introduced by irrelevant branch circuits. For a carrier channel between stations *A* and *B*, each of the branch circuits at *A*, *B*, *C*, and *D* may absorb a considerable amount of carrier-frequency energy as a shunt load, aside from possible undesirable reflection effects. The effects of the branches at *A* and *B* may be greatly reduced by means of two traps located as shown. At *C* and *D*, traps are used in each of the branch circuits.

Because of reflections, a short branch line, such as *D* of Figure 14, if over 1,000 feet or less than 20 miles in length, may have the effect of a very low impedance shunt at certain frequencies in the carrier range. The difficulty of determining accurately the frequencies at which the branch will appear as a low impedance and possible limitations in choice of a frequency to avoid this loss usually make it advisable to use a trap in each of these cases.

High-voltage cable circuits, such as shown for one of the branch circuits at station *C* of Figure 14, almost invariably require a trap. The extremely low characteristic impedance of such cable circuits, as compared with the line impedance, imposes a heavy load on the carrier circuit at all frequencies, and in addition it may cause undesirable standing-wave or reflection effects.

For certain classes of carrier channels, such as those used for pilot relaying, line traps are as essential to the circuit as the coupling capacitors or the terminal equipment itself. Their principal function in these circuits is to insure that an external fault does not short out the carrier circuit. This application is illustrated by Figure 15, showing a pilot-relaying carrier channel between stations *A* and *B*. If a fault occurs on the system at some point beyond *B*, such as at *X*, the line trap at *B*

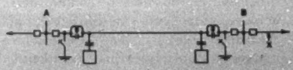


Figure 15. One-line diagram illustrating the use of line traps in maintaining the carrier circuit when faults or grounds are applied

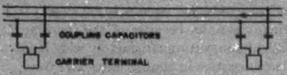


Figure 16. Single interphase coupling

will function to maintain a sufficient amount of impedance between the fault and the carrier circuit so that a normal carrier signal may be transmitted over the circuit during the fault.

Figure 15 also illustrates the use of line traps in maintaining a carrier circuit over a line that may be removed from service and grounded at its terminals. This is accomplished by locating the traps between the coupling capacitors and the grounding switches as shown. In some types of switchyard construction, where a grounding bus is used, the bus is insulated and tied to ground through one or more line traps.

In the case of certain types of carrier circuits it is desirable to isolate effectively the line conductor or conductors used at the carrier frequencies of the channel, by using traps at each terminal and in all branch circuits. Two-terminal circuits such as used for two-frequency duplex telephone channels, most frequently follow this practice. In addition to the usual gains in efficiency and reliability, this arrangement essentially clears the circuit of transmission irregularities and permits the circuit to be terminated properly by the terminal equipment. Under these conditions, definite channel frequencies may be assigned in advance of installation.

Methods of Line Coupling

There are five general types of line coupling commonly employed for single-circuit and double-circuit lines. These types are listed in the order of their general preference, based upon over-all reliability and ease of installation of the carrier channel, as follows:

1. Single interphase coupling.
2. Ground-return coupling.
3. Intercircuit coupling.
4. Double-circuit ground-return coupling.
5. Double-circuit interphase coupling.

Detailed descriptions of these various types of coupling follow.

SINGLE INTERPHASE COUPLING

Single interphase coupling, as shown in Figure 16, uses two of the three power phases of the transmission line as a path for the carrier energy; consequently, it is not affected by high ground resistances or variations in ground resistance as is any type of coupling which requires a ground-return path for the carrier energy.

In an interphase-coupled carrier circuit, conditions on the phase wires to which coupling is not made have negligible effect on the operation of the carrier channel. In addition, this type of coupling provides a circuit having the most stable operating characteristics, the lowest attenuation, and the lowest noise level. Its principal disadvantages lie in the cost and the construction complexity involved in providing coupling and line traps for two phases throughout the circuit, as well as the complexity of line-tuning equipment at terminals and by-passes. It has been used most extensively for long-haul circuits where its efficiency justifies its higher cost.

GROUND-RETURN COUPLING

Ground-return coupling utilizes only one phase wire of the transmission line and ground or any other suitable path for the return side of the circuit, as illustrated in Figure 17. If good grounds are present at the carrier terminals or if a ground wire is carried between the terminals, this type of coupling compares very favorably, in respect to line-attenuation losses, with interphase coupling.

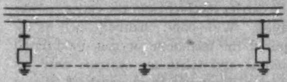


Figure 17. Ground-return coupling

The principal advantage of ground-return coupling is that it generally requires only half the number of coupling capacitors and line traps as required for full metallic coupling such as interphase. In addition, the line-tuning equipment required at terminals and by-passes is usually less complicated. Its main disadvantages lie in its somewhat higher attenuation and higher noise level as compared with the interphase circuit.

Another characteristic, sometimes considered a disadvantage for the ground-return circuit, is the fact that a considerable portion of the carrier energy uses the uncoupled phase wires of the line as a return path (in addition to the ground-return path); and, since definite provision is seldom made for maintaining this return path around transformers or open switches, the circuit may be subject to wider variations in attenuation.

For many types of carrier channels, the disadvantages of the ground-return circuit are greatly outweighed by its simplicity and lower cost. It is almost universally used for all pilot relay channels and extensively used on medium and short-haul circuits for all other types of carrier channels.

INTERCIRCUIT COUPLING

Where a double-circuit transmission line is available between carrier-terminal stations, it is sometimes desirable to use both lines for the carrier circuit to insure its continuity when either line is taken out of service. The intercircuit coupling arrangement shown in Figure 18 is used sometimes to provide this feature.

This is a form of interphase coupling in which coupling is made to one phase wire of one line and to a different phase wire of the other line of a double-circuit power line. It is only applicable on double-circuit lines which are *incapable of being sectionalized between the carrier terminals.* Where a suitable frequency is available to permit operation of the carrier channel with the increased number of switching configurations possible on the double-line channel, as compared with a single-line channel, this double-circuit type of coupling will provide uninterrupted carrier service with all phases of either line solidly grounded at any point. In this respect, it will provide most of the advantages of double-circuit interphase coupling with but half the coupling capacitors and line-tuning assemblies required by the double-interphase arrangement.

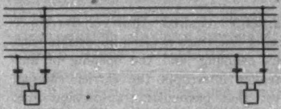


Figure 18. Intercircuit coupling

DOUBLE-CIRCUIT GROUND-RETURN COUPLING

The essential difference between double-circuit ground-return coupling, as shown in Figure 19, and intercircuit coupling is that the coupling is made to the *same* phase wire instead of a *different* phase wire in each line. Since earth or ground wire provides the return path for the carrier energy, this type of coupling can be applied even where the double-circuit lines are sectionalized between carrier terminals. That is, it will provide uninterrupted operation with either line open, or, in most cases, with either line grounded.

DOUBLE-CIRCUIT INTERPHASE COUPLING

This type of coupling, shown in Figure 20, provides the possible advantages of both double-circuit ground-return coupling and intercircuit coupling but requires twice the number of coupling capacitors, line traps, and line-tuning units as these former types of double-circuit coupling. The small advantage gained with double interphase coupling makes it difficult to justify its comparatively high cost.

The preceding comments assume that the coupling capacitors are connected to the conductors on the line side of the station breakers and disconnecting switches. In a limited number of cases, particularly where it is desired to couple to several transmission lines at one location, the coupling capacitors have been connected to the station high-voltage bus. The "bus coupling" may be ground-return, as shown in Figure 21, or interphase.

Any advantage of bus coupling is in the reduction in the quantity of coupling capacitors required. However, the proper use of line traps is often impossible with bus coupling, and the opening of any breaker interrupts the carrier channel over that particular transmission line. Also, the common use of multiple busses, or busses that can be sectionalized, frequently makes the arrangement impractical. These restrictions seriously limit the use of bus coupling and reduce or eliminate its apparent economic advantage.

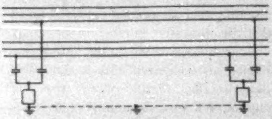


Figure 19. Double-circuit ground-return coupling

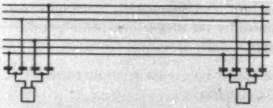


Figure 20. Double-circuit interphase coupling

Selection of Frequency

The frequency range of from 50 to 150 kilocycles has been recognized as standard for power-line-carrier equipment for a number of years; for this reason, the bulk of application effort will probably be concerned with carrier channels in this range. However, there are conditions occasionally encountered that require consideration of frequencies outside of this standard range, even at the expense of special equipment. Some systems have already become so crowded with channels in this range that new channels can be added only by extending the range somewhat, both up and down, or modifying older apparatus to modern standards of selectivity. However, by careful advance planning in the selection of channel frequencies, it is usually possible to provide for all of the channels required on a system in the range available with standard equipment.

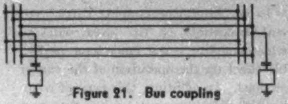


Figure 21. Bus coupling

There are several factors that should be considered in selecting a frequency for a new channel. If there are already carrier channels in use on the system or on adjoining interconnected systems, the question of possible interference with these other channels is usually of primary importance. The spacing of channels is largely dependent on the type of channels involved, the selectivity characteristics of the carrier receivers and the carrier power levels. Wide-band channels such as required for telephone or tone-modulated control and telemeter systems may require a minimum band width of five or six kilocycles with a minimum separation of channels running from 10 to 20 kilocycles. Narrow-band channels such as required for pilot relaying or for frequency-shift-type telemetering may require only one to two kilocycles of the frequency spectrum and, under favorable conditions, may be spaced only one or two kilocycles apart.

For some types of channels, such as are used for single-frequency party-line telephone systems, reflections attributable to untrapped branch lines or short tap lines may limit the choice of frequencies that will give satisfactory transmission between all stations under a variety of line-configuration conditions. Often, in these cases, it is necessary to make circuit-frequency-characteristic measurements before the best frequency can be selected. For most medium and short-haul circuits, however, reasonable judgment in the use of line traps will usually permit a wide choice of frequencies.

Where several types of carrier channels are to be operated on the same system, it is usually a good plan to reserve the lower end of the band for the long-haul circuits to realize the advantage of lower line attenuation. The short-haul circuits can often stand the additional attenuation at the higher frequencies.

When it is found necessary to use frequencies below 50 kilocycles, they should be used only for narrow-band channels, where possible, because of the increased difficulty in getting satisfactory wide-band characteristics in tuning units and traps.

Carrier-Circuit Attenuation

The attenuation or dissipation of the carrier-frequency energy in the course of its transmission from a carrier transmitter at one terminal to a carrier receiver at another terminal may be considered as analogous to voltage drop in 60-cycle power transmission. However, where voltage drop at 60-cycles is usually expressed in per cent, the ratios of transmitted volts and watts to received volts and watts at carrier frequencies is so great that it is most conveniently expressed in decibels, which is a measure of the ratios on a logarithmic scale. A power ratio of 10,000, corresponding to an attenuation of 40 decibels, is not uncommon in transmitting carrier energy over long lines. With this amount of attenuation, a transmitted power of ten watts would appear at the receiver as only 0.001 watt.

Fortunately, the carrier receiver is essentially a voltage-actuated device, requiring a negligible amount of carrier energy at a fraction of a volt on its input terminals to produce the desired output. Compared to a standard broadcast radio receiver, however, the carrier receiver is relatively insensitive. Attributable to the relatively high noise level present on the average transmission system, the maximum sensitivity that can be used in the receiver is limited. The received carrier signal must therefore arrive at the receiver input terminals at a level sufficiently high to establish a satisfactory signal-to-noise ratio.

Another fortunate feature of the modern carrier receiver is its automatic volume control. Practically all types of carrier receivers now have some form of automatic volume control that functions to produce an essentially constant and usable output for a wide variation in input signal. Some types of carrier telephone receivers are in use that will hold the audio-output-level variation within five decibels for a carrier-input-level variation of up to 40 decibels.

From the foregoing considerations it would appear that the application of a carrier circuit would involve such deep-seated problems as determining how much transmitter power is required to produce a certain voltage on the receiver input terminals. In order to simplify the problem greatly, it has become standard practice to rate a transmitter-receiver combination, normally used to operate together in forming a carrier channel, in terms of the maximum attenuation in decibels through which the combination will operate successfully. This rating takes into account three factors: the receiver sensitivity, the automatic-volume-control range, and the transmitter output. The problem therefore resolves itself into one of laying out a circuit that will have a total estimated attenuation within the decibel rating of the equipment it is desired to use for the channel.

The process of estimating the total attenuation for given carrier circuit is simply a matter of determining a value for each of the components involved and adding them up. The principal attenuation components making up the total and the methods used in evaluating each are given in the following list and discussion.

1. ATTENUATION IN LEAD-IN CABLE AT TRANSMITTER TERMINAL

This component is the loss in the concentric cable between the transmitter terminals and the terminals of the line-tuning or coupling equipment. When the line tuning is located with the coupling capacitor and the cable is properly matched through an impedance-matching transformer at the tuning unit, this loss is determined from Figure 22 by multiplying the cable length by the attenuation per 1,000 feet indicated for the carrier frequency selected. If the frequency has not been determined, the value for 85 kilocycles may be used as an average figure.

If the line-tuning equipment is mounted with the terminal equipment and the connection to the coupling capacitor carried directly to the coupling through a concentric cable, the loss should be estimated from the values indicated in Table II.

When both the terminal and line-tuning equipment are mounted together and located near the coupling capacitor, no loss is included for this component.

2. LINE-TUNING AND COUPLING LOSSES AT TRANSMITTER TERMINAL

The losses in the line-tuning and coupling equipment will vary somewhat with the complexity of the tuning circuit and the frequency used. However, since this loss is small and usually less than one decibel, the usual practice is to neglect the variations and use a value of one decibel as shown for item *A* of Table III.

3. TRANSMISSION-LINE ATTENUATION

The attenuation of the carrier energy, in transmission over the power line itself, is influenced by a number of factors; principal among these are the size, spacing, insulation, and disposition of the line conductors, the carrier frequency used, the type of coupling used, that is, whether it is ground-return, interphase, and so forth. For practical purposes, some of these variables can be neglected and the others combined to form the simple set of curves and charts given on Figure 23.

The attenuation values indicated are based on the use of copper or the equivalent steel reinforced aluminum cable conductor and full-metallic coupling. For ground-return coupling, the values should be increased by 20 per cent. The values indicated are purposely made somewhat higher than actual measured values taken on de-energized lines, to include additional miscellaneous losses contributed by connected power equipment.

In using Figure 23, if the line voltage and conductor sizes to be used do not coincide with one of the line descriptions listed, the correct value to use for the attenuation per mile may be determined by interpolating between curves or by using the next higher curve. The geographical line length, neglecting sag and topography, may be used with sufficient accuracy. If the frequency of the channel has not been selected, the value for 85 kilocycles may be used as an average figure.

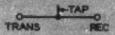
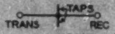
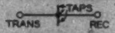
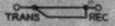
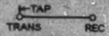
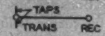
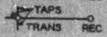
4. ATTENUATION ATTRIBUTABLE TO IRRELEVANT BRANCH LINES

If the transmitter or receiver terminal is located at a line-junction point and the carrier energy is permitted to dissipate over one or more paths, not included in the carrier circuit under consideration, the attenuation introduced may be found under *G*, *H*, and *I* of Table III.

Untrapped spur or branch lines on the circuit between transmitter and receiver terminals will introduce similar losses which may be obtained from *B*, *C*, and *D* of Table III.

Untrapped loop circuits between the transmitter and receiver terminals may introduce the large loss indicated for *E* in Table III, at certain frequencies. Unless it is essential that the alternative paths provided by the loop be maintained, the usual practice is to insert a line trap

Table III. Attenuation Introduced by Miscellaneous Sources

Item	Source of Attenuation		Decibel Loss
A	Line-tuning and coupling equipment		1
B	Untrapped single tap or spur line		3.5
C	Untrapped double tap or spur line		6
D	Untrapped triple tap or spur line		8
E	Untrapped loop		7
F	Additional bridged terminals on same carrier channel, each		1
G	Terminal located at junction with one untrapped tap line		3
H	Terminal located at junction with two untrapped tap lines		5
I	Terminal located at junction with three untrapped tap lines		6

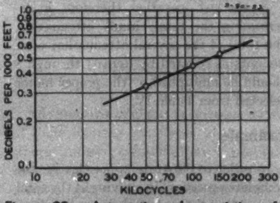


Figure 22. Attenuation characteristics of concentric lead-in cable

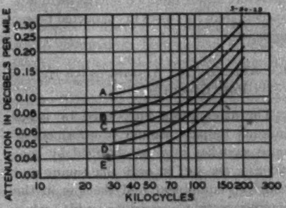


Figure 23. Attenuation introduced by the transmission line

Line Voltage (Kilovolts)	Conductor Size	Curve
34.5	Number 4 average	A
34.5	Number 2/0 average	B
69	Number 4 average	B
69	Number 4/0 average	C
115–161	Number 0 average	C
115–161	1-inch diameter	D
220–287	1-inch diameter	E

Table IV. Multiplying Factors

Type of Carrier Channel	Multiplying Factor
Telephone	1.3
Pilot relaying	1.5
Telemetering, high accuracy	1.5
Telemetering, indication only	1.3
Supervisory control	1.5
Load control	1.3

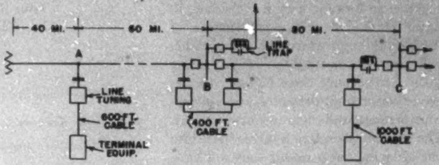


Figure 24. One-line diagram showing typical circuit application

at one end of the loop and treat the remainder as a single branch line.

The values indicated in Table III for circuit configurations are based largely on losses caused by energy being absorbed by these loads on the circuit and do not include additional losses which may be introduced at certain frequencies attributable to reflections caused by short (less than 20 miles) untrapped spur lines. It is usually not considered good practice to leave more than two such short spur lines untrapped even when a wide choice in frequency is available. If the choice in frequency is already limited by the presence of other carrier channels, it is good practice to trap all such short spur lines.

5. ATTENUATION ATTRIBUTABLE TO CONNECTED POWER APPARATUS

Most high-voltage power transformers appear to the carrier circuit as a relatively high capacitive reactance, and a line terminating in a transformer behaves at carrier frequencies essentially as though it were terminated in an open circuit. Such a small amount of carrier energy is transmitted through a transformer that it is seldom necessary to trap it. It is most convenient to include the small amount of loss in transformers and other power apparatus as an average figure making up a small part of the line loss. It is therefore usual practice not to include a specific item for losses in connected power apparatus.

6. ATTENUATION ATTRIBUTABLE TO BY-PASS CIRCUITS

The attenuation introduced by losses in each by-pass around transformers or open switches may be obtained by considering the loss in each line-tuning and coupling equipment and the loss in the interconnecting concentric cable, when used. The loss in each tuning unit is given under *A* of Table III, and the loss in the cable is obtained from Figure 22.

7. LINE-TUNING AND COUPLING LOSSES AT RECEIVER TERMINAL

Line-tuning and coupling-equipment losses at the receiver terminal are treated in the same way as similar losses at the transmitter terminal and calculated as indicated for item 2 of this list.

8. ATTENUATION IN LEAD-IN CABLE AT RECEIVER TERMINAL

This loss is treated in the same way as for the transmitter terminal, under item 1 of this list.

9. MULTIPLYING FACTORS

It is a well-known fact that the accumulation of ice, sleet, snow, and frost on the conductors and insulators of open-wire telephone lines may increase the attenuation of such circuits enormously at carrier frequencies.[2] The increase in attenuation of a power-line-carrier circuit as a result of formations of ice, sleet, snow, or frost on line conductors and insulators has not as yet been determined accurately; however, general experience has indicated that this increase is much smaller than experienced on telephone lines and, in general, can be accommodated easily in the receiver automatic volume control.

The accumulation of dust and oily or sooty deposits on line insulators also has the general effect of both increasing the attenuation and increasing the noise level on a power-line carrier circuit.

The most satisfactory method of accommodating these variations and insuring that they fall within the automatic-volume-control range of the carrier receiver is through the use of a multiplying factor. Since the effect of attenuation variation and the effect of abnormal line noise will vary with the type of service rendered by a carrier channel, different multiplying factors are applied for different types of carrier channels as listed in Table IV.

As the last operation, therefore, in determining the total circuit attenuation, the total of the several attenuation components as covered by items 1 through 8 should be multiplied by the proper factor selected from Table IV.

Example

In order to illustrate, more clearly, the complete procedure in arriving at the total attenuation figure for a carrier circuit, a typical circuit such as Figure 24 may be taken as an example. Assuming that a telephone channel is to be established between stations *A* and *C* over a line distance of 140 miles on a 69-kv 4/0 line, using a carrier frequency of 100 kilocycles and interphase coupling, the components and totals would work out as follows:

Lead-in cable at *A* (Figure 22)	0.27 decibel
Line tuning and coupling at *A* (Table III, item *A*)	1.00 decibels
Untrapped spur line at *A* (Table III, item *G*)	3.00 decibels
Transmission line from *A* to *B* (Figure 23)	6.00 decibels
By-pass at *B* (Figure 22 and Table III)	2.18 decibels
Transmission line from *B* to *C* (Figure 23)	8.00 decibels
Line tuning and coupling at *C* (Table III)	1.00 decibels
Lead-in cable at *C* (Figure 22)	0.45 decibel
Total attenuation components	21.90 decibels
Multiplying factor (Table IV)	1.3
Total attenuation	28.47 decibels

Based on the preceding estimate, a transmitter-receiver combination having a rating of 28.47 decibels or more could be used for the channel.

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Safe Ratings for Overhead Line Conductors

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Synopsis: Many factors must be considered in establishing safe ratings for overhead copper line conductors. Most important is the low-temperature annealing and loss of strength of the hard-drawn copper itself. Others are the effect of atmospheric conditions and line location relative to structures and topographical features tending to obstruct the wind. The actual construction details of the line, such as ground clearances, conductor splices, and connectors are pertinent, as are the policies of the personnel responsible for maintaining the line.

This paper attempts to show how these factors can be evaluated to establish operating ratings, indicating how readily available data can be utilized, pointing out the lack of completely satisfactory data on other points. Procedure is stressed rather than results, as the influence of local conditions upon many of the factors precludes publication of ratings which are entirely safe to apply without careful investigation of the effect upon the particular copper conductors to which they are to be applied.

WARTIME urgency is focusing increasing attention upon utmost power to defense plants with minimum critical material to reinforce the bulk power system. Quite logically pressure is being applied to raising the operating limits of many types of equipment. The need is unquestionable, but extreme caution is essential lest the very effort to assist our war production by allowing increased ratings prove to jeopardize the service we are endeavoring to render. This need for caution applies, of course, to all component parts of the electric-power system, but particularly to the bare conductors of the overhead transmission system where it is too easy to overlook the seriousness of permanent impairment by overheating. This paper is intended to discuss that phase of the problem with the aim of presenting a complete analysis of one commonly used conductor in such form as to facilitate similar analyses of any other copper conductor. The author has endeavored to collect all the pertinent data that are available and to present the combined results in a form suitable for determining safe ratings. Several phases of the analysis are inadequately supported by factual data and will be indicated as assumptions in the hope that laboratory test programs may be arranged to determine the exact relationships.

The allowable rating for any overhead conductor depends upon at least eight factors, as follows:

1. The ability of the conductor to withstand the resulting temperature without excessive loss of mechanical strength by annealing.
2. The ability of clamps, connectors, and joints on the conductor to withstand the resulting temperature without oxidation of the contact surfaces and local heating in excess of the allowable conductor temperature.
3. The adequacy of span clearances to permit the additional sag caused by the conductor temperature associated with the proposed rating.
4. The problem of providing short-circuit and overload protection to operate within the margin between the current rating and destructive overloads.
5. The adequacy of substation and terminal equipment to carry the currents for which the transmission conductors may be rated.
6. Operating problems of the bulk power system with individual circuits rated to high values.
7. The ability of voltage-regulating equipment to compensate for the voltage variations which accompany heavily loaded lines.
8. The considerable energy losses resulting from heavy line currents.

No engineer would contend that all eight are of equal importance, but neither can he afford to ignore any one until he has satisfied himself that it does not limit the particular line which he wishes to rate.

Methods for Raising Limitations

Methods are available to improve many of these limiting conditions to the point where other considerations establish limitations at higher current ratings. In general, increased vigilance is essential, as many of the limitations depend upon visual observation for detection, and operation at higher current ratings should be expected to require more frequent inspection and maintenance.

One company, desiring to carry heavier loads on a 66,000-volt 336,400-circular-mil steel reinforced aluminum cable transmission circuit, ordered close inspection of the dead-end clamp, loop, and insulator assemblies on several structures. This inspection revealed conductor strands damaged in the dead-end clamps to the point where heavier loading probably would have caused failure, and parallel-groove connectors distorted to the point where contact pressure had been relaxed and strands had welded to the contact surfaces. The circuit was rebuilt with compression-type dead-end clamps and improved connectors, then raised to the higher rating.

After that discovery, the policy has been established of thoroughly inspecting and overhauling any circuit before it is released for operation above a nominal rating. Tests in still air at currents up to 750 amperes on 4/0 conductors and various types of connectors indicated that the newer and heavier bronze parallel-groove connectors and the *U*-bolt type bronze connectors operate much cooler than the 4/0 conductors themselves and appear to be adequate for any currents that can be applied safely to the conductors. Automatic line splices in tension seem to be satisfactory, but the old served joints have been found inadequate. Split-bolt connectors of the types used for distribution construction cannot be recommended for heavily loaded transmission lines. Further tests may prove the adequacy of more devices, but methods are available now to eliminate clamps and connectors as restrictions to the rating of copper conductors for conductor temperatures up to at least 100 degrees centigrade.

Magnetic heating of suspension and strain clamps may be a serious problem. W. H. Burleson[1] has reported tests on 500,000-circular-mil stranded copper conductor indicating conductor temperatures inside the usual malleable iron suspension clamps as much as 100 degrees Fahrenheit higher than the conductor temperature outside the clamps, evidently caused by magnetic losses in the iron. His tests indicated also that the magnetic heating is reduced considerably by substituting nonferrous *U*-bolts in the malleable iron clamps and virtually eliminated by the further substitution of a nonferrous keeper. Similar tests by other experimenters using large aluminum conductors without armor rods in malleable iron suspension clamps show substantially the same magnetic heating, with consider-

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able reduction when armor rods were added to the conductor in a correspondingly larger clamp. Although the effect seems less pronounced with smaller conductors and correspondingly lower currents, there is ample reason to fear that magnetic heating in conventional suspension and strain clamps may raise the temperature of copper conductors well beyond the limit established by annealing. Suspension clamps also have been found within which the copper conductor has been burned badly, apparently caused by loosening of the clamps to the point where arcing has occurred between the conductor and the clamp. Every suspension and strain clamp thus becomes subject to investigation before high current ratings can be allowed, and two methods offer possibility of correction, namely:

1. Replace or rebuild the clamps to eliminate the closed magnetic paths and provide reliable contact between the clamps and the conductor.

2. Install additional conductor material, possibly in the form of armor wires, through larger all-ferrous clamps to reduce the resistance loss and to aid in dissipating the magnetic heating.

Many transmission lines have been designed for safe clearances to ground and structures under the spans at conductor temperatures of 50 degrees centigrade or less. Raising the maximum operating temperature to 100 degrees centigrade causes a considerable increase in length by expansion of the conductors and reduces clearances to ground by nearly four feet in a 1,000-foot span. Obviously, then it is important to ascertain that the line has ample clearances before the operating ratings are increased. Occasionally additional clearance can be secured by increasing the tension, but this method must be applied with due consideration to the strength of supporting structures and recognition of the fact that higher design tensions allow less margin for annealing and thus reduce the maximum current rating that can be allowed. There remains always the possibility of raising the point of attachment on structures by remodeling the structure or sometimes by changing the conductor attachment from suspension to strain. Clearance at elevated temperatures may be a problem, but several methods are available to eliminate it as a final limitation to higher current ratings.

Currents of the magnitude caused by line faults may be exceedingly destructive if allowed to persist, as they tend to cause very high conductor temperature. Most protection in common use depends upon some form of overcurrent relays to discriminate between load currents and fault currents, the principal exception being several adaptations of differential protection such as pilot-wire or carrier and double-circuit balanced protection. Obviously some portion of the useful life of the conductor must be allocated to short-circuit operation, the amount depending upon how fast and how close above load-current ratings the fault protection can be maintained and upon the frequency and magnitude of the faults. Conservation of conductor life of heavily loaded circuits thus becomes an additional argument for improved lightning protection to reduce the number of faults, and improved relay and breaker protection to reduce the conductor heating caused by the fault currents.

No increase in transmission conductor rating is useful unless all associated equipment is adequate. The entire circuit must be reviewed from substation bus through the disconnect switches, circuit breakers, metering and relay equipment, and leads to the transmission line conductors, thence through any intermediate switching stations to the substation at the other end of the circuit. Numerous investigations have disclosed disconnect switches which failed to pass test at the higher currents without reinforcing or silver plating of contacts or enlarging the leads near the switch terminals to prevent heat flow from the conductors into the switch contacts. Some circuit breakers have required additional contacts or larger bushings, and metering transformer ratios, and meter scales have been changed. Many such limitations may be found, most of which could cause serious trouble if overlooked but can be corrected quite easily if detected before the rating is raised.

Operating problems of a power system become increasingly involved as the relative importance of operating units is increased. When a transmission circuit forms part of a loop or network, it is customary to maintain service to all the load through the remaining circuits during any single circuit outage. As the load carried by any one circuit is increased, the provision of reserve capacity in the other facilities which must carry the load during outages of the heavily loaded circuit becomes increasingly difficult. In some cases the point is reached where the only recourse is to lighten the load by dumping, which would be intolerable where essential service would be interrupted. In this manner, operating problems may become a critical aspect requiring extensive analysis before an increased rating can be permitted to supplant system reinforcement.

Transmission circuits designed for operation at lower ratings frequently have no provision for voltage-regulating equipment to compensate for transmission regulation or have been provided with synchronous capacitors or other equipment of limited range. Heavier loadings may be expected to cause greater voltage variations, possibly in excess of the maximum for which the system was designed to operate. This aspect is not likely to be critical in metropolitan systems but may easily constitute a problem on more extensive lines. System stability may be involved, also, and these two factors should be investigated together. Many methods are available, however, to provide relief in case either one should prove a limitation.

Energy losses at the ratings indicated by this paper amount to 100 kilowatts

Table I. Determination of Equivalent Ambient Temperatures for Copper Conductors

Month	Greatest Monthly Mean Temperature (Degrees Fahrenheit)	Greatest Monthly Range of Daily Mean Temperature (1933–42) (Degrees Fahrenheit)	Greatest Daily Range of Hourly Temperatures (Degrees Fahrenheit)	Daily Correction (Degrees Fahrenheit)	Monthly Correction (Degrees Fahrenheit)	Equivalent Ambient Temperature		
						Monthly (Degrees Fahrenheit)	Seasonal (Degrees Fahrenheit)	Annual (Degrees Fahrenheit)
December	45.6	48.0	49.0	+7.6	+6.4	59.6	59.0	76.0
January	44.4	51.0	51.0	+8.0	+7.0	59.4		
February	42.7	51.0	52.0	+8.2	+7.0	57.9		
March	50.7	47.0	44.0	+6.2	+6.2	63.1	66.6	
April	57.2	41.0	48.0	+7.3	+4.9	69.4		
May	69.5	34.0	48.0	+7.3	+3.4	80.2	84.4	
June	75.0	32.0	37.0	+4.4	+3.1	82.5		
July	80.3	26.0	37.0	+4.4	+2.0	86.7		
August	78.7	25.0	37.0	+4.4	+1.9	85.0		
September	77.2	37.0	39.0	+4.9	+4.0	86.1		
October	62.9	42.0	41.0	+5.4	+5.1	73.4	70.6	
November	51.6	52.0	52.0	+8.2	+7.3	67.1		

or more per mile of circuit. The cost of the system investment and energy thus lost from useful service in some cases may justify the provision of additional facilities in lieu of remodeling existing circuits for increased ratings. In other cases, installation of power-factor-corrective equipment may be indicated.

Limitations of the conductor itself have been left for final consideration. Overhead conductors depend upon their inherent mechanical strength for support. Many conductors now in service have been weakened by flashover burns and broken strands, which tend to increase the heating at points already reduced in strength but not sufficiently damaged to justify repair. Heavier current loading means increased temperature in the conductor with a tendency to approach some limit as yet not defined. Whereas all of the other limiting factors have been found susceptible to relief, however, it seems probable that the conductor itself will prove to be a final limitation which cannot be relieved by any method short of replacement.

Table II. Average Wind Velocities

Month	Monthly Average (1940–42)	Seasonal Average	Annual Average
December	11.6	11.8	10.5
January	11.9		
February	12.0		
March	12.7	11.8	
April	11.0		
May	10.0	9.0	
June	9.2		
July	8.8		
August	8.4		
September	8.8		
October	10.0	10.9	
November	11.8		

Relationship Between Current and Conductor Temperature

It is reasonable to expect that the ultimate limit to the current-carrying capacity of a conductor will be established by temperature rise caused by energy losses to the point beyond which permanent damage begins. The first approach to the solution, therefore, involves a determination of the heat energy which can be dissipated into the atmosphere at various conductor temperatures. It is necessary to consider

1. Atmospheric temperature.
2. Wind velocity and direction relative to conductor.
3. Atmospheric pressure.
4. Effect of solar radiation.
5. Conductor surface thermal emissivity constant.
6. Conductor outside diameter.

Atmospheric temperatures ranging from −20 degrees Fahrenheit to 103 degrees Fahrenheit have been recorded by the United States Weather Bureau at Pittsburgh. For our purpose, however, an annual equivalent ambient temperature will be derived, using the method proposed by W. C. Sealey[2] with the aging curve of hard-drawn copper. Pertinent Weather Bureau data and calculated values are shown as Table I.

Plotting the various temperatures of Table I as Figure 1 serves to demonstrate the conservatism of this method for determining the equivalent ambient temperatures. Pittsburgh temperature data are recorded well above the ground under conditions closely resembling those prevailing along the transmission system, hence should be a satisfactory basis for establishing current ratings. It is proposed subsequently to use the annual equivalent ambient temperature in determining the relationship between current and temperature.

Wind velocity likewise is important as an aid to dissipating heat from the conductors. No way has yet been found to establish the effectiveness of wind independent of other variables, however, and it is necessary to resort to approximations. Weather Bureau records during the three-year period, 1940–2 inclusive, show wind velocities from practically zero up to 46 miles per hour, with an average velocity of 10.5 and a slight but consistent seasonal variation. These data are shown as Table II.

Subsequent formulas will show that wind velocity is effective approximately as the square root in dissipating heat, hence the squared average of the square roots of the hourly observations would be more suitable than the arithmetical average shown in Table II. Such data are not readily available, but computation from the hourly readings of wind velocity for several typical months indicates the squared average root of the wind velocity to be approximately 90 per cent of the arithmetic average.

Wind direction also is important insofar as it affects the angle of incidence to the conductors. With most lines having sections at considerable angular displacement from the general source-to-destination direction, it is not feasible to attempt to consider the specific direction of the wind. Marked deviation from the perpendicular has comparatively small influence, however, as wind at any angle from 30 degrees to 150 degrees should be at least equivalent to a perpendicular wind of 50 per cent of the velocity. No information has been found which could be applied to a wind parallel with the conductors, but when the angular shifting of even a steady wind is considered, together with the catenary curvature of span wires and the effect of ground contours in setting up local turbulence, it seems reasonable to assume that wind in any direction can be represented by a perpendicular wind with 50 per cent of the velocity. Following that assumption, wind velocities will be used at 50 per cent of the squared average root of the recorded values, and the direction disregarded.

Still another factor having considerable influence upon the cooling effect of winds is the shielding effect of high ground and buildings near the line. A good example is found in the ten per cent differential between the monthly average wind velocity recorded at the Allegheny County Airport situated on high ground and simultaneous recordings on the roof of a large building in the valley at Pittsburgh. For 66-kv

Table III. Effect of Five Degrees Centigrade Solar Heating During Sunshine Hours Upon Equivalent Ambient Temperatures

Month	Equivalent Monthly Ambient Temperature (Degrees Fahrenheit)	Sunshine Time (Per Cent)	Solar Heating Correction (Degrees Fahrenheit)	Equivalent Effective Temperature: Monthly (Degrees Fahrenheit)	Equivalent Effective Temperature: Seasonal (Degrees Fahrenheit)	Equivalent Effective Temperature: Annual (Degrees Fahrenheit)
December	59.6	11.6	+1.0	60.6	60.5	79.1
January	59.4	12.8	+1.2	60.6		
February	57.9	17.4	+2.3	60.2		
March	63.1	22.4	+2.7	65.5	69.6	
April	69.4	27.8	+3.4	72.8		
May	80.2	35.7	+4.1	84.3	88.7	
June	82.5	40.2	+4.3	86.8		
July	86.7	40.6	+4.2	90.9		
August	85.0	36.2	+4.1	89.1		
September	86.1	32.1	+4.0	90.1		
October	73.4	25.3	+3.0	76.4	73.0	
November	67.1	16.7	+1.7	63.8		

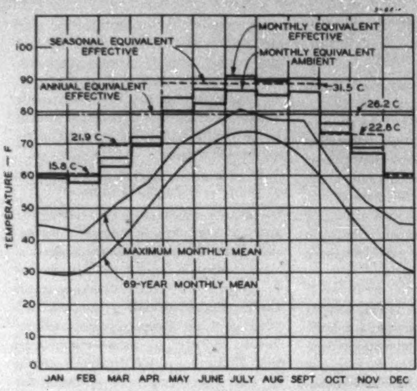


Figure 1. Equivalent ambient temperatures for copper conductors and equivalent effective temperatures including solar heating, Pittsburgh district

transmission circuits, located as they commonly are on hill tops and in open country, the Weather Bureau data from the downtown Pittsburgh observation post should be amply conservative, and no other allowance for shielding seems necessary. When airport data are used, they will be reduced ten per cent to approximate corresponding data as they would have been recorded at the downtown location. Lower-voltage lines, particularly when located in built-up urban areas, definitely require an allowance for the shielding effect of buildings and hillsides, however, and tests by Schurig and Frick[3] seem to indicate that only approximately 25 per cent of the undisturbed wind velocity would reach the line.

Summarizing these wind-velocity factors, average wind velocities recorded at the airport are reduced to 40.5 per cent of the recorded values when applied to exposed 66-kv lines, to 10.1 per cent when supplied to shielded lower-voltage lines. Hourly wind velocities recorded at the downtown Pittsburgh observation post are reduced to 50 per cent of the recorded values when applied to exposed 66-kv lines, to 12.5 per cent when applied to shielded lower-voltage lines.

Atmospheric pressure enters the formula for heat dissipation as the square root. The normal variations in any one location do not often exceed plus or minus two per cent, hence have little effect upon the heat conductivity. Altitude reduces the atmospheric pressure nearly four per cent for each 1,000 feet above sea level, however, and should be included in the calculations for many localities. In the vicinity of Pittsburgh, for example, the altitude of transmission conductors is close to 1,000 feet above sea level and the normal pressure is 0.96 atmosphere.

Solar radiation tends to raise the temperature of conductors exposed to sunlight above that normally associated with the losses generated by current flow and the atmospheric temperature and wind velocity. Of course the effective surface of a cylindrical conductor is only $1/\pi$ times the total exterior surface and any rise in temperature results immediately in greater dissipation of heat to the surrounding air and other masses so that the added temperature rise from solar radiation should be small. Schurig and Frick[3] observed a two to eight degrees centigrade temperature increase attributable to solar radiation with the higher values applying only to conductors starting at atmospheric temperature. Several other sources show temperature increases of ten degrees centigrade for dark surfaces otherwise at essentially atmospheric temperatures. As an extreme condition, therefore, it will be assumed that loaded overhead conductors are subject to a five degree centigrade rise in temperature during all the hours in which sunshine is reported by the Weather Bureau.

Solar heating is very similar in effect to a corresponding increase in the atmospheric temperature. Arbitrarily adding nine degrees Fahrenheit (five degrees centigrade) to each monthly equivalent ambient temperature for the normal hours of sunshine for that month, then combining the two blocks by the method used by Sealey[2] for combining different months gives an equivalent which will be called the "monthly equivalent effective temperature." The monthly values then may be combined into seasonal and annual equivalent effective temperatures as shown in Table III.

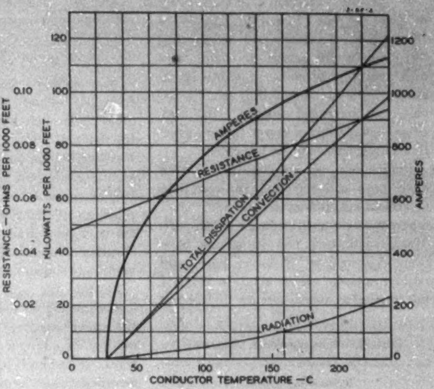


Figure 2. Curves for 4/0 bare stranded copper conductor

1. Watts dissipated
2. Resistance
3. Current at various conductor temperatures for equivalent effective atmospheric temperature (including solar heating) = 26.2 degrees centigrade, and effective crosswise wind velocity, 40.5 per cent of airport average = 4.25 miles per hour

Conductor surface of bare copper ultimately becomes darkened by exposure to the atmosphere. Various investigators have reported surface thermal emissivity factors from 0.15 for bright copper increasing to 0.77 for well-tarnished copper. In industrial districts, the formation of a tarnish film is accelerated by atmospheric smoke and other waste products and a coating develops which is judged to have an emissivity slightly higher than the ordinary tarnished surface. As a compromise, it is proposed to use 0.50 in subsequent calculations. This value would permit slight overheating of new conductors, which would tend to accelerate oxidation and would be compensated amply by underheating after the tarnished surface had formed completely.

Conductor outside diameter enters into computations of convection and radiation, both of which are dependent upon the outside surface area. For solid conductors, the surface is obviously π (diameter) (length). Stranded conductors have an irregular surface which is considered equivalent to an envelope measured as π (diameter over strands) (length).

With the requisite data thus established, it is possible now to compute the dissipation of heat at various conductor temperatures. During the last forty years, many experimenters have published reports about the dissipation of heat from cylindrical surfaces in air, but I. Langmuir[4] and C. W. Rice[5,6] seem to have prepared the most complete presentations.

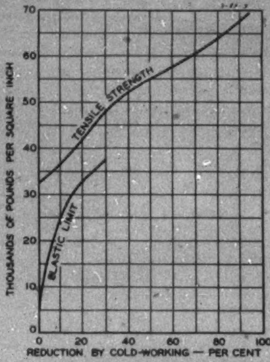


Figure 3. Tensile strength and elastic limit of cold-drawn copper

Total dissipation of heat,

$$W = W_R + W_C \text{ watts per centimeter of length} \quad (1)$$

where radiation,

$$W_R = 18.55\times 10^{-12} D[(T_2)^4-(T_1)]E \text{ watts per centimeter of length} \quad (2)$$

and convection,

$$W_C = S(\phi_2-\phi_1) \text{ watts per centimeter of length} \quad (3)$$

The shape conductance, S, of the stationary film of air held by friction to the surface of the conductor is determined as

$$S = 2\pi/\log_e\left(\frac{2B+D}{D}\right) \quad (4)$$

where the film thickness for moving air

$$B = (0.00572D^{0.5}T_a^{0.377})/(P^{0.5}V^{0.5}) \text{ centimeter} \quad (5)$$

The thermal conductivity of the air film

$$(\phi_2-\phi_1) = 3.46\times 10^{-6}T_a^{0.754}(T_2-T_1) \quad (6)$$

In equations 1 to 6, the symbols are defined as follows:

T_2 = absolute temperature of conductor, degrees absolute
T_1 = absolute temperature of atmosphere, degrees absolute
T_a = average of conductor and atmospheric temperatures, degrees absolute
ϕ_2 = thermal conductivity of air at atmospheric pressure and temperature T_2
ϕ_1 = thermal conductivity of air at atmospheric pressure and temperature T_1
E = thermal emissivity factor for surface of conductor (black body = 1.00)
D = outside diameter of conductor, centimeters
P = atmospheric pressure, atmospheres
V = wind velocity, centimeters per second

Subsequent tests by Schurig and Frick[3] confirmed the aforementioned relationships within close limits for conductor sizes from 0.46-inch diameter up.

The next step is to utilize these data and equations to compute the energy which can be dissipated from the conductor. A typical calculation for 4/0 stranded is given as Appendix A, with the annual equivalent effective temperature (including solar heating) substituted for atmospheric temperature. Similar calculations for other conductor temperatures permit drawing the curves for the dissipation of heat from the 4/0 bare stranded conductor shown in Figure 2.

Knowing the conductor temperature, it is necessary now to determine the conductor resistance. The resistance of 4/0 stranded hard-drawn copper is given as 0.0624 ohm per 1,000 feet at 75 degrees centigrade and the temperature coefficient is 0.00408 per degree centigrade. Hence the resistance at any other temperature is found from the relationship

$$R = 0.0624(1+0.00408t_2)/(1+0.00408\times 75) \text{ ohms per 1,000 feet} \quad (7)$$

where t_2 = conductor temperature, degrees centigrade. For convenience, the resistance at various temperatures has been added to Figure 2.

With the total watts dissipation and the resistance both known for various conductor temperatures, the corresponding conductor current is determined as

$$I = \sqrt{W/R} \text{ amperes} \quad (8)$$

Various points give the heavy curve of Figure 2 for the relationship between current and conductor temperatures in air at 76.0 degrees Fahrenheit (24.5 degrees centigrade) with sufficient solar heating to raise the effective conductor temperature 3.1 degrees Fahrenheit (1.7 degrees centigrade) and wind velocity of 10.5 miles per hour at the airport.

The family of curves for other values of temperature and wind velocity can be derived readily from Figure 2 by modification of the method proposed by Kidder and Woodward.[7] Their equation 13 is modified for bare wire to

$$I = CD^{1.25}(t_2-t_1)^{0.5}V^{0.25} \quad (9)$$

In this equation for a given wire size $CD^{1.25}$ is substantially constant throughout the range of temperatures and wind velocities required, therefore we may write

$$I' = I(t_2'-t_1')^{0.5}(V')^{0.25}/(t_2-t_1)^{0.5}(V)^{0.25} \text{ amperes} \quad (10)$$

where

I = current from Figure 2, amperes.
I' = current for other atmospheric conditions, amperes
t_2 = conductor temperature from figure two, degrees centigrade
t_2' = conductor temperature for other atmospheric conditions, degrees centigrade
t_1 = annual equivalent effective ambient temperature = 26.2 degrees centigrade
t_1' = other equivalent effective annual temperature, degrees centigrade
V = annual average wind velocity at Weather Bureau observation post = 10.5 miles per hour

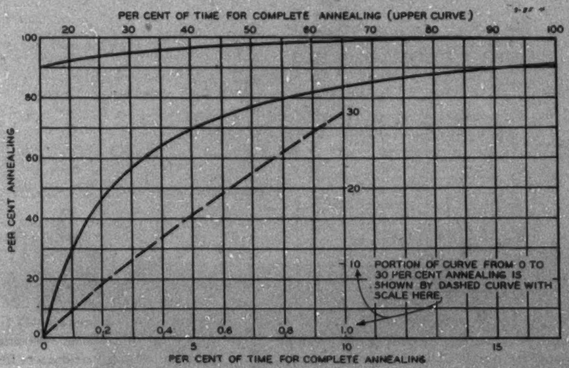


Figure 4. Annealing rate of cold-worked electrolytic copper

V' = other wind velocity at Weather Bureau observation post, miles per hour

With this relationship established, multiplying factors to convert the current curve of Figure 2 computed on the annual equivalent effective basis to corresponding current curves at the same conductor temperatures for summer and winter are determined as follows:

	Annual	Summer (May–September)	Winter (November–January)
$t_2 =$	56.0 C		
$t_2' =$		56.0 C	56.0 C
$t_1 =$	79.1 F = 26.2 C		
$t_1' =$		88.7 F = 31.5 C	60.5 F = 15.8 C
$V =$	10.5 miles per hour		
$V' =$		9.0 miles per hour	11.8 miles per hour
$I =$		510 amperes	
$I' =$		$510\frac{(24.5)^{0.5}(9.0)^{0.25}}{(29.8)^{0.5}(10.5)^{0.25}}=444$ amperes	$510\frac{(40.2)^{0.5}(11.8)^{0.25}}{(29.8)^{0.5}(10.5)^{0.25}}=611$ amperes

Thus we have a method to correlate conductor temperature and current with atmospheric temperature and pressure, solar heating, and wind velocity for any period of the year.

Annealing Characteristics of Copper Conductors at Low Temperatures

An offhand figure for the annealing temperature of copper often is given as 200 degrees centigrade. Investigation of the literature on the subject, however, discloses a number of notations of copper being annealed at temperatures below 100 degrees centigrade. The rate of annealing at these low temperatures depends upon the temperature, the time for which that temperature is maintained, the purity of the copper (small fractions of one per cent of silver or arsenic have marked effect), and the amount of cold-working in the process by which the copper was prepared.

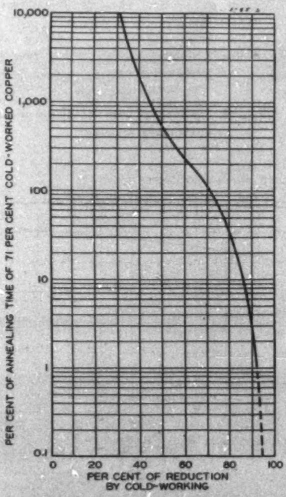


Figure 5. Effect of percentage of cold-working upon the annealing time of electrolytic copper

Wire is manufactured to meet the American Society for Testing Materials specifications which measure purity in terms of ability to meet mechanical and electrical requirements. Since it is known that pure copper has the lowest annealing temperature and electrolytic copper is probably the purest grade used in the manufacture of commercial wire, it is proposed to base all annealing considerations upon commercial electrolytic copper.

The amount of cold-working in solid copper transmission conductors is indicated by the tensile strength. Stranded conductors are rated as cable, usually running approximately 90 per cent of the summation of strength of the individual strands, whence determination of the characteristics of the copper must be based upon the individual strands. Comparison of the specifications for conductors in common use indicates rather consistent ranges of tensile strength.

Conductor Size	Strands		Tensile Strength (Pounds Per Square Inch)	
(B and S Gauge)	Number	Size (Inch)	Conductor	Strands
Hard Drawn				
4/0	7	0.1739	55,100	61,200 to 62,000
1/0	7	0.1228	57,300	63,700 to 64,300
3	1	0.2294	59,000	59,000
Medium Hard Drawn				
4/0	7	0.1739	45,600	48,660 to 56,000
1/0	7	0.1228	48,500	49,660 to 57,000
3	1	0.2294	51,500	48,000 to 55,000

The hard-drawn copper ranges from 59,000 to 64,300 pounds per square inch; the medium-hard-drawn from 48,000 to 57,000 pounds per square inch. Because of the change to stranded conductors for the larger sizes, the variation in diameter of the copper is held to a rather close range, 0.1228 to 0.2294 inch. By grouping the conductors in this way, it becomes feasible to derive one set of data for the annealing of hard-drawn conductors and a second set for medium-hard-drawn.

The only extensive investigation of the annealing characteristics of cold-worked copper which has been found is that by Pilling and Halliwell.[8] They prepared samples of four different amounts of cold-rolling from the same batch of commercial electrolytic copper and heated test pieces from these samples for different lengths of time at different temperatures. From these tests, they derived

1. A curve showing the relationship between the amount of cold-rolling and the tensile strength.
2. A curve showing the per cent of annealing against per cent of the time required for complete annealing which applies to widely different conditions.
3. Two curves showing the effect of the amount of cold-rolling upon the time required for complete annealing.
4. Curves showing the time required at various temperatures for complete annealing of copper of two different amounts of cold-rolling.

Chemical analysis of the copper showed oxygen 0.066 per cent, iron 0.003 per cent, sulphur 0.002 per cent as the only measurable impurities.

Equivalent data for hard-drawn copper wire are not available, but limited test data have been reported by various experimeters. Metallurgists advise that there is no great physical difference between cold-drawn and cold-rolled copper; therefore it is reasonable to utilize the Pilling and Halliwell curves to convert the wire test data to a common base of an initial tensile strength of 64,000 pounds per square inch. Data published by Webster, Christie, and Pratt[9] establish a curve for tensile strength versus reduction of area by cold-drawing for electrolytic copper, reproduced as Figure 3, by which 64,000 pounds per square inch tensile strength is found to correspond to 80.5 per cent cold-drawing. This curve, together with the Pilling and Halliwell data showing the per cent of annealing versus per cent of time for complete annealing and the effect of per cent of cold-working upon the annealing time, reproduced here as Figures 4 and 5, respectively, complete the tools for this conversion. The twelve points, after conversion to 80.5 per cent cold-drawing, have been plotted in Figure 6, then averaged by the method of least squares to the solid curve. For comparison, the corresponding curve for cold-

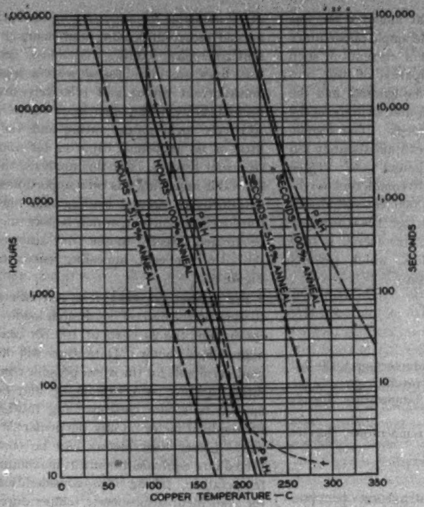


Figure 6. Time required to anneal completely 80.5 per cent cold-drawn copper wire

Heavy dash curves show time permissible to retain 1.5 factor of safety

—— Average for hard-drawn wire

- - - Tests on 0.104-inch hard-drawn wire

— — Tests on 4/0 seven-strand hard-drawn conductor

⊕ ⊕ Points from other tests

P&H Computed from Pilling and Halliwell cold-rolled data

rolled copper has been added. The points show greater deviation than could be considered desirable, but that may be attributed to probable errors in tests run for other purposes in which the annealing data were only incidental, also in part to possible variations in the silver content of the test specimens. It is hoped that carefully controlled tests can be run to establish the time-temperature data for hard-drawn copper transmission conductors definitely in order to make available their full capacity. Until this is done however, the only reasonable procedure is to use such a curve as Figure 6, allowing ample margin for possible error in annealing data.

Similar analysis for medium-hard-drawn copper of 57,000 pounds per square inch tensile strength indicates that the temperature must be approximately 26 degrees higher than for hard-drawn copper to anneal completely in the same time.

Since some engineers are interested in the amount of annealing at a certain temperature for varying lengths of time, or, in the remaining tensile strength, Figure 7 has been derived from the aforementioned data. This illustration shows clearly the rapid loss in strength of hard-drawn copper as compared with medium-hard-drawn and may be useful for comparison of these calculated values with laboratory data.

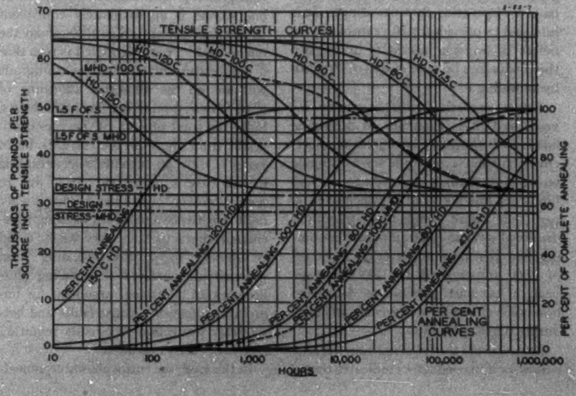
The ultimate strength of completely annealed hard-drawn copper (annealed at low temperature) is about 33,000 pounds per square inch, hence the loss from an initial strength of approximately 64,000 pounds per square inch is only 48 per cent. Since most designs are based on conductors stressed to 50 per cent of ultimate for heavy-loading conditions, it is probable that sufficient margin would remain to keep the conductor from failing. The softened conductor has a very low elastic limit, however, and tends to stretch until the resultant stress has been reduced to the new elastic limit. This phenomenon causes the annealed wires to sag lower than originally, and removing the slack does not permanently correct the condition.

In order to maintain approximately the design sags, it is necessary to keep the elastic limit above the design tension. All annealing data are expressed in terms of tensile strength, and there appears to be no direct relationship between the strength and the elastic limit after partial annealing. A curve plotted from limited data for the elastic limit against the per cent of reduction by cold-working has been added to Figure 3 to provide an approximate means for converting the required elastic limit into corresponding ultimate tensile strength. The difference between this and the initial tensile strength is the allowable loss due to annealing which in turn establishes time-temperature limits for the copper conductor.

Hard-drawn stranded conductors have an initial tensile strength in the strands of approximately 64,000 pounds per square inch. Transmission design normally provides a factor of safety of 2.0, stresses the strands to approximately 32,000 pounds per square inch under ice and wind loading conditions. High-temperature operation tends to reduce the tensile strength and must be presumed to lower the elastic limit also. It would seem desirable, therefore, to restrict the temperature and time to values which will preserve an elastic limit not lower than the design tension of 32,000 pounds per square inch and an ultimate tensile strength to give a factor of safety of at least 1.5.

Figure 7. Annealing and loss of tensile strength of hard-drawn copper conductors (HD)

A corresponding curve for medium-hard-drawn copper conductors (MHD) shown by dash line for comparison. Design stress and 1.5 factor of safety limits (F of S) indicated by broken lines



The elastic limit of copper is indefinite at best, and the available data are exceedingly limited. It appears to increase with cold-drawing in much the same manner as the tensile strength, as indicated by the curve added to Figure 3. Since all data available on annealing are measured in terms of tensile strength rather than elastic limit, the only means available for establishing an annealing limit from elastic limit is in terms of the corresponding tensile strength. Assuming that the elastic limit of hard-drawn copper annealed down to a medium-hard-drawn tensile strength is similar to that of copper drawn directly to the same medium-hard-drawn tensile strength, we find that an elastic limit of 32,000 pounds per square inch requires a tensile strength of 41,000 pounds per square inch. This strength gives a factor of safety of only 1.28, however, and the limitation of annealing is established by the desired 1.5 factor of safety at a minimum tensile strength of 48,000 pounds per square inch. The permissible annealing thus is

$$(64{,}000-48{,}000)/(64{,}000-33{,}000)=51.6 \text{ per cent}$$

Reference to Figure 4 indicates that 51.6 per cent annealing occurs in 2.47 per cent of the time for complete annealing. Accordingly a dashed curve has been added to Figure 6 showing the time-temperature limits of cold-drawn copper as established by annealing. It is interesting to note that 100 degrees centigrade for only 3,500 hours terminates the entire useful life of the conductor, whereas 75 degrees centigrade extends the life to 27,000 hours.

With the allowable time-temperature limits established, and a method developed to correlate current with temperature, it becomes possible to set up a basis for operating current ratings. It is intended to provide for 20 years of service before it becomes necessary to replace the conductors because of loss of strength by annealing.

Before operating ratings can be investigated, it is necessary to determine how much annealing is caused by short-circuit currents. Records for a 280-mile 66-kv system show an average of 1.65 faults per line per year, of which over 50 per cent were single-phase-to-ground drawing less than 1,000 amperes. Assuming that the long-time average in excess of 1,000 amperes will be 1.00 faults per year, it becomes possible to determine how much annealing will occur from faults during the 20-year life.

Analysis of the clearing-time-current curves for 4/0 conductors indicates that a curve allowing 5,000 amperes for one second should provide ample opportunity for fault clearing. For currents of such short duration, dissipation of heat into the atmosphere can be ignored, and the temperature rise computed entirely on the basis of thermal capacity. The specific heat of copper averages approximately 180 watt-seconds per pound per degree centigrade, whence 4/0 conductors weighing 640 pounds per 1,000 feet will absorb 115,000 watt-seconds per 1,000 feet per degree centigrade. This energy is supplied by the I^2R loss, which permits setting up the equation

$$I^2R_at=115{,}000(T_1'-T_1) \qquad (11)$$

where

R_a = resistance of conductor per 1,000 feet at average temperature
T_1 = initial conductor temperature, degrees absolute
T_1' = final conductor temperature, degrees absolute
t = time in seconds

The resistance, R_a, at average temperature may be represented approximately by the expression

$$R_a=8.95\times10^{-6}(T_1'+T_1) \qquad (12)$$

which is substituted in equation 11, rearranged to give

$$T_1'=T_1(12.86\times10^{9}+I^2t)/(12.86\times10^{9}-I^2t) \qquad (13)$$

Assuming that the initial temperature of conductor was 100 degrees centigrade = 373 degrees absolute, then 5,000 amperes for 1.0 second would give

$$T_1'=388 \text{ degrees absolute}=115 \text{ degrees centigrade}$$

A straight line increase from 100 degrees centigrade to 115 degrees centigrade in one second is approximately equivalent to 109 degrees centigrade throughout the one second. From Figure 6 it is seen that a temperature of 109 degrees centigrade will use up the allowable annealing of hard-drawn conductors in 1,600 hours. Since the total time at 109 degrees centigrade from short-circuit currents in 20 years is only 20 seconds, the annealing in this case is negligible. It is well to note, however, that lines having higher fault experience and slower clearing time-current characteristics very easily could be subject to partial annealing from short-circuit currents. Annealing thus becomes an additional argument for improved protection against faults and better relay protection of circuits requiring high load current ratings.

In this case, the entire allowable annealing is available for apportioning between normal recurrent loading and nonrecurrent emergency loading. Obviously certain lines are more useful with a high nonrecurrent rating and relatively low normal rating, while other lines require a relatively high normal rating and very little or no additional capacity for nonrecurrent loading. For this analysis, the allowable annealing has been apportioned two-thirds to normal loading and one-third to nonrecurrent, which factors may be applied directly to the time scale of the 51.6 per cent annealing curve of Figure 6.

For present purposes, it is convenient to work on the basis of continuous loading throughout the 20 years at 100 per cent annual load factor. Data thus will be made available for the worst possible condition to which correction factors may be applied to secure corresponding ratings for the load factor on any particular circuit. Lesser load factors can be converted into equivalent hours at maximum temperature, taking into consideration the coincident atmospheric temperature, solar heating, and wind velocity. The method used by Sealey[2] to convert atmospheric temperature data into equivalent ambient temperatures could be adapted to the conversion of load factor, but for the present it will be assumed that the equivalent time may be expressed as a percentage identical with the load factor. Taking into consideration the usual daily load factors, the variations in peak from day to day, and the shifting of load and of generating schedules over a period of 20 years, it is not likely that the load factor during 20 years will exceed 50 per cent based upon the highest load with the system operating normally, which will be represented as 50 per cent equivalent time.

Computation of Ratings

The economic life of transmission conductors has been assumed to be 20 years. It should be perfectly possible to set up an economic comparison of total annual cost per kilowatt-hour transmitted versus resulting life of conductors and so determine a more exact figure for the economic life. For present purposes, however, a 20-year life is a marked reduction below the life span previously taken for granted and will be used to establish ratings.

Twenty years constitute 175,200 hours, which is to be two thirds of the total allowable annealing time. From Figure 6, the annual equivalent copper temperature at which the total allowable annealing occurs in $^3/_2\times175{,}200$ hours (262,500 hours) is found to be 47.5 degrees centigrade. Referring to Figure 2, the conductor current corresponding to 47.5 degrees centigrade is found to be 435 amperes. This value is the maximum current which could be carried continuously for 20 years in a 4/0 hard-drawn bare copper transmission conductor fully exposed to atmospheric temperature and winds without dangerously annealing the copper.

Nonrecurrent emergency loading must be restricted to nonprearranged operating conditions not to occur more frequently than possibly five times per year if the rating is to be sufficiently higher to be of value. A convenient duration to provide time for most emergency repairs seems to be 24 hours per nonrecurrent load period. With 100 such load periods during the twenty-year life, a total of 2,400 hours must be assumed, which constitute one third of the total annealing time. From Figure 6, the annual equivalent copper temperature at which the total allowable annealing occurs in three times 2,400 hours (7,200 hours) is found to be 91 degrees centigrade, and Figure 2 shows a corresponding current of 720 amperes.

The conductor temperatures thus far have been expressed on an equivalent annual basis, assuming atmospheric temperature based upon monthly means, average wind velocity, and less than the maximum solar heating. Some indication of the amount of deviation above and below these equivalent average temperatures is given as follows:

	Equivalent Annual	Conductor Temperature	
		July 1942 (High Hour)	December 1942 (Low Hour)
Current, 100 per cent load factor amperes	435	435	435
Atmospheric temperature, degrees Fahrenheit	76.0	78.0	7.0
Solar heating, degrees Fahrenheit	3.1	0.0	0.0
Wind velocity, miles per hour	8.4*	1.0	26.0
Conductor temperature, degrees Fahrenheit	117.5	190.0	29.0
Conductor temperature, degrees centigrade	47.5	87.6	−1.7

* Squared average root equivalent at Pittsburgh of 10.5 miles per hour average at airport.

Dropping back to the 50 per cent 20-year load factor, the allowable temperatures would be these shown on Figure 6 corresponding to 50 per cent of the aggregate time for 100 per cent load factor. The annual equivalent copper temperature for normal operation is found to be 56.0 degrees centigrade corresponding to 510 amperes; the annual equivalent copper temperature for nonrecurrent loading is 99.5 degrees centigrade, corresponding to 760 amperes. With these ratings established on an annual equivalent basis, they may be modified by means of equation 10 to any desired seasonal or monthly condition.

As long as the conductor temperature does not exceed approximately 120 degrees centigrade, it is not likely that the contact surfaces in connectors and splices will become hot enough to cause rapid oxidation and consequent damage to the joint. This temperature limitation should be maximum, however, not annual equivalent, and it would seem advisable to use a somewhat lower value on the order of 100 degrees centigrade unless a detailed hour-by-hour analysis of weather data and load currents is to be conducted.

An earlier section of this paper has mentioned several reasons why transmission conductors cannot be assumed to be always in perfect condition. In addition, the entire discussion of weather data has been directed toward deriving a set of indexes to represent in one set of figures the composite effect of many variables. Finally the information available by which to incorporate annealing of the hard-drawn copper conductor in this analysis has required many approximations. In view of all these possibilities for error, it must be urged that the ratings thus derived be applied with a generous margin of safety until further investigation of the points now inadequately covered by test data permits more accurate analyses.

It is recommended for the present that the calculated ratings should be derated at least 20 per cent. Any circuits so rated should be set up for close observation of actual load and coincident atmospheric conditions with periodic tensile and elastic limit tests on samples removed from service at definite intervals of time as a check upon the validity of the calculations and as an indication of the time when the conductors should be replaced. Only by such procedure is it likely that thoroughly dependable information will become available to the industry.

Appendix

Calculation of heat dissipation from 4/0 bare conductor at 100 degrees centigrade into atmosphere at 26.2 degrees centigrade equivalent effective temperature with wind velocity of 10.5 miles per hour:

$T_2 = 100\text{ C} = 373\text{ K}$

$T_1 = 79.1\text{ F} = 26.2\text{ C} = 299.2\text{ K}$

$$T_a = \frac{T_2 + T_1}{2} = \frac{672.2}{2} = 336.1\text{ K}$$

$E = 0.50$

$D = 0.528\text{ in.} = 1.340\text{ cm}$

$P = 0.96$ atmosphere

$V = 40.5\%$ of $10.5 = 4.25$ mph $= 190$ cm per sec

$$W_R = 18.55 \times 10^{-12} \times 1.340[(373)^4 - (299.2)^4]0.5$$
$= 0.142$ watt per cm length $= 4{,}330$ watts per 1,000 ft

$T_a^{0.754} = \log_{10}^{-1}(0.754 \times 2.527) = 79.8$

$(\phi_2 - \phi_1) = 3.46 \times 10^{-4} \times 79.8)(73.8) = 0.0204$

$D^{0.50} = 1.16 \quad p^{0.50} = 0.98 \quad V^{0.50} = 13.8$

$T_a^{0.877} = \log_{10}^{-1}(0.877 \times 2.527) = 165.0$

$$B = \frac{0.00572 \times 1.16 \times 1{,}650}{0.98 \times 13.8} = 0.081\text{ cm}$$

$$S = \frac{2\pi}{\log_e (1.121)} = \frac{2\pi}{0.114} = 55.1$$

$W_c = 55.5(0.0204) = 1.130$ watts per cm length $= 34{,}450$ watts per 1,000 ft

$W = 1.272$ watts per cm, $= 38{,}780$ watts per 1,000 ft

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Interim Report on Characteristics and Performance of Conductors for Supervisory Control and Telemetering

AIEE SUBCOMMITTEE ON CHARACTERISTICS AND PERFORMANCE OF CONDUCTORS FOR SUPERVISORY CONTROL AND TELEMETERING

Preface: The present war emergency requires that the maximum use be made of existing equipment and systems and that a minimum of critical material be used for new equipment.

This publication and other guides and reports of this series have been prepared for the information of users during the war emergency. Upon termination of the war emergency, they will be reconsidered by the Standards committee and the committees which prepared them, and will be approved, revised for normal use, or rescinded.

This procedure is being followed in preference to the preparation of special emergency Standards which might involve redesigning and drastic changes in manufacturing practices. These guides will accomplish the maximum conservation of critical materials, since they provide for the maximum use of existing equipment and systems, as well as new equipment, without changing the fundamental basis on which the present Standards have been prepared.

DURING 1941, in collaboration with the relay subcommittee of the committee on protective devices,[*] a questionnaire with regard to pilot conductors[**] for supervisory control and telemetering systems, was sent to a number of operating companies by the committee on automatic stations.

The number of reporting companies or governmental units (25) represents but a small portion of those to whom the questionnaire was sent, and the summary may not, therefore, represent completely the practice and experience of the majority of those in the electric-power industry using pilot conductors for supervisory control and telemetering. It is believed, however, that within its limitations as a sample, it is indicative of certain features and is being presented with this view in mind.

Summary of Data

The summary is based on the data covering 189 circuits totaling slightly under 1,200 circuit-miles and slightly over 9,000 circuit-mile-years. The generalizations permissible from the data are:

(*a*). Of the 189 circuits of an average length[†] of 6.3 miles—only 15 per cent being over ten miles in length[†]—about 55 per cent utilized privately owned pilot conductors. Of 75 of these privately owned pilot conductors, 84 per cent were close to power circuits. Of all the circuits—both privately owned and leased—for which data were received, less than 15 per cent used open-wire construction for even a part of the circuit. Only about ten per cent used a ground-return path for any portion of the circuit.

(*b*). Considering the performance of the pilot conductors alone, the out-of-service time is not over $^1/_{20}$th of one per cent of the total time. While this figure is significant, data are not available showing what part of the *total out-of-service* time of the supervisory-control or telemetering systems may be due to the pilot conductors.

(*c*). It may be noted—see section VII of the summary—that two bases for summarizing the service performance have been used. Basis *A* indicates that the leased pilot conductors had about double the outages per circuit-mile-year of the privately owned conductors. The average duration of an outage for the leased conductors was slightly less than that for the privately owned conductors. Basis *B* indicates that the leased conductors had about one-half the outages per circuit-mile-year of the privately owned conductors and a slightly longer average duration for an outage. Considering both bases for summarizing, it appears that there is no substantial difference between the performances of privately owned and of leased pilot conductors. There is also indication that:

1. There is a continuing improvement in the degree of service continuity for pilot conductors. Most of the troubles for the systems occurred during their initial periods and, in some instances involving the older systems, these troubles largely occurred ten years or more ago.
2. This improvement appears to be due largely to an increased use of cable conductors and to more effectively designed and co-ordinated terminal protection.

(*d*). The special information covered in section VIII of the summary indicates that for new pilot conductors:

1. There is a preference for automatic supervision.
2. Cable conductors are desired.
3. The protective terminal equipment is more frequently connected to the power station ground than (*a*) to a remote ground or (*b*) left ungrounded.

Detailed Summary of Answers to Questionnaire

I. General

(*a*).

Number of companies and governmental units submitting answers	25
Number of circuits for which some data were provided	189

(*b*). Uses for circuits

1. Circuits for supervisory control and/or indication	80
2. Circuits for telemetering	31
3. Circuits for both	13
4. Circuits for systems not identified	65

(*c*). Ownership of circuits (question 1 of questionnaire of March 12, 1941—see appendix)

1. Privately owned throughout	106
2. Leased throughout	81
3. Partially leased and partially privately owned	2

(*d*). Type of construction (question 2)

1. Circuits wholly of cable construction	163
2. Circuits wholly of open-wire construction	17
3. Circuits of both cable and open-wire	8
4. Not indicated	1

(*e*). Proximity of supervisory or indicating conductors to power circuits (question 3)

	Near	Distant	Both
1. Privately owned	63	12	
2. Leased	5	65	3
3. No information			41

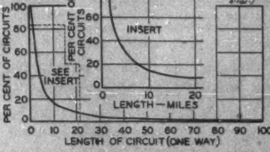

Figure 1. Cumulative per cent curve of circuits versus length

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Paper 43-101, recommended by the AIEE committee on automatic stations for presentation at the AIEE national technical meeting, June 21–25, 1943. Manuscript submitted April 21, 1943; made available for printing May 19, 1943.

Personnel of subcommittee on characteristics and performance of conductors for supervisory control and telemetering: R. F. Davis, *chairman;* M. E. Reagan, J. R. Harrington, Perry Peterson.

This interim report was prepared by the AIEE subcommittee on characteristics and performance of conductors for supervisory control and telemetering of the committee on automatic stations for the purpose of making essential information immediately available to war industries, thus furthering the conservation of valuable material for the war emergency. It is educational and in no way mandatory. It is not intended as a "Standard" and has not been formally approved by the Standards committee nor the board of directors.

The committee on automatic stations expresses its appreciation to the 25 companies and governmental units which furnished the data in the "Detailed Summary of Answers to Questionnaire."

[*] The results of the questionnaire with regard to pilot-wire circuits used for protective relaying were covered in a report of the committee on protective devices which was presented at the AIEE national technical meeting, January 25–29, 1943.[1]

[**] The term, "pilot conductors," as used in this report should not be confused with conductors used for protective-relaying systems, even though the conductor characteristics may be quite similar.

[†] In the case of the 174 circuits for which the length was stated.

(*f*). Type of channel used (question 4)

1.	Completely metallic	168
2.	Ground-return	9
3.	Operating on phantom*	11
4.	Part metallic and part ground-return	1

II. Characteristics of Privately Owned Conductors

(*a*). Size of conductor—number of circuits of various gauges (question 5)

Number 8 copper or copper-weld	*Number 12 copper*	*Number 14 copper*	*Number 16 copper*	*Number 19 copper*	*Number 22*
11	42	4	12	21	8

Miscellaneous (not over 2 of any gauge)	*Not given*
8	2

(*b*). Type of insulation—number of circuits having various insulating material (question 6)

Rubber	*Paper*	*Open-wire (bare)*	*Not given*
47	39	21	1

(*c*). Size of cable—number of circuits involved in cables which have (question 7)

1–5 conductors	*6–10 conductors*	*11-20 conductors*	*21-50 conductors*
10	35	19	11

51–100 conductors	*101–202 conductors*	*Not given*
7	9	1

(*d*). Arrangement of conductors in cables—number of circuits where conductors were (question 8)

Nontwisted	*Twisted*	*Quadded*	*Not given (or indefinite)*
39	34	5	6

(*e*). Type of sheath—number of circuits in cables having (question 9)

Lead sheath	*Nonmetallic sheath*	*Not given*
79	5	1

III. Length, Resistance, and Loading of Conductors

(*a*). Length (question 10—see Figure 1)

Average of 174 circuits for which length was given	6.3 miles

Range: Minimum of 0.28 mile to maximum of 112.9 miles

(*b*). Resistance (question 11—see Figure 2)

Average of 115 circuits for which resistance was given	475 ohms

Range: Minimum of 6.5 ohms to maximum of 4,000 ohms

(*c*). Loaded or nonloaded (question 12)

149 circuits nonloaded
21 circuits loaded
2 circuits partly loaded and nonloaded
17 circuits no information given

IV. Protection at Terminal of Circuit

(*a*). Number of circuits indicated as having the following protective devices (question 13):

Carbon blocks	65
Air gaps	6
Thyrite	4
Protective tubes	55
Vacuum gaps	8
Fuses	101
Heat coils	5
Relay protectors	7
Other devices	9

Circuits indicated as having

One type of protective device	66
Two types of protective devices	55
Three types of protective devices	25
Four types of protective devices	2

(*b*). Circuits indicated as having no protective devices.......... 5

(*c*). Circuits for which no information was given.......... 38

IV*A*. Breakdown Voltage of Protective Devices Summarized in Item IV(a)

(*a*). At 60 cycles (question 14*a*)

*0–100 volts**	*101–250 volts**	*251–350 volts**	*351–600 volts**	*Not given*
9	15	22	21	122

(*b*). Impulse voltages (question 14*b*)

No information furnished

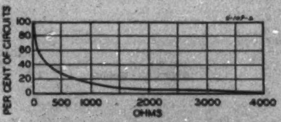

Figure 2. Cumulative per cent curve of circuits versus loop resistance of conductors

IV*B*. Protection Against Potential Gradients (Question 15)

Neutralizing transformers	4 circuits
Insulating transformers*	6(1) circuits
Insulating relay equipment	31 circuits
Drainage**	16 (3) circuits
Relay Protectors**	(2) circuits
Series impedance	2 circuits
No special protection	37 circuits
No information given	94 circuits

IV*C*. Insulation Level of Pilot Wires (Question 16)

0–100 volts	*101–499 volts*	*500 volts*	*1,500 volts*	*3,500 volts*	*15,000 volts*	*No data given*
7 circuits	2 circuits	54 circuits	5 circuits	1 circuit	2 circuits	118 circuits

This is assumed to be the dielectric strength (conductor-to-ground), as determined by manufacturers' tests, which is usually in rms volts.

V. Protection on Pilot Wires at Central Offices and Other Remote Points from Terminal of Circuit (Question 17)

(*a*). 55 circuits equipped with carbon blocks
(*b*). 1 circuit equipped with air gaps
(*c*). 6 circuits equipped with protector tubes
(*d*). 29 circuits equipped with fuses
(*e*). 8 circuits equipped with heat coils
(*f*). 1 circuit equipped with relay protectors
(*g*). 8 circuits having no protection
(*h*). 23 circuits having two of above devices
(*i*). 10 circuits having three of above devices
(*j*). 124 circuits for which no data was given

V*A*. Breakdown Voltage of Protective Devices Summarized in item V

(*a*). At 60 cycles (question 18*a*)

0–200 volts	*201–400 volts**	*401–600 volts**	*No data given*
7 circuits	21 circuits	15 circuits	146 circuits

(*b*). Impulse voltages (question 18*b*)

No information furnished.

VI. Special Markings or Other Precautionary Measures Taken to Prevent Inadvertently Interrupting Pilot Wires (Question 19)

(*a*) *Circuits with special markings*	*Circuits having no special markings*
74	18

(*b*) Circuits having other special features
1 circuit having special routing
1 circuit having notification before testing

(*c*) *Circuits for which no data were given*
95

VII. Performance of Pilot-Wire Conductors

(*a*). General (questions 10, 20, 21 and 22—see Table II)

Two bases for summarizing have been used. Basis *A* consists of averaging all of the data furnished, even though various elements of the information do not apply to the same circuit, for example, for "years of service" data were furnished for 71 privately owned circuits and 70 leased circuits, and for "number of outages" the data cover 60 privately owned circuits and 59 leased circuits but *not necessarily for the same circuits*. Basis *B* consists of averaging *only* those circuits for which complete data for *all* four of the items of information were furnished.

(*b*). Causes of service outages (question 23)

Total outages for which causes were indicated	475
Due to short circuit of pilot-wire conductors	120 (25.3 per cent)
Due to open circuit of pilot-wire conductors	119 (25.1 per cent)
Due to circuit rearrangement of pilot-wire conductors	32 (6.7 per cent)
Due to operation of terminal protectors on pilot-wire conductors	70 (14.7 per cent)
Due to miscellaneous causes	134 (28.2 per cent)

of which 85 were due to failure of supervisory equipment; 36 were due to low insulation during fogs and floods; and the remaining 13 were from scattered causes

VIII. Miscellaneous Information and Comments

Of the various questions listed under the heading "Additional Information" in the appendix, the data received were adequate or suitable for summarizing for the following items:

(*a*). Are pilot-wire conductors automatically supervised? (question 24)

Yes......43 circuits No......110 circuits

(*b*). Are pilot-wire conductors used for other purposes than solely supervisory control and telemetering?—(question 25)

Yes......15 circuits No......182 circuits

(*c*). Are pilot-wire conductors subject to troubles from induction effects or surges?—(question 26)

Yes......22 circuits No......180 circuits

(*d*). Various comments regarding preferences for new pilot-wire conductors—(questions 27 and 28)

1. No. of companies answering............Maximum of 6

(Text concluded on page 862)

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* Often (10) on ground-return basis.

* Assumed to be peak volts although not specified.

** These protective elements were in some cases—as indicated by the figures in parentheses—combined with one or more other protective elements.

Table I

Designation of Power Company	Type of Circuit or Identification**	Quest. 1		Quest. 2		Quest. 3		Quest. 4			Quest. 5	Quest. 6	Quest. 7	Quest. 8			Quest. 9	Quest. 10	Quest. 11	Quest. 12	
		Leased	Private	Cable	Open-Wire	Near	Distant	Metallic	Grd. Return	Phan.	Size of Cond.	Cond. Insulation	No. of Conds. in Cable	Untwisted	Twisted Pairs	Quadded	Type of Sheath	Length (Mi. One Way)	Res. of Loop Exc. Term. Eq. (Ohms)	Loaded	Nonloaded
Company *A*	Tm.		x		x		x	x			#10	O.W.						12	126		x
Company *B*	Tm.		x		x	x		x			#6	O.W.		x				4.5	—		x
Company *C*	Both		x	x		x		x			#19	Paper	20	x			Pb	4	164	x	
	Both		x	x		x		x			#19	Paper	20	x			Pb	2	82	x	
	Both		x	x		x		x			#19	Paper	12	x			Pb	5	210	x	
	Both		x	x			x	x			#19	Rubber	12	x			Pb	2.5	103	x	
	Ind.		x		x	x		x			#12	Rubber	1	x			Pb	1	8	x	
	Tm.		x		x	x		x			#12	Rubber	1	x			Pb	1	8		x
	Both	x		x			x	x												x	
	Ind. & Tm.	x		x			x	x												x	
	Ind.	x		x			x	x												x	
	Ind.	x		x			x	x												x	
	Both	x		x			x	x												x	
	Both	x		x			x	x										1.5	300	x	
	S.c.	x		x			x	x										1.5	300	x	
	Tm.	x		x			x	x										1.5	300	x	
	S.c.	x		x			x	x										1.5	300		x
	S.c.	x		x			x	x										1.5	300	x	
	S.c.	x		x			x	x										1.5	300	x	
Company *D*	S.c.		x	x	x	x		x			#8 C.W. 19/22 Cu.	— Rubber	1 2		Trans.		Pb	1.5	30		x
Company *E*	Tm.	x		x			x	x									Pb	3			x
	Tm.	x		x			x	x									Pb	3			x
Company *F*	Tm.	x		x						Ph-g.								14.5	3,500		x
	Tm.	x		x						Ph-g.								13.25	3,300		x
	Tm.	x		x						Ph-g.								3	700		x
	Tm.	x		x						Ph-g.								3	700		x
	Tm.	x		x						Ph-g.								7	1,600		x
	Tm.	x		x						Ph-g.								6	1,200		x
	Tm.	x		x						Ph-g.								36.75	4,000		x
	S.c.		31	x				x			#12	$^{3}/_{64}$ R	7	x			Pb	0.8–4.5 (2.2 avg.)	36.2		x
Company *G*		x		x			x	x										12			
		x		x			x	x										½			
		x	x		x	x (P)	x (L)	x (L)	x (P)									5			
		x		x			x	x										4			
		x		x			x	x										3			
Company *H*		x		x		x		x										2			x
		x		x		x		x										3			x
		x		x		x		x										2			x
		x		x			x	x										4			x
		x		x			x	x										5½			x
		x		x		x		x										6			x
		x		x			x	x										4½			x
		x		x			x	x										1½			x
Company *I*		x		x			x	x													x
		x		x			x	x													x
		x		x			x	x													x
		x		x			x	x													x
		x		x			x	x													x
		x		x			x	x													x
			x	x		x		x			#12	R	3				R	5.9			x
			x	x		x		x			#12	R	3				R	9.5			x
			x	x		x		x			#12	R	3				R	10.3			x
			x	x		x		x			#12	R	3				R	5.1			x
		x		x			x	x										6.3			x
			x	x	x	x		x			#14	R	3				Pb	2.7			x
Company *J*		x		x	x		x	x										28.5	3,317	22.5	6.0
		x		x			x	x										14.1	1,730	5.2	8.9
		x		x			x	x										2.13	514	0	2.13
Company *K*	S.c.	x		x			x	x										5½			x
	S.c.	x		x			x	x										4			x
	S.c.	x		x			x	x										1½			x
	S.c.	x		x			x	x										2.4			x
	S.c.	x		x			x	x										1½			x
	S.c.	x		x			x	x										0.7			x
	S.c.	x		x			x	x										1½			x
	S.c.	x		x			x	x										1			x
	S.c.	x		x			x	x										1			x
Company *L*		x		x	x		x	x										8	680		x
Company *M*			x		x		x			x	102 Cu.							112.9			
		x		x			x	x										4	1,459		x
		x			x		x	x			109 Fe.							4	280		
			x		x		x	x			#10 AWG							30	300		
Company *N*			x	x		x		x			#19	Paper	20		x		Pb	1.12	95		x
			x	x		x		x			#19	Paper	20		x		Pb	2.9	247		x
			x	x		x		x			#19	Paper	20		x		Pb	3.55	302		x
			x	x		x		x			#19	Paper	20		x		Pb	7.38	627		x
			x	x		x		x			#19	Paper	20		x		Pb	4.1	349		x
			x	x		x		x			#19	Paper	20		x		Pb	13.6	1,156		x
			x	x		x		x			#19	Paper	20		x		Pb	6.58	560		x
			x	x		x		x			#19	Paper	20		x		Pb	10.5	893		x

S.c.—Supervisory Control. Tm.—Telemetering. Ind.—Indicating or Sup. Ind. *Serjedetours. † Relay protectors.

Table I (Continued)

		Quest. 13 Terminal Protection								Quest. 14 Breakdown		Quest. 15 Potential Grad. Pot.				Quest. 16	Quest. 17 Treatment in Tel. Exchanges or Other Remote Pts.								Quest. 18 Breakdown of Item 17	Quest. 19
Designation of Power Company	Type of Circuit or Identification**	Carbon Blocks	Air Gaps	Thyrite	Protective Tubes	Vacuum Gaps	Fuses	Heat Coils	Relay Protect.	60 Cycles	Impulse	Neutralizing Trans.	Line Coup'g Trans.	Ins. Relay Equipment	Drainage Equipment	Ins. Level of Pilot Wire to Sta. Grd.	Carbon Blocks	Air Gaps	Thyrite	Protective Tubes	Vacuum Gaps	Fuses	Heat Coils	Relay Protect.	60 Cycles Impulse	Special Marking
Company *A*	Tm.	x				x				400–600 ▾				x			x				x				400–600 ▾	
Company *B*	Tm.			x						250 ▾			x													
Company *C*	Both				x					250 ▾																
	Both				x					250 ▾																
	Both				x					250 ▾																
	Both				x																					
	Ind.						x																			Yes
	Tm.						x																			Yes
	Both						x																			Yes
	Ind. & Tm.						x																			Yes
	Ind.						x																			Yes
	Ind.						x																			Yes
	Both						x																			Yes
	Both						x																			Yes
	S.c.																									Yes
	Tm.						x																			Yes
	S.c.						x																			Yes
	S.c.																									Yes
	S.c.																									Yes
Company *D*	S.c.				x		x			600 ▾																Special route
Company *E*	Tm.	x								350 ▾																
	Tm.	x								350 ▾																
Company *F*	Tm.						x							x		500 ▾	x									Yes
	Tm.						x							x		500 ▾	x									Yes
	Tm.						x							x		500 ▾	x									Yes
	Tm.						x							x		500 ▾	x									Yes
	Tm.						x							x		500 ▾	x									Yes
	Tm.						x							x		500 ▾	x									Yes
	Tm.						x							x		500 ▾	x									Yes
	S.c.															500 ▾										
Company *G*		x										x														
		x																								
		x			x										x											
Company *H*		x			x		x									1500										Yes
		x			x		x								x & scr's†	1500										Yes
		x			x		x								& scr's	1500										Yes
		x			x		x																			Yes
		x			x		x																			Yes
		x			x		x									1500										Yes
		x					x																			Yes
		x			x		x									1500										Yes
Company *I*					x		x																			Yes
					x		x																			Yes
					x		x																			Yes
					x		x																			Yes
					x		x																			Yes
					x		x																			Yes
									x																	
									x																	
									x																	
									x																	
					x		x													x		x				Yes
									x																	
Company *J*		x					x							Series mp.			x					x	x			Yes
		x					x							Series imp.			x					x	x		350	Yes
		x					x										x					x	x		350	Yes
Company *K*	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
	S.c.	x	x				x										x	x				x				Yes
Company *L*		x			x		x							Nothing special			x					x			350	See note
Company *M*		x					x		x		350		x													
		x									350	x					x								350	
		x					x				350						x								350	No
							x		x		350						x					x		x	350	No
Company *N*					x		x				250			x		500										
					x		x				250			x		500										
					x		x				250			x		500										
					x		x				250			x		550										
					x		x				250			x		500										
					x		x				250			x		500										
					x		x				250			x		500										
					x		x				250			x		500										

S.c.—Supervisory Control. Tm.—Telemetering. Ind.—Indicating or Sup. Ind. * Serjedetours. † Relay protectors.

Table I (Continued)

		Quest. 20	Quest. 21	Quest. 22	Quest. 23 No. of Outages Due to:				Quest. 24	Quest. 25	Quest. 26	
Designation of Power Company	Type of Circuit or Identification**	Years of Service	No. of Outages During Service	Total Outage Time (Hrs.)	Short Circuits	Open Circuits	Circuit Rearrangements	Terminal Protect.	Pilot Wire Automatically Super'd?	Pilot Wire Used for Other?	Troubles Due to Induction or Surges?	Miscellaneous Information as Covered in Questionnaire Under "Additional Information." In General These Data Were Not Suitable for Summarizing
Company *A*	Tm.	3	6	1 day per outage—no need for cont.	2				No	No	No	
Company *B*	Tm.	9										No troubles due to surges.
Company *C*	Both		2	Neg.		2			No	No	No	$63 per pr. per yr. for about 1 mile.
	Both								No	No	No	Privately owned supervisory and control cable is located on same structure and *above* power line. Control cable is lead-covered and protected at ends with protective tubes.
	Both								No	No	No	
	Both								No	No	No	
	Ind. Tm.								Yes	Yes	No	
	Tm.								No	No	No	
	Both								Yes	Yes	No	Protective tubes and fuses preferred.
	Ind. & Tm.		8	Neg.	1	5	2		Yes	No	No	Terminal equipment is ungrounded.
	Ind.		6	Neg.	4	1	1		Yes	No	No	No monitoring current. Audible alarm and supervisory signals on controlled equipment.
	Ind.		13	Neg.	2	8	3		Yes	No	No	
	Both								Yes	Yes	No	
	Both		5	Neg.	3		2		Yes	Yes	No	For new installations—separate routed, u.g. and metallic covering to prevent interference or tampering. Automatic superv.
	S.c.		1	Neg.	1				Yes	No	No	
	Tm.								Yes	No	No	
	S.c.								Yes	No	No	
	S.c.		2	Neg.	2				Yes	No	No	
	S.c.		1	Neg.	1 or 2				Yes	No	No	
Company *D*	S.c.	4	0	0	0	0	0	0	Yes	No	No	Prot. tubes grounded to station grounds. Lightning arrester on o.w. connected to C/W grd. rods. Auto-super. on new.
Company *E*	Tm.	12	Some							No	No	
	Tm.	10								No	No	
Company *F*	Tm.	9					x	x	No	No	No	In city $4.50 for 1st ½ mile plus $1.25 per ½ mile per month.
	Tm.	8		Neg.			x	x	No	No	No	Outside city $3.33 per mile per month.
	Tm.	14		Neg.			x	x	No	No	No	Cap. and ldng. of PW does not affect performance.
	Tm.	14		Neg.			x	x	No	No	No	
	Tm.	14	Norec.	Neg.			x	x	No	No	No	Telem. ccts. to sta. grd.
	Tm.	14	Neg.	Neg.			x	x	No	No	No	New telem. & s.c. circuits should be same as now used. S.c. circuits use more current than permitted over leased facilities.
	Tm.	5		Neg.			x	x	No	No	No	
	S.c...17 old	J			x		x		No	No	No	
Company *G*		2							Yes	No	No	Approx. $2 per route mile.
		2							Yes	No	No	Grded. to sta. grds.
		L = 9 P = 12	No record	No record					No	Tel.&Sig.	Yes	Prefers leased metallic—wants automatic supervision.
		9							No	No	No	
		6							No	No	No	
Company *H*		13	3	9½	1		2		Yes	No	Yes	Approx. $2 per route mile.
		14	17	54½	10	1	1	4	Yes	No	Yes	On opp. side of st. from 24 kv.
		14	19	89½	13	2	1	3	Yes	No	Yes	C and ldg. does not affect.
		14	10	75½	4	4	1	1	Yes	No	Yes	Remote grd. in some cases.
		10	2	6	1	1			Yes	No	Yes	Grds. on PW are detected.
		13	12	108	4	5	1	2	Yes	No	Yes	Inst. new prot. tubes and relay protectors.
		13	2	6	1	1			Yes	No	Yes	
		13	9	35	2	4	1	2	Yes	No	Yes	Auto. super. for new.
Company *I*		5	5	2½					Yes	No	No	$.75 per ½ mile air line.
		5	5	2½					Yes	No	No	
		5	5	2½					Yes	No	No	
		5	5	2½					Yes	No	No	
		5	5	2½					Yes	No	No	
		5	5	2½					Yes	No	No	
		4	1	5		1			Yes	No	No	
		4	1	5		1			Yes	No	No	
		4	0	0					Yes	No	No	
		2	0	0					Yes	No	No	
		2			1				Yes	No	No	
		2	0	0					No	No	Yes	
Company *J*		9	26	268	14	10	2	0	Yes	No	Yes	$4 per mile per month shortest wire route.
		3	1	1½	0	1	0	0	Yes	No	No	On new installations prefer leased circuits for shorter distances and power line carrier for longer routes.
		1	0	0	0	0	0	0	Yes	No	No	
Company *K*	S.c.	1	1	2	1				No	No	No	
	S.c.	1½	0	0					No	No	No	$4 per mile.
	S.c.	1½	0	0					No	No	No	
	S.c.	3	0	0					No	No	No	
	S.c.	1½	0	0					No	No	No	
	S.c.	3	0	0					No	No	No	
	S.c.	2	0	0					No	No	No	
	S.c.	3½	0	0					No	No	No	
	S.c.	3½	0	0					No	No	No	
Company *L*		3½		4–5 days	70% (ins.)	10%		20%				Tel. co. notifies before testing.
Company *M*		1½							No	Yes	No	$2.50 per cct. mile
		1½	No record	No record					No	No	No	One circuit is Ph. with GE ins. coil and WE 76A rept. coil.
		1							No	Yes	No	
		1½							No	Yes	Yes	Exp. OW have high pot. GE ins. coils plus relay protectors.

S.c.—Supervisory Control. Tm.—Telemetering. Ind.—Indicating or Sup. Ind. * Serjedetours. † Relay protectors.

Table I (Continued)

Designation of Power Company	Type of Circuit or Identification**	Quest. 1		Quest. 2		Quest. 3		Quest. 4			Quest. 5	Quest. 6	Quest. 7	Quest. 8			Quest. 9	Quest. 10	Quest. 11	Quest. 12	
		Leased	Private	Cable	Open-Wire	Near	Distant	Metallic	Grd. Return	Phan.	Size of Cond.	Cond. Insulation	No. of Conds. in Cable	Untwisted	Twisted Pairs	Quadded	Type of Sheath	Length (Mi. One Way)	Res. of Loop Exc. Term. Eq. (Ohms)	Loaded	Nonloaded
Company O	S.c. & I.		x	x		x		x			#12	R	7	x			Pb	0.57			x
	S.c. & I.		x	x		x		x			#14	R	12	x			Pb	0.54			x
	S.c. & I.		x	x		x		x			#14	R	4	x			Braid	7.66			x
	HT tel. line		x		x	38′		x			#6							6.2			x
	HT tel. line		x		x	x		x			#9							28.0			x
	Tm.	x		x			x	x										12.75	2,985		x
	Ind.	x		x			x	x										3.75	650		x
	S.c. & I.	x		x			x	x										2.5	556		x
	Ind.		x	x		x		x			#14	P	6	x			Pb	0.28			x
Company P			x		x	x		x			#8	4-kv. ins.		x				7½			
			x	x		x		x			#19	R + braid	4-used	x			Pb	1	9.9		x
			x		x	x		x			#8	4-kv ins.		x				3			
			x		x	x		x			#8	4-kv ins.		x				6½			
			x		x	x		x			#8	4-kv ins.		x				6			
			x		x	x		x			#8	4-kv ins.		x				1½			
			x		x	x		x			#8	4-kv ins.		x				6			
			x		x	x		x			#8	4-kv ins.		x				½			
Company Q	S.c.		x	x			x	x			#19	Paper	52		x		Pb	2.7	227		x
	S.c.		x	x			x	x			#19	Paper	152		x		Pb	2.2	185		x
	S.c.		x	x			x	x			#22	Paper	102		x		Pb	2	330		x
	S.c.		x	x			x	x			#19	Paper	52		x		Pb	3.1	267		x
	S.c.	x			x		x	x										1	48		x
	S.c.	x	x	x			x	x										2	330		x
	S.c.		x		x	x		x			#8 Cu. Cl.							1.5	37		x
	S.c.	x		x			x	x										7	915		x
Company R	S.c.		x	x		x		x			#12	4/64"	10				Pb	3.27	66.5		
Company S	Tm.	x		2½	½	½ 40′	2½	x										3			x
	S.c.	x		x			x	x										1.75	287		x
Company T	S.c.	x						x													x
	S.c.		x	x		x		x			#22	Single Paper	202		x		Pb	0.75			x
	S.c. & tm.		x	x		x		x			#22	Single Paper	82		x		Pb	1.9			x
	S.c.	x		x			x	x										1.5			x
	S.c. & tm.		x	x		x		x			#22	Single Paper	82		x		Pb	1.35			x
	S.c. & tm.		x	x		x		x			#22	Single Paper	52		x		Pb	1.85			x
	Ind.	x		½	½	½	½	x										1.0			x
	Ind.		x	x			x	x			#19	Double Paper	202		x		Pb	1.46	120		x
	S.c. & tm.		x	x			x	x			#19	Double Paper	102		x		Pb	3.05	264		x
	Ind.		x	x		x		x			#22	Single Paper	152		x		Pb	2.11	360		x
	S.c. & tm.		x	x			x	x			#19	Double Paper	202		x		Pb	0.33	27		x
	S.c.	x		½	1½	1½	½	x										1.75			x
Company U	S.c. & tm.		x	x		x		x			#22	Paper 1,000–3,500	52	x			Pb	3	570		x
Company V	Tm.		x		x	x				Ph-g		12 kv						77			x
	Tm.		x		x		x			Ph-g		12 kv						7			x
Company W	S.c.	x		x			x	x										11.4	1,554	x	
	S.c.	x		x			x	x										3.26	511	x	
	S.c.	x		x			x		x									8.95	268	x	
	Tm.	x		x			x		x									11.4	1,153	x	
	S.c.	x		x			x	x										5.75	828	x	
	Tm.	x		x			x		x									20.5	1,435	x	
	Tm.	x		x			x		x									20.6	1,514	x	
	Tm.	x		x			x		x									29.3	1,485	x	
	Tm.	x		x			x		x									1.8	457	x	
	Tm.	x		x			x		x									1.7	441	x	
	Tm.	x		x			x	x										1.8	457	x	
	Tm.	x		x			x		x									1.8	457	x	
	S.c.	x		x			x	x										5.4	1,010	x	
	Tm.	x		x	x		x			Ph-g 5.2 mi.								37.5	1,554	x	
Company X	S.c.		x		x	x		x			#17	Rubber	2		x		Fabric	2.1	337		x
	Ind.		x	x		x			x		#12	Rubber	3		x		Pb	1.6	21		x
	Ind.		x	x		x		x			12/8	Rubber	3		x		Pb	1.9	29		x
	Ind.	x		x		x		x				Paper					Pb		412		x
	Ind.	x		x			x	x				Paper					Pb		919		x
	Ind.		x	x		x		x			#12	Rubber	42		x		Pb	0.76	6.5		x
	Tm.		x	x		x		x			#8	Rubber	7		x		Pb	3.5	26		x
	Ind.		x		x	x		x			#17	Rubber	2		x		Fabric	0.3	47		x
Company Y			x	x		x		x			#16	Paper	48/12		x		Pb	21.8			x
			x	x		x		x			#16	Paper	48/12		x		Pb	21.8			x
			x	x		x		x			#16	Paper	48/12		x		Pb	21.8			x
			x	x		x		x			#16	Paper	48/12		x		Pb	21.8			x
			x	x		x		x			#16	Paper	48/12		x		Pb	21.8			x
Company Z	S.c.		x	x		x		x			#19	Paper	120		x		Pb	0.9	78.4		x
	S.c.		x	x		x		x			#16	Paper	50		x		Pb	1.2	35		x
	S.c.		x	x		x		x			#16	Paper	50		x		Pb				x
	S.c.		x	x		x		x			#16	Paper	50		x		Pb	0.85	36		x
	S.c.		x	x		x		x			#16	Paper	70		x		Pb	0.6	25		x
	S.c.		x	x		x		x			#16	Paper	50		x		Pb	2.95	125		x
	S.c.		x	x		x		x			#16	Paper	50		x		Pb	0.7	30		x
	S.c.		x	x		x		x			#16	Paper	200		x		Pb	1.0	41		x
	S.c.		x	x		x		x			#16	Paper	28		x		Pb	3.2	137		x

S.c.—Supervisory Control. Tm.—Telemetering. Ind.—Indicating or Sup. Ind. * Serjedetours. † Relay protectors.

Table I (Continued)

		Quest. 13 Terminal Protection								Quest. 14 Breakdown		Quest. 15 Potential Grad. Prot.				Quest. 16	Quest. 17 Treatment in Tel. Exchanges or Other Remote Pts.								Quest 18 Breakdown of Item 17	Quest. 19
Designation of Power Company	Type of Circuit or Identification**	Carbon Blocks	Air Gaps	Thyrite	Protective Tubes	Vacuum Gaps	Fuses	Heat Coils	Relay Protect.	60 Cycles	Impulse	Neutralizing Trans.	Line Coup'g Trans.	Ins. Relay Equipment	Drainage Equipment	Ins. Level of Pilot Wire to Sta. Grd.	Carbon Blocks	Air Gaps	Thyrite	Protective Tubes	Vacuum Gaps	Fuses	Heat Coils	Relay Protect.	60 Cycles Impulse	Special Marking
Company *O*...	S.c. & I.																									
	S.c. & I.																									
	S.c. & I.					x	x																			
	HT tel. line	x				x	x						x		x											
	HT tel. line	x				x	x						x													
	Tm.						x										x						x			Yes
	Ind.						x										x						x			Yes
	S.c. & I.						x										x						x			Yes
	Ind.																									
Company *P*					x	Sj*	x									16										
							x									65										
			x		x	Sj*	x									115										
					x	Sj*	x									50										
			x		x	Sj*	x									115										
			x			Sj*	x							x		65										Yes
			x			Sj*	x							x		65										Yes
					x	Sj*	x									50										
Company *Q*..	S.c.	x	At disp. only					x		350				x		500 v										
	S.c.	x	At disp. only					x		350				x		500 v										
	S.c.	x	At disp. only					x		350				x		500 v										
	S.c.	x	At disp. only					x		350				x		500 v										
	S.c.					x				450				x		500 v										
	S.c.	x						x		350				x		500 v	x						x		350	
	S.c.					x				450				x		500 v										
	S.c.	x								350		x				500 v	x						x			
Company *R*..	S.c.				x		x			300–500																
Company *S*...	Tm.	x		x						500	on ord.						x					x			500	Yes
	S.c.	x															x					x			500	Yes
Company *T*..	S.c.	x															x					x			500	Yes
	S.c.	x					x																		500	
	S.c. & tm.	x					x			500															500	
	S.c.	x					x										x					x			500	Yes
	S.c. & tm.	x					x																			
	S.c. & tm.						x																			
	Ind.	x					x										x					x			500	Yes
	Ind.	x					x			500																
	S.c. & tm.	x					x																			
	Ind.	x					x																			
	S.c. & tm.						x																			
	S.c.	x					x										x					x			500	Yes
Company *U*..	S.c. & tm.				x					350						3,500										
Company *V*..	Tm.		x			x	x						x		x	15 kv.										
	Tm.		x			x	x						x		x	15 kv.										
Company *W*..	S.c.				x		x			500		Not used					x								350	Yes
	S.c.			x			x			150		Not used					x								350	Yes
	S.c.						x					Not used					x								350	Yes
	Tm.				x		x			90		Not used					x								350	Yes
	S.c.			x			x			150		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
	S.c.				x					150		Not used					x								350	Yes
	Tm.				x					90		Not used					x								350	Yes
Company *X*..	S.c.				x		x		HiVF	400					x											Unnec.
	Ind.						x																			Unnec.
	Ind.																									Unnec.
	Ind.	x					x															x				Yes
	Ind.	x					x															x				Yes
	Ind.						x																			Unnec.
	Tm.																									Unnec.
	Ind.				x		x		HiVF	400					x											Unnec.
Company *Y*		x			x		x			600					x		x			x		x			600	No
		x			x		x			600					x		x			x		x			600	No
		x			x		x			600					x		x			x		x			600	No
		x			x		x			600					x		x			x		x			600	No
		x			x		x			600					x		x			x		x			600	No
Company *Z*..	S.c.	x								350																
	S.c.	x								350																
	S.c.	x								350																
	S.c.				x					350																
	S.c.	x								350																
	S.c.	x								350																
	S.c.	x								350																
	S.c.	x								350																
	S.c.	x								350																

S.c.—Supervisory Control. Tm.—Telemetering. Ind.—Indicating or Sup. Ind. * Serjedetours. † Relay protectors.

Table I (Continued)

Designation of Power Company	Type of Circuit or Identification**	Quest. 20	Quest. 21	Quest. 22	Quest. 23 No. of Outages Due to:				Quest. 24	Quest. 25	Quest. 26	
		Years of Service	No. of Outages During Service	Total Outage Time (Hrs.)	Short Circuits	Open Circuits	Circuit Rearrangements	Terminal Protect.	Pilot Wire Automatically Super'd?	Pilot Wire Used for Other?	Troubles Due to Induction or Surges?	Miscellaneous Information as Covered in Questionnaire Under "Additional Information." In General These Data Were Not Suitable For Summarizing
Company *N*		13							Yes.-Mo.	No	Yes	Difficulty from induced voltages. Only grounded thru detectors. Meggered monthly at 500 volts.
		13	Some early troubles due to Matthews test caps. Now have some difficulty from transverse voltages.						Yes.-Mo.	No	Yes	
		13							Yes.-Mo.	No	Yes	
		10							Yes.-Mo.	No	Yes	
		13							Yes.-Mo.	No	Yes	
		4							Yes.-Mo.	No	Yes	
		1½							Yes.-Mo.	No	Yes	
		6							Yes.-Mo.	No	Yes	
Company *O*	S.c. & I.	13							No	No	No	
	S.c. & I.	15							No	No	No	
	S.c. & I.	20							No	Yes	No	
	HT tel. line	11							No	No	Yes	
	HT tel. line	21							No	No	Yes	
	Tm.	3							No	No	No	
	Ind.	13							No		No	
	S.c. & I.	12							No	Yes	No	
	Ind.	15							No	No	No	
Company *P*		8	50	75	5	3		42	Yes	No	No	Terminal equipment ungrded. except through station battery. Auto. detection of accidental grds. and incipient faults.
		13	70	150	Failure of sup. eq.				Yes	No	No	
		7	5	3	1	1		3	Yes	No	No	
		6	20	96		15			Yes	Yes TP	Only minor	
		7	4	8		2		2	Yes	No	No	New inst. prefer cable prs.
		7	30	60	12	13			Yes	Yes TM	No	Some failures due to fog.
		7	35	70	3	2			Yes	Yes TM	No	
		6	10	48	Sup. equip. 10				Yes	Yes TP	Only minor	
Company *Q*	S.c.	14							No	No	No	Equipment is grounded at sta. when grd. is <0.5 w at dispatcher's end.
	S.c.	14							No	No	No	
	S.c.	14							No	No	No	Sta. end ungrounded.
	S.c.	14							No	No	No	Would want auto. super. on new inst
	S.c.	4	Occa. relay coil burnout fuses						No	No	Yes	
	S.c.	10							No	No	No	
	S.c.	15	Occa. relay coil burnouts						No	No	Yes	
	S.c.	1							No	No	No	
Company *R*	S.c.	1½							No		No	Grounded only through prot. tubes.
Company *S*	Tm.	7½	3	7	1			2	No	No	No	$.875 per ½ mile.
	S.c.	1	0						No	No	No	
Company *T*	S.c.	0							No	No		Most of terminal equipment grounded to remote grounds.
	S.c.	14	No record								No	
	S.c. & tm.	14	No record								No	
	S.c.	13	1 reg. & 1 emergency pair						Yes		No	
	S.c. & tm.	15	Numerous out. due to grd. prot.-corrected								No	
	S.c. & tm.	15	Numerous out. due to grd. prot.-corrected								No	
	Ind.	3	1	52	1				Yes		No	
	Ind.	14	3	32	3 (vandalism)						No	
	S.c. & tm.	13	4	89	4 (2 sh. injuries—2 vandalism)				Yes		No	
	Ind.	14	Many trbls. & large amt. of lost time								No	Poor cable.
	S.c. & tm.	13	1	127			Flood				No	
	S.c.	10	5					5	No		No	
Company *U*	S.c. & tm.	New	No oper. exp.									Taps from cable 500/1400.
Company *V*	Tm.	10	No record						No	Tel.	Yes	Would prefer ccts. in own cable but exp.
	Tm.	2+	No record						No	Tel.	Yes	Possibly auto. super.
Company *W*	S.c.	6.1	0						No	No	No	$1.25 per wire mile per month.
	S.c.	4.3	0						No	No	No	Grounded to sta. grd.
	S.c.	0.8	0						Yes	No	No	No exp. to show grd. prot. is adequate.
	Tm.	3.6	6	5.2		5			No	No	No	Leased because maintenance is superior and cheaper. Channel can easily be duplicated in case of prolonged outages.
	S.c.	2.8	1	1.7		1			No	No	No	
	Tm.	8.3	4	5		4			No	No	No	
	Tm.	3.5	9	7	1	7			No	No	No	
	Tm.	3.6	9	5.6		9			No	No	No	Troubles on leased ccts. not coincident with trans. line (part from induction).
	Tm.	3.5	0						No	No	No	
	Tm.	3.5	0						No	No	No	Prefer auto. super. Thyrite or UG's.
	Tm.	8.3	0						No	No	No	Supervision by means of relay which drops out in reduced current.
	Tm.	8.4	0						No	No	No	
	S.c.	3	3	14.8		3			Yes	No	No	Grds. detected by sensitrol relay conn. between center tap of pilot wire relay battery and grd.
	Tm.	0.2	1	10.5		1			No	No	No	
Company *X*	S.c.	1½	11	110	7	2		2	No	No		$4 per mile per pr.
	Ind.	16	No record						Yes	No	Yes	Power sta. grd.
	Ind.	16	No record						Yes	No	No	Many tm. ccts. continuously energized and troubles indicated by instruments.
	Ind.	12	No record						No	No	No	
	Ind.	12	No record						No	No	No	
	Ind.	14	No record						Yes	No	No	Early troubles from surges relieved by resistor.
	Tm.	22	No record						Yes	No	No	
	Ind.	1½	0						No	No	No	
Company *Y*		15	No record available									
		15	No record available									
		15	No record available									
		15	No record available									
		15	No record available									
Company *Z*	S.c.	8							No	No		
	S.c.	6	2	13	1		1		No	No		
	S.c.	10	9	34	4		5		No	No		
	S.c.	9							No	No		
	S.c.	12	1	3			1		No	No		
	S.c.	11	1	4½	1				No	No		
	S.c.	15	2	3	1		1		No	No	Yes	
	S.c.	17	3	21	1		3		No	No		
	S.c.	17	6	25½	2		4		No	No	Yes	

S.c.—Supervisory Control. Tm.—Telemetering. Ind.—Indicating or Sup. Ind. * Berjedetours. † Relay protectors.

Automatic supervision	4 companies preferred 1 company did not prefer
3. Circuits in cable	5 companies preferred
In underground cable	1 company preferred
4. Circuits along separate route from power	1 company preferred
5. (*a*) Leased circuits	3* companies preferred
(*b*) Power-line carrier	1** company preferred
(*c*) Privately owned cable	1 company preferred

(*e*). Is terminal equipment grounded to power-station ground, external ground, or ungrounded? (question 13)

1. Number of companies answering	10
2. Number of companies using station ground	7
3. Number of companies using remote ground	2
4. Number of companies leaving ungrounded	1

Table II

	Average Years In Service Per Circuit	Average Total Number of Outages Per Circuit	Average Total Outage Time Per Circuit (Hours)	Average Length of Circuit (Miles)	Average Duration Of outage Per Circuit (Hours)	Average Number outages Per Circuit Mile-Year	Average Outage Time Per Circuit Mile-Year (Hours)
Basis A							
Privately owned	10.3 (71)*	4.45 (60)	18.8 (60)	6.2 (105)	4.22 (60)	0.06 (60)	0.25 (60)
Leased	4.9 (70)*	4.4 (59)	16.7 (59)	6.4 (69)	3.80 (59)	0.12 (59)	0.45 (59)
Total	7.6 (141)*	4.43 (119)	17.75 (119)	6.3 (174)	4.01 (119)	0.09 (119)	0.35 (119)
Basis B							
Privately owned	7.9 (27)*	9.8	40.4	3.7	4.13	0.33	1.37
Leased	5.1 (38)*	5.0	22.7	7.0	4.53	0.14	0.64
Total	6.3 (65)*	7.0	30.1	5.6	4.30	0.23	0.95

* Figures in parentheses refer to number of circuits involved in average. In basis *B* the figures (27), (38) and (65) apply to the corresponding entries in all columns.

Appendix. Questions Submitted to Operating Organizations for Information on Pilot-Wire Conductors

Circuit Details

1. *L*—Leased; privately owned.

2. *C*—Cable; open-wire.

3. *D*—Located at a distance from power lines.
A—Located adjacent to power lines.

4. *M*—Metallic; *G*—Ground return; *P*—Phantom.

†5. Size of conductor.

†6. Conductor insulation.

†7. Number of conductors in cable.

†8. Conductor arrangement:

U—Untwisted.
T—Twisted pairs.
Q—Quadded.

†9. Type of sheath.

10. Length (miles one way).

11. Resistance (ohms—loop exclusive of terminal devices).

12. *L*—Loaded; *U*—Unloaded.

Terminal Protection

13. *C*—Carbon blocks; *AG*—Air gaps; *T*—Thyrite; *PT*—Protective tubes; *VG*—Vacuum gaps; *F*—Fuses; *H*—Heat coils; *R*—Relay protectors.

14. Breakdown values of item 13.

(*a*) 60 cycle.
(*b*) Impulse.

Potential Gradient Protection

15. *N*—Neutralizing transformers; *C*—Line-coupling transformers; *I*—Insulated relay equipment; *D*—Drainage equipment.

16. Insulation level of pilot wire to station ground at item 15.

Treatment in Telephone Exchanges or at Other Remote Points

17. Protection as in item 13.

18. Breakdown values of item 17:

(*a*) 60 cycle.
(*b*) Impulse.

19. Special marking to prevent routine testing.

Performance

20. Years of service.

21. Pilot-wire outages (number during item 20).

22. Total outage time (hours).

23. Number of outages caused by:

S—Short circuits.
O—Open circuits.
C—Circuit rearrangements.
P—Terminal protection.

24. Is pilot-wire circuit automatically supervised?

25. Is pilot-wire circuit used for other purposes?

26. Have troubles been experienced because of induction effects or surges on the pilot wires?

Additional Information

1. If the circuit is leased, what is the basic rental rate?

3. If the circuit is located on the same right of way with a power-line phase, give details as to the construction of the power circuit. What provisions are made to prevent troubles from induced voltages, both between the pilot conductor and between the pilot conductors and ground?

4. Please give details if a phantom circuit is used.

12. Does capacity and loading of the pilot circuit affect the performance, and what compensation methods if any are employed?

13. Is the terminal equipment grounded to the power-station ground, external ground, or ungrounded?

13 to 16. Please indicate by a sketch the arrangement used indicating the position of ground connections. What evidence either from experience or special tests do you have to establish that the protection afforded is adequate?

24. Please describe the monitoring system employed and values of monitoring current, and indicate whether or not grounds on the pilot wires are detected. What minimum insulation resistance is permitted between conductors before circuit is considered bad? What value is detected? Have incipient faults thus been discovered?

25. Please describe other joint uses of the pilot wires.

26. Please discuss troubles experienced caused by induction or surges and means taken to prevent them.

27. For new installation, what type of pilot channel would you prefer? Why?

28. For new installations would you want to use automatic supervision?

29. For new installations what form of terminal or intermediate protection (items 13 and 17) would you prefer?

Reference

1. Pilot-Wire Circuits for Protective Relaying—Experience and Practice, AIEE relay subcommittee. AIEE Transactions, volume 62, 1943, May section, pages 210–14.

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* 1 for the shorter distances.

** For the longer distances.

† Privately owned circuits only.

Theory of Rectifier—D-C Motor Drive

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THE use of rectifiers for furnishing power to d-c motors is becoming more common, particularly for variable-voltage drives. The theory applying to drives such as those incorporating the Ward Leonard system is quite inadequate for use with a rectifier-d-c motor drive. This paper is an introduction to the theory involved when operating a d-c shunt-wound motor from a rectifier. It is hoped that further thought and discussion of this new and vital subject will be stimulated.

In the course of the development work on the system described in a paper by K. P. Puchlowski entitled "Electronic Control of D-C Motors,"[1] it became apparent that purely empirical data on performance were unsatisfactory for thorough analysis of the system. This paper describes the theoretical approach to the obtaining of fundamental characteristics of a complete drive consisting of a single-phase full-wave electronic rectifier and a d-c motor. The rectifier output is variable by grid control of the thyratrons combined with a control circuit for the rectifier. The rectifier for the larger motors is usually multiphase. The control circuit regulates to any manually preset motor speed, compensates for armature *IR* drop, and incorporates current-limit acceleration.

We are concerned here with the broad considerations of a d-c shunt-wound motor operated from a grid-controlled electronic rectifier rather than the details of the speed-control circuits for the drive. Only a single-phase full-wave rectifier is considered. This type of rectifier is normally used on motors of low horsepower rating, usually not over three horsepower.

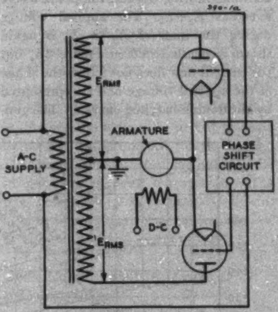
General Considerations

The simplified circuit of a typical single-phase full-wave system is shown in Figure 1A. In this particular circuit, the rectifier is composed of two thyratron tubes with their grids arranged for phase-shift control to permit the tubes to start conducting (firing) at any chosen phase angle, with respect to the applied a-c voltage, during each half-cycle.

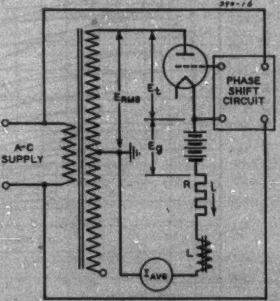
The simplified circuit in Figure 1A has been converted into an equivalent circuit in Figure 1B to show more clearly the basic considerations and terminology. Several very important assumptions have been made in the conversion from Figure 1A to Figure 1B, and these are used throughout the paper.

Assumptions

1. The machine is shunt wound with no compounding.
2. The field is continuously energized and saturated.
3. The armature inductance is constant.
4. The armature and circuit inductances have been lumped in the inductance *L*.
5. The resistances of the armature and the external circuit have been lumped in the resistance *R* and are constant.
6. The counter electromotive force, E_g, is directly proportional to motor speed and is represented as a battery with no internal resistance.
7. The transformer inductance and resistance are negligible.
8. The arc drop of the tube is shown as E_t and is constant.
9. The tube characteristics are such that conduction can begin as soon as the instantaneous value of the transformer voltage is equal to or greater than E_g+E_t.
10. The effect of armature reaction is negligible.
11. The individual half-cycles of current are discreet pulses which do not join each other. Hence the equivalent circuit shows only one half of the full-wave rectifier.
12. All calculations apply to one half-cycle in view of (11) above.



A. The elementary diagram of a single-phase full-wave rectifier—d-c motor drive



B. A circuit equivalent to one half of the full-wave rectifier shown in A

Figure 1

Some of the assumptions are exact while some are close approximations. All are sufficiently accurate that the resulting data serve as a very good guide for analysis of test results and further elaboration of the theory.

For convenience, the term E_m is used in the appendix to denote $\sqrt{2}E_{rms}$. When the formulas are transferred from the appendix to this text, the form $\sqrt{2}E_{rms}$ is used.

Firing (Starting) and Stopping Angles of Current Pulses

When a d-c motor is being driven from a single-phase full-wave rectifier as shown in Figure 1A, the available input voltage E_{rms} has instantaneous values e as shown in Figure 2A. The rectifying effect is shown by subsequent half-cycles of voltage e being all positive with respect to the zero line. The counter electromotive force E_g is shown as less than the peak voltage e_{max} of the a-c wave. This must always be so, since otherwise no armature current would ever flow. During any of the time that e is more than E_g+E_t, current flow can be started by proper control of the phase angle of the grid voltage.

Since it has been assumed that the individual half-cycles are discreet pulses which do not join each other, we need consider only a single half-cycle of events, at least within the limits where the assumption holds. The limits will be discussed later in more detail. In Figure 2, X_f is the "firing" angle at which the grid

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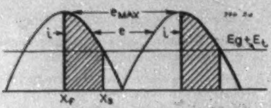
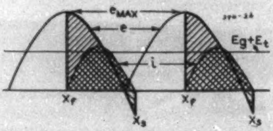


A. The wave form of current and voltage in a full-wave rectifier operating the d-c motor which is assumed to have no inductance. The effect of rectification is shown by locating both halves of the a-c cycle of voltage and current above the zero line where the zero line is at the potential of the transformer mid-point



B. Same as A except with inductance present

Figure 2

phasing permits the thyratron tube to start conduction. The thyratron may be considered as a rectifying single-pole switch which closes at the "firing" angle X_f. If the armature inductance L in Figure 1B were zero, the current would flow until, in Figure 2A, e becomes less than E_g+E_t at angle X_s, which is called the "stopping angle." The current must stop flowing at the angle X_s, because the opposing voltages are equal, and the rectifying property of the thyratron does not permit reversal of the current. The voltage e may be considered as applied to the armature during the interval X_f to X_s, and thus the hatched portion of Figure 2A represents the voltage applied to the armature by the rectifier. This wave shape is not directly measurable in an actual system, because it cannot be conveniently disassociated from the counter electromotive force E_g. It should not therefore be thought of as the measureable armature voltage which is a more involved function. If the inductance were zero, the current pulse in Figure 2A would have the same shape as the voltage wave from X_f to X_s.

The ideal case of zero armature inductance is quite far from the actual one, and Figure 2B shows the modified relations with inductance present. The general effect of the inductance is to reduce the peak of the current pulse and broaden its base by causing the current to continue flowing beyond the point where e equals E_g+E_t. The applied voltage during the interval X_f to X_s is again shown single-hatched in Figure 2B, but the current now does not follow the shape of the applied voltage and is represented by the double-hatched curve i. The general shape of the current wave i resembles a sine wave but must not be mistaken for one. The voltage e from X_f to X_s is somewhat peculiar when inductance is present, since the current may flow until the voltage is either positive or negative at X_s with respect to ground.

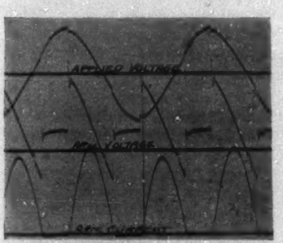
The oscillograms in Figure 3 show clearly the relations actually obtained between a-c voltage, current, and armature voltage at two different conditions of load and speed.

The fundamental relationship on which most of this paper is based is that of the "stopping angle" X_s as a function of the "firing angle" X_f with other variables as parameters and is shown in Figure 4. The mathematical equation from which the curves were drawn is derived in the appendix, part B and is as follows:

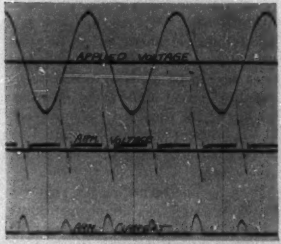
$$\cos\theta\ \sin(X_s-\theta)-a+\left[a-\cos\theta\ \sin(X_f-\theta)\right]\epsilon^{-\frac{X_s-X_f}{\tan\theta}} \quad (27)$$

or another form is

$$\left[a-\cos\theta\sin(X_s-\theta)\right]\epsilon^{\frac{X_s}{\tan\theta}} = \left[a-\cos\theta\ \sin(X_f-\theta)\right]\epsilon^{\frac{X_f}{\tan\theta}} \quad (28)$$



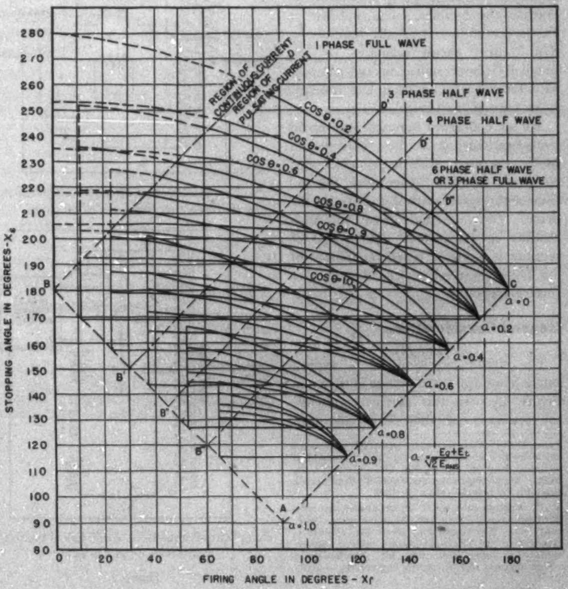
A. Full rated speed and torque



B. No load and 16 per cent of rated speed

Figure 3. Oscillograms of current and voltage in an electronic d-c motor drive with constant field

Figure 4. Graph showing "stopping angle" X_s of current as a function of "firing angle" X_f with cos θ and "speed factor" a as parameters



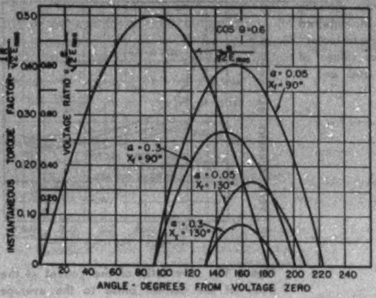


Figure 5. Calculated wave shape of current pulse for two "firing angles" X_f and two "speed factors" a

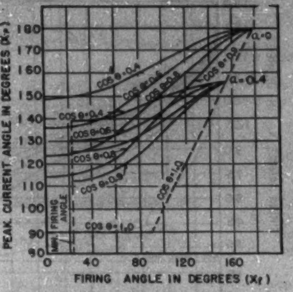


Figure 6. Curves showing the angle X_p at which peak current occurs as a function of "firing angle" X_f for two "speed factors" and several values of cos θ

where

$a=\frac{E_g+E_t}{\sqrt{2}E_{rms}}$ called "speed factor" in this paper

$\theta=\arctan\frac{\omega L}{R}$ = power-factor angle of the motor armature circuit at the a-c line frequency

The "speed factor" a is a convenient means of expressing, as a ratio, the a-c voltage E_{rms} and the counter electromotive force of the live load consisting of the voltage E_g and the tube arc drop E_t. The "speed factor" is so named, because E_g is assumed to be directly proportional to motor speed.

Several interesting points should be noted in regard to Figure 4. Obviously, the firing angle X_f in Figure 2 cannot be greater than the greatest angle at which e becomes equal to E_g+E_t. Since the limit is a function of E_g and consequently of a, there is a locus of maximum possible firing angle X_f. This locus is shown in Figure 4 as line A–C which is the beginning of all curves.

Also, it is obvious that the firing angle X_f cannot be less than that angle at which e first becomes greater than E_g+E_t. This limit is shown in Figure 4 by termination of the curves, which begin on A–C, at firing angles projected vertically from the line A–B. The same limit holds for all values of cos θ at a given value of a.

If $X_s-X_f>180$ degrees, the flow of current will no longer be in discreet half-cycle pulses but will be continuous. Equations 27 and 28 do not hold for conditions of continuous current. The limits within which the equations hold true are established in Figure 4 by the lines B–D, B'–D', B''–D'', B'''–D'''.

Wave Shape of Current Pulse

The wave shape of the current pulse resembles that of a sine wave. It is, however, mathematically speaking, a combination of a sine wave, a constant negative term, and an exponential function. The equation for the current pulse is derived in the appendix, part A and is

$$\frac{iR}{\sqrt{2}E_{rms}}=\cos\theta\ \sin(x-\theta)-a+[a-\cos\theta\ \sin(X_f-\theta)]\epsilon^{-\frac{x-X_f}{\tan\theta}} \quad (26)$$

where x = any angle greater than X_f but less than X_s. A graph of this equation is shown in Figure 5 with firing angle X_f and speed factor a as parameters while cos θ is kept constant. The expression $iR\div\sqrt{2}E_{rms}$ serves to give a ratio which eliminates specific values of current and will be called "instantaneous torque factor." It is actually the instantaneous IR drop in per cent of peak a-c voltage. The parameters which have been chosen for illustration in Figure 5 are typical of those in a practical drive.

Peak Current and Its Angle of Occurrence

The angle X_p at which peak current flows is readily obtained by differentiating the equation 26 and setting it equal to zero as shown in the appendix, part C. This gives the equation

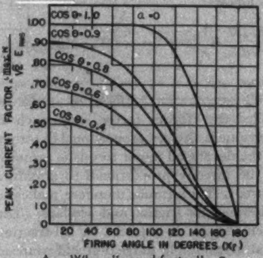
$$\cos(X_p-\theta)\epsilon^{\frac{X_p}{\tan\theta}}=\left[\frac{a}{\sin\theta}-\frac{\sin(X_f-\theta)}{\tan\theta}\right]\epsilon^{\frac{X_f}{\tan\theta}} \quad (49)$$

Data from equation 49 are plotted in Figure 6 which shows the peak-current angle X_p as a function of the firing angle X_f for parameters of cos θ and speed factor a.

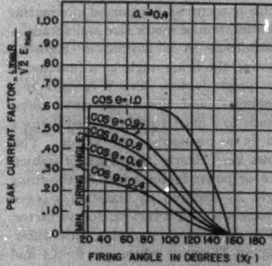
The peak value of the "instantaneous torque factor" $iR/\sqrt{2}E_{rms}$ can now be calculated by substituting the value of X_p calculated from equation 49 for x in equation 26. At the maximum value of current we may substitute i_{max} for i in the expression $iR/\sqrt{2}E_{rms}$ giving $i_{max}R\div\sqrt{2}E_{rms}$ which will be called "peak-current factor." A graph showing "peak-current factor" as a function of firing angle X_f is shown in Figure 7.

Armature Load–Speed Relations

The purpose of these calculations is to obtain performance characteristics in addition to the more fundamental data.



A. When "speed factor" = 0



B. When "speed factor" = 0.4

Figure 7. Curves showing the "peak-current factor" as a function of "firing angle" X_f for several values of cos θ

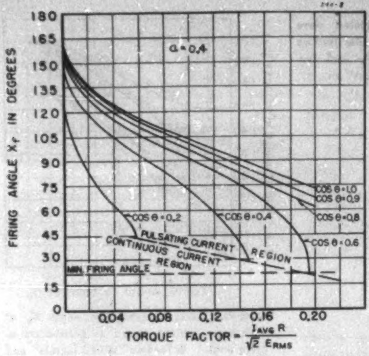


Figure 8. Curves showing the "firing angle" X_f required to maintain the "speed factor" $a = 0.4$ for various values of "torque factor" and cos θ

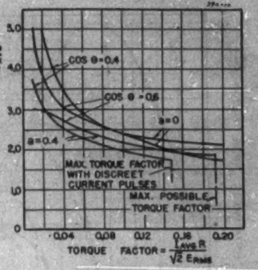


Figure 10. The ratio of the current at the peak of the current pulse to the average armature current shown as a function of "torque factor" for two values of cos θ and two of "speed factor" *a*

The necessary step in achieving this is to determine the torque–firing-angle relationship. This is done in the appendix, part D, and results in an equation for the average armature current I_{avg} as a function of the firing angle X_f. The equation for I_{avg} follows:

$$I_{avg} = \frac{\sqrt{2}E_{rms}}{\pi R}[\cos X_f - \cos X_s - a(X_s - X_f)] \tag{57}$$

It is convenient to express equation 57 as follows:

$$\frac{I_{avg}R}{\sqrt{2}E_{rms}} = \frac{1}{\pi}[\cos X_f - \cos X_s - a(X_s - X_f)] \tag{58}$$

The expression $I_{avg}R/\sqrt{2}E_{rms}$ will be called the "torque factor," inasmuch as its value is directly proportional to torque if the developed motor torque is assumed to be directly proportional to armature current. The formula, therefore, makes no allowance for friction and windage. The useful torque is obtained by subtracting that attributable to friction and windage from the calculated value. The torque factor is useful as a means of eliminating consideration of motor size and details.

Equation 58 is plotted in Figure 8 showing the firing angle as a function of the torque factor. Cos θ is used as a parameter, and the whole curve corresponds to a fixed value of speed factor *a* and is a constant-speed graph. This shows very clearly the effect of inductance or power-factor angle on the firing angle X_f required to maintain a speed factor $a = 0.4$ for a varying torque factor.

The data are presented a little differently in Figure 9 where cos $\theta = 0.4$ and the speed factor is a parameter. These are, of course, curves of most practical interest for a given motor. The curves in Figure 9 show the very considerable change of firing angle required to maintain constant speed as the torque factor is varied. Obviously, space does not permit showing complete curves for a full range of both cos θ and speed factor. Hence, only the two typical curves in Figures 8 and 9 have been shown.

Peak-Current Ratio

The term "peak-current ratio" as used here denotes the ratio i_{max}/I_{avg}. This gives a conception of the peak current which the tubes must carry as related to the known I_{avg} required to deliver the desired torque. This ratio is obtained by dividing the "peak-current factor" $i_{max}R/\sqrt{2}E_{rms}$ by the "torque factor" $I_{avg}R/\sqrt{2}E_{rms}$ from Figure 9. This gives the "peak-current ratio" i_{max}/I_{avg}, which can be plotted as a function of the torque factor. A typical set of curves for the peak-current ratio is shown in Figure 10. It is seen that the electronic rectifier tube must therefore be selected with a peak-current rating of several times the maximum I_{avg} required by the motor during the starting inrush.

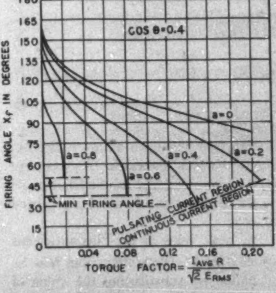
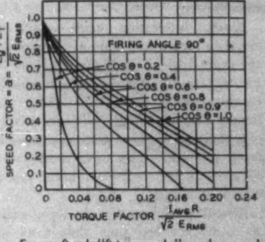


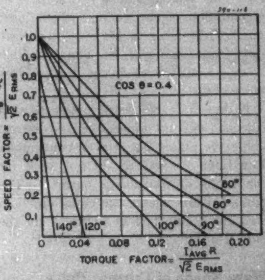
Figure 9. The "firing angle" required to maintain various "speed factors" at different values of "torque factor"; cos θ is held constant

Speed-Torque Curves

The data contained in Figures 8 and 9 are in such form that we can directly replot them to give the speed factor *a* as a function of the torque factor. The speed-torque curves in Figure 11A are replotted from Figure 8 and show the speed regu-



A. For a fixed "firing angle" and several values of cos θ



B. For fixed cos θ and several different "firing angles"

Figure 11. Speed–torque characteristics

lation for a typical constant firing angle of 90 degrees for various values of cos θ. The curves in Figure 11B are replotted from Figure 9 and show the speed regulation at cos $\theta=0.4$ for various fixed firing angles.

Conclusions

1. The calculated wave shapes are verified by the oscillograms.

2. The speed-torque curves of a single-phase full-wave-rectifier–d-c motor drive without automatic speed regulation are such as to be useful only for specialized applications.

3. Most applications will require that the drive incorporate automatic speed regulation.

4. Additional work to correlate mathematical and test results would be desirable. This work should cover various sizes, speeds, and types of motors.

5. Further refinement of the theory and extension to multiphase rectifiers would be helpful.

6. It is hoped that this paper will serve its stated purpose of stimulating discussion of this new subject.

Appendix. Mathematical Analysis of the D-C Motor–Rectifier System

Part A. Equation of the Current Pulse

The equivalent circuit of a single-phase half-wave rectifier system with armature of a d-c motor as a load is represented in Figure 13.

It is assumed that:

1. R = resistance of the armature circuit and is constant.

2. L = inductance of the armature circuit and is constant.

3. E_g = counter electromotive force induced in armature winding of the motor and is directly proportional to the speed.

4. E_t = arc drop of the mercury-vapor or gaseous-discharge rectifier and is constant.

Voltages E_g and E_t may be regarded as constant components of the voltage applied to the system and are represented in Figure 13 by a battery without any internal resistance and an electromotive force $=E_g+E_t$.

The method is based upon the mathematical solution of a general circuit shown in Figure 12 and subsequent restriction of the domain of definition of the independent variable (time) and of its function (current) in accordance with the physical property of unidirectional conduction (rectification). These limitations will be discussed later and will become apparent from Figure 14 where the applied a-c voltage is represented as function of electrical angle (or time).

Referring to Figure 14, it will be seen that the sinusoidal voltage applied to the system is the following function of the electrical angle:

$$e=E_m \sin x \tag{1}$$

Denoting the firing angle of the rectifier (angle of ignition) by X_f we may write

$$x=X_f+\omega t \tag{2}$$

where

$\omega=2\pi f$ = angular frequency of the applied voltage

t = time counted from the instant $\frac{X_f}{\omega}$ (for $x=X_f$, $t=0$)

and the a-c voltage applied to the system can be expressed as

$$e=E_m \sin (X_f+\omega t) \tag{3}$$

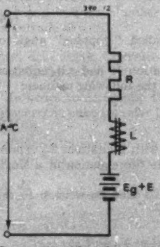


Figure 12. An elemental circuit with resistance, inductance, and a d-c voltage connected in series and supplied from an a-c line

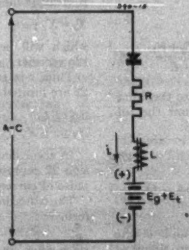


Figure 13. An elemental half-wave rectifier circuit with resistance, inductance, and a counter electromotive force in the load

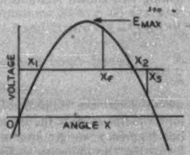


Figure 14. A sine wave of voltage with counter electromotive force

The general equation of the circuit (Figure 12) can now be written in the following form:

$$L\frac{di}{dt}+Ri+E_g+E_t=E_m \sin (X_f+\omega t) \tag{4}$$

or

$$L\frac{di}{dt}+Ri=E_m \sin (X_f+\omega t)-(E_g+E_t) \tag{5}$$

where $i=f(t)$ = current flowing in the circuit.

Equation 5 will be solved in accordance with the method of undetermined coefficients.

The particular integral i_1 of equation 5 is sought under the form

$$i_1=A \cos (X_f+\omega t)+B \sin (X_f+\omega t)+C \tag{6}$$

where coefficients A, B, and C can be evaluated by substituting the particular solution 6 into equation 5 and evaluating A, B, and C so that an identity is obtained.

Thus we have

$$-\omega LA \sin (X_f+\omega t)+\omega LB \cos (X_f+\omega t)+ RA \cos (X_f+\omega t)+RB \sin (X_f+\omega t)+ RC\equiv E_m \sin (X_f+\omega t)-(E_g+E_t)$$

and

$$(RB-\omega LA) \sin (X_f+\omega t)+(RA+\omega LB)\times \cos (X_f+\omega t)+RC\equiv E_m \sin (X_f+\omega t)-(E_g+E_t) \tag{7}$$

In order to satisfy the identity 7 the coefficients A, B, and C must satisfy the set of the following equations:

$$\left.\begin{aligned} RB-\omega LA&=E_m\\ RA+\omega LB&=0\\ RC&=-(E_g+E_t)\end{aligned}\right\} \tag{8}$$

and from equations 8 we get

$$A=-\frac{\omega LE_m}{R^2+\omega^2L^2} \tag{9}$$

$$B=\frac{RE_m}{R^2+\omega^2L^2} \tag{10}$$

$$C=-\frac{E_g+E_t}{R} \tag{11}$$

Hence, the particular integral of equation 5 is

$$i_1=\frac{RE_m}{R^2+\omega^2L^2} \sin (X_f+\omega t)- \frac{\omega LE_m}{R^2+\omega^2L^2} \cos (X_f+\omega t)-\frac{E_g+E_t}{R} \tag{12}$$

The general solution of equation 5 is found as the sum of equation 12 and of the general integral i_2 of the simplified equation

$$L\frac{di_2}{dt}+Ri_2=0 \tag{13}$$

Equation 13 can be solved directly by separating the variables

$$L\frac{di_2}{i_2}=-Rdt \quad (i_2\neq 0)$$

$$L \log i_2=-RT+M$$

$$i_2=\epsilon^{-\frac{Rt+M}{L}}$$

where M is an arbitrary constant of integration or

$$i_2=D\epsilon^{-\frac{R}{L}t} \tag{14}$$

where $D=\epsilon^{-\frac{M}{L}}$ is a constant of integration.

The general solution of equation 5 can now be expressed as

$$i=i_1+i_2=\frac{RE_m}{R^2+\omega^2L^2}\ \sin\,(X_f+\omega t)-$$
$$\frac{\omega LE_m}{R^2+\omega^2L^2}\ \cos\,(X_f+\omega t)-\frac{E_g+E_t}{R}+D\epsilon^{-\frac{R}{L}t} \tag{15}$$

The constant of integration D can be evaluated from the following boundary condition:

$t=0$
$i=0$ (compare equation 2)

Thus

$$\frac{RE_m}{R^2+\omega^2L^2}\ \sin X_f-\frac{\omega LE_m}{R^2+\omega^2L^2}\ \cos X_f-$$
$$\frac{E_g+E_t}{R}+D=0$$

$$D=\frac{\omega LE_m}{R^2+\omega^2L^2}\ \cos X_f-$$
$$\frac{RE_m}{R^2+\omega^2L^2}\ \sin X_f+\frac{E_g+E_t}{R} \tag{16}$$

Substituting expression 16 into the equation 15, and taking into account the following well-known relationships:

$$R^2+\omega^2L^2=Z^2 \tag{17}$$

$$\frac{R}{Z}=\cos\theta \tag{18}$$

where Z is the impedance and θ the power-factor angle of the armature circuit at line frequency, we get after a simple transformation the following expression for the armature current:

$$i=\frac{E_m}{Z}\ \sin\,(X_f-\theta+\omega t)-\frac{E_g+E_t}{R}+$$
$$\left[\frac{E_g+E_t}{R}-\frac{E_m}{Z}\ \sin\,(X_f-\theta)\right]\epsilon^{-\frac{R}{L}t} \tag{19}$$

Further, denoting

$$\frac{E_g+E_t}{E_m}=a \quad \text{(called ``speed factor'')} \tag{20}$$

and taking into account the relationship

$$\frac{R}{L}=\frac{\omega}{\tan\theta} \tag{21}$$

as well as equations 2 and 18, we obtain the following final form of the expression for the armature current as a function of angle x:

$$i=\frac{E_m}{R}\left\{\cos\theta\ \sin\,(x-\theta)-a+\right.$$
$$\left.[a-\cos\theta\ \sin\,(X_f-\theta)]\epsilon^{-\frac{x-X_f}{\tan\theta}}\right\} \tag{22}$$

In order to analyze and to limit the domain of definition of the variable x and its function i in accordance with the physical property of unidirectional conduction (see circuit in Figure 13), it must be remembered that on account of rectification i cannot assume negative values, that is, the following condition must be fulfilled:

$$i\geqq 0 \tag{23}$$

We further restrict our considerations to the case where the current flows in discreet pulses, that is, two consecutive pulses of current corresponding to two consecutive half-cycles of a single-phase full-wave rectifier do not join with each other. It should be noted that X_f itself is subject to definite limitations which will be discussed in part B. It is useful to note that in line with these limitations it is sufficient to analyze but one current pulse and thus restrict the considerations to a half-wave rectifier system as shown in Figure 13.

Under these conditions the current will twice assume the value 0 within one half-cycle of the applied voltage:

1. For $t=0$, that is, $x=X_f$ when the rectifier starts conducting
2. For $t=t_s$ when the rectifier stops conducting

The instant 1 corresponds to the firing angle $x=X_f$ (see relationship 2). The instant 2 corresponds to an angle X_s such that

$$X_s=X_f+\omega t_s \tag{24}$$

which will be called "stopping" angle of the rectifier.

Thus, t in equation 19 and x in equation 22 are limited in the following manner:

$$\left.\begin{array}{l}0\leqq t\leqq t_s\\ X_f\leqq x\leqq X_s\end{array}\right\} \tag{25}$$

In accordance with limitations 25 expression 22 represents the equation of a single pulse of current.

The equation 22 may be written in the form

$$\frac{iR}{E_m}=\cos\theta\ \sin\,(x-\theta)-a+$$
$$[a-\cos\theta\ \sin\,(X_f-\theta)]\epsilon^{-\frac{x-X_f}{\tan\theta}} \tag{26}$$

which represents the ratio of the instantaneous resistive armature voltage drop to the peak value of the a-c voltage applied to the system as a function of the angle x for given values of parameters a ("speed factor") and θ.

Part B. Firing (Starting) and Stopping Angles of Current Pulses

From the definition of the stopping angle (see equation 24), it follows that $i=0$ for $x=X_s$ and we have from equation 22 the basic relationship between the firing and stopping angles

$$\cos\theta\ \sin\,(X_s-\theta)-a+$$
$$[a-\cos\theta\ \sin\,(X_f-\theta)]\epsilon^{-\frac{X_s-X_f}{\tan\theta}}=0 \tag{27}$$

or after a simple rearrangement

$$[a-\cos\theta\ \sin\,(X_s-\theta)]\epsilon^{\frac{X_s}{\tan\theta}}$$
$$=[a-\cos\theta\sin\,(X_f-\theta)]\epsilon^{\frac{X_f}{\tan\theta}} \tag{28}$$

The relationship 27 (or 28) represents X_s as an implicit function of X_f for different values of a and θ which are parameters. It should be remembered that

$$\theta=\text{arc tan}\ \frac{\omega L}{R} \tag{21a}$$

determines the relationship between the inductance and the resistance of the armature circuit and

$$a=\frac{E_g+E_t}{E_m} \tag{20}$$

is a term which may be regarded as representing the speed of the motor.

It should be noted that for an ideal rectifier $E_t=0$ and

$$a=\frac{E_g}{E_m} \tag{20a}$$

Although it is impossible to represent the stopping angle X_s in the form of an explicit formula

$$X_s=F(X_f,\theta,a)$$

it is possible to calculate the value of X_s for each value of X_f and different values of parameters a and θ and draw a system of graphs of the function 27 (Figure 4).

In our considerations we restrict ourselves to the physically possible range of the firing angles of the rectifier. On the other hand, as it was mentioned previously, only conditions for discreet pulses of current are being analyzed.

The two previous limitations can be readily represented analytically as well as graphically in the following manner:

If we refer to Figure 14, and remember the fundamental property of the rectifier, namely that conduction is possible only when the anode is positive with respect to the cathode, it will become apparent that the firing angle X_f must satisfy the following condition:

$$x_1\leqq X_f\leqq x_2 \tag{29}$$

Expressions for x_1 and x_2 can be obtained from the equation for the instantaneous armature voltage drop

$$e_a=E_m\ \sin x-(E_g+E_t) \tag{30}$$

from the following condition:

For $x=x_1 \quad e_a=0$

Hence

$$E_m\ \sin x_1-(E_g+E_t)=0$$

and

$$x_1=\text{arc sin}\,\frac{E_g+E_t}{E_m}=\text{arc sin}\,a \tag{31}$$

The expression 31, of course, yields an infinite number of values for x_1, but the physical conditions limit them as follows:

$$x_1=\text{arc sin}\,a,\quad 0\leqq x_1\leqq\frac{\pi}{2} \tag{32}$$

$$x_2=\pi-x_1. \tag{33}$$

For an ideal rectifier $E_t=0$ and a is represented by formula 20a. Here the range of a is as follows:

$$0\leqq a\leqq 1 \tag{34}$$

Obviously, for a stalled motor

$$E_g=0;\ a=0;\ x_1=0;\ x_2=\pi \tag{35}$$

For the maximum possible theoretical speed of the motor

$$E_g = E_m;\quad a = 1;\quad X_f = X_s = x_1 = x_2 = \frac{\pi}{2} \tag{36}$$

The condition 36 is represented graphically in Figure 4 by a single point $X_s = X_f =$ 90 degrees.

The border case of the maximum possible firing angle $X_f = x_2$ for $0 \leq a \leq 1$ is represented by a straight line A–C connecting points (90°, 90°) for $a = 1$ and (180°, 180°) for $a = 0$.

It should be noticed that in the case $X_f = x_2$ we always have

$$X_s = X_f = x_2 \tag{37}$$

and thus for a particular a and $X_f = x_2$ a single point is obtained on graph Figure 4 for all possible values of $\cos\theta$, that is, all curves $X_s = f(X_f)$ for different $\cos\theta$ and the same value of a converge at a point lying on the straight line (90°, 90°), (180°, 180°) and corresponding to that particular value of a.

Since a given a determines the minimum possible firing angle x_1 (see equation 32), each family of curves $X_s = f(X_f)$ for different $\cos\theta$ and the same value of a will stop at a straight line $X_f = x_1 = \text{const}$, corresponding to that particular value of a.

It is apparent that for $\cos\theta = 1$ and a given a

$$X_s = x_2 = \text{const} \tag{38}$$

If we assume $X_f = x_1$ (minimum possible firing angle) then from equations 38 and 33 we get

$$X_s = \pi - X_f \tag{39}$$

The preceding equation represents a straight line A–B (90°, 90°), (0°, 180°) shown in Figure 4.

It can be readily understood that in the case of a single-phase full-wave rectifier the necessary condition for a pulsating noncontinuous current is as follows:

$$X_s < 180° + X_f \tag{40}$$

The equation

$$X_s = 180° + X_f \tag{41}$$

represents the border condition when separate successive pulses of current join with one another and is shown in Figure 4 as the straight lines B–D separating the region of pulsating current from that of continuous current.

It may be interesting to note that, with exception of formulas 40 and 41, all the preceding formulas and considerations hold for any multiphase-rectifier system as long as discreet pulses of current are obtained. The necessary condition for a pulsating current represented by expression 40 in case of a single-phase full-wave rectifier system will be modified in the following manner:

Three-phase half-wave rectifier system

$$X_s < 120° + X_f \tag{42}$$

Three-phase full-wave rectifier system

$$X_s < 60° + X_f \tag{43}$$

Six-phase rectifier system

$$X_s < 60° + X_f \tag{44}$$

Four-phase rectifier system

$$X_s < 90° + X_f \tag{45}$$

If for any of the preceding systems the corresponding condition 40, 42, 43, 44, or 45, is fulfilled, the system of curves $X_s = f(X_f)$ shown in Figure 4 can be applied directly to any of these rectifier systems.

The limitation 34 corresponds to the case of an ideal rectifier whose forward resistance is equal to zero (no voltage drop across the rectifier, $E_t = 0$). For an actual mercury vapor or gaseous discharge rectifier, however, formula 20 must be used rather than 20a since

$E_t \cong 15$ to 20 volts

and limitation 34 will become

$$k \leq a < 1 \tag{46}$$

where $k \cong 20/E_m$ corresponds to the condition of a stalled motor (see equations 20 and 20a) and is a low number of the order of magnitude of 0.04.

Part C. Peak Current and Its Angle of Occurrence

In order to determine the crest value of the current pulse we refer back to the equation 26.

Differentiating equation 26 with respect to x we find

$$\frac{R}{E_m}\frac{di}{dx} = \cos\theta\,\cos(x-\theta) - \frac{1}{\tan\theta}\times \left[a - \cos\theta\sin(X_f-\theta)\right]\epsilon^{-\frac{x-X_f}{\tan\theta}} \tag{47}$$

The maximum of the armature-current pulse will occur at the angle $x = X_p$ such that

$$\cos\theta\,\cos(X_p-\theta) - \frac{1}{\tan\theta}\times \left[a - \cos\theta\,\sin(X_f-\theta)\right]\epsilon^{-\frac{X_p-X_f}{\tan\theta}} = 0 \tag{48}$$

or after a simple rearrangement

$$\cos(X_p-\theta)\epsilon^{\frac{X_p}{\tan\theta}} = \left[\frac{a}{\sin\theta} - \frac{\sin(X_f-\theta)}{\tan\theta}\right]\epsilon^{\frac{X_f}{\tan\theta}} \tag{49}$$

Equation 48 or 49 represents the relationship between the peak-current angle X_p and the firing angle X_f, that is, represents X_p as an implicit function of X_f for different values of parameters a and θ. Similarly, as in case of functions 27 or 28 it is possible to calculate the value of X_p for each value of X_f (see limitation 29), although X_p cannot be represented by an explicit formula. The graphs of the function $X_p = f(X_f)$ for different values of parameters a and θ are shown in Figure 6.

If symbol X_p is substituted for x into the equation 22, we will get the expression for the peak value of the current pulse i_{max}.

$$i_{max} = \frac{E_m}{R}\left\{\cos\theta\,\sin(X_p-\theta) - a + \left[a - \cos\theta\,\sin(X_f-\theta)\right]\epsilon^{-\frac{X_p-X_f}{\tan\theta}}\right\} \tag{50}$$

where

$X_p = f(X_f)$

as represented by equation 48 or 49.

A more convenient form analogous to equation 26 is

$$\frac{i_{max}R}{E_m} = \cos\theta\,\sin(X_p-\theta) - a + \left[a - \cos\theta\sin(X_f-\theta)\right]\epsilon^{-\frac{X_p-X_f}{\tan\theta}} \tag{51}$$

Graphs of the function 51 for different values of parameters a and θ are shown in Figure 7.

It is useful to note that, for the theoretical case of zero inductance $\cos\theta = 1$, neither X_p nor i_{max} can be calculated from the equations 49 and 50 which assume an indeterminate form for $\theta = 0$. However, from Figure 14 it is apparent that for $\theta = 0$ we have

$$\left.\begin{aligned} &X_p = X_f \quad \text{when } X_f \geq 90° \\ &X_p = 90° \quad \text{when } X_f < 90° \end{aligned}\right\} \tag{52}$$

and

$$\left.\begin{aligned} &\frac{i_{max}R}{E_m} = \sin X_f - a \quad \text{when } X_f \geq 90° \\ &\frac{i_{max}R}{E_m} = 1 - a \quad \text{when } X_f < 90° \end{aligned}\right\} \tag{53}$$

Graphs of equations 52 and 53 are also shown in Figure 6 and Figure 7.

Part D. Armature Load-Speed Relations

Having established the relationship between the firing and stopping angles of the rectifier (see Figure 4), we now take up the problem of determining the average value of the resistive voltage drop in the armature circuit $I_{avg}R$ proportional to the average armature current, that is, proportional to the torque of the motor.

In order to obtain the average value of the armature current I_{avg} or the average value of the ratio $I_{avg}R/E_m$, one should integrate the function 22 or 26 between the limits $x = X_f$ and $x = X_s$, taking the average over one half-cycle of the applied voltage in case of single-phase full-wave rectification. Thus

$$I_{avg} = \frac{1}{\pi}\frac{E_m}{R}\int_{x=X_f}^{x=X_s}\left\{\cos\theta\,\sin(x-\theta) - a + \left[a - \cos\theta\,\sin(X_f-\theta)\right]\epsilon^{-\frac{x-X_f}{\tan\theta}}\right\}dx \tag{54}$$

However, it can be readily proved by actually performing the integration in equation 54 and taking into account the relationship 27 that the same result will be obtained in a much simpler way by integrating the voltage function rather than the current one.

With reference to Figure 14, it can be easily understood that the equation for the instantaneous armature voltage drop is

$$e_a = E_m\,\sin x - (E_g + E_t) \tag{30}$$

We obtain the average resistive voltage drop in the armature circuit by integrating the function 30 between the limits $x = X_f$

Electronic Control of D-C Motors

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IN recent years the need for an improved adjustable-voltage d-c motor drive operated from an a-c supply has been continually increasing. Although the conventional drive of that type using a motor–generator set, and sometimes referred to as the Ward-Leonard system, adapts a d-c motor to an a-c line and provides adjustable armature voltage for the motor, it does not solve all of the problems involved. Thus, good speed regulation at all speeds, ability to deliver full torque at speeds as low as $^1/_{50}$ of the rated speed of the motor, and automatically controlled acceleration and reversal are but a few vital problems which have been awaiting solution.

Since the development of mercury-vapor and inert-gas-filled tubes with controlled angle of ignition, such as thyratron and ignitron rectifiers, numerous attempts were made to apply them for armature control of d-c motors. Here, the firing angle of the rectifier can be controlled to vary the rectifier output voltage applied to the armature. Also, an additional controlled rectifier may be used to supply field current to extend the range of control above the rated or base speed of the motor.

Rectifiers for armature control of d-c motors were first used about ten years ago, but until recently that kind of drive has not been of major importance, primarily because of its excessively drooping speed-torque characteristic.

Recent engineering development, however, has improved the performance of the rectifier–motor system so that it has become one of the most perfected drives. It possesses a very good speed regulation throughout an extremely wide speed range and also incorporates a number of additional features such as fully automatic control of acceleration, deceleration and reversal of the motor.

An accompanying paper by E. H. Vedder and K. P. Puchlowski, "Theory of Rectifier–D-C Motor Drive,"[1] presents the mathematical analysis and a number of conclusions concerning the rectifier–motor system in general. This paper deals with certain specific characteristics and circuits of a modern rectifier motor drive and its advantages as compared to an elementary unregulated system.

Elementary System

The schematic circuit diagram of an elementary rectifier–motor system is shown in Figure 1. Thyratron tubes 1 and 2 are connected as a conventional single-phase full-wave rectifier and supply unidirectional voltage to the armature of the motor connected between the cathodes and the center tap of the rectifier transformer. The a-c grid voltage applied to the grids of the tubes from the control-grid transformer is shifted to lag in phase the anode supply voltage of the thyratrons by 90 degrees. The voltage applied to the primary winding of the control grid transformer is obtained from a conventional dephasing circuit consisting of a resistance $R5$ and a capacitance $C5$.

The grid voltage of thyratron tubes 1 and 2 also includes a d-c component which is a combination of two voltages across portions of $P1$ and $P5$, so that the a-c component is superimposed on the d-c voltage. This d-c voltage may be varied from a definite negative to a definite positive value, and in that way the firing angle of the tubes 1 and 2 may be adjusted arbitrarily by changing the setting of the potentiometer $P1$.

Although the speed of the motor may be varied by the adjustment of the speed-control potentiometer $P1$, the speed not only depends on the setting of that potentiometer but is affected to an overwhelming degree by the mechanical torque on the shaft of the motor. A particular setting on the speed-control dial does not in fact correspond to any particular speed which may be anything from zero to full speed of the motor, depending upon the value of the load torque.

In order better to understand the operation and performance of a rectifier–motor system, it seems useful to refer back to the characteristics of a conventional d-c shunt-wound motor.

It is a well-known fact that a conventional shunt-wound motor normally has a slightly drooping speed-torque characteristic which may be represented by the following equation:

$$n=\frac{E-IR}{C\phi}$$

where

n = number of revolutions per minute
E = d-c voltage across the armature
I = armature current
R = resistance of armature circuit
ϕ = main magnetic flux
C = coefficient of proportionality

Under normal conditions, E is constant, ϕ and R may be assumed constant, and the preceding equation will represent n as a linear function of I (or torque); see Figure 2. The speed of the motor will decrease with increasing torque because of the effect of the armature voltage drop which, assuming a constant magnetic flux ϕ, is directly proportional to the torque. The change in speed obviously

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and $x=X_s$ and taking the average over one half-cycle of the applied voltage.

$$I_{avg}R=\frac{1}{\pi}\int_{x=X_f}^{x=X_s}e_a dx$$

$$I_{avg}R=\frac{1}{\pi}\int_{x=X_f}^{x=X_s}[E_m \sin x-(E_g+E_t)]dx \qquad (55)$$

Thus

$$I_{avg}R=\frac{1}{\pi}\left\{E_m \Big/ -\cos x \Big/_{x=X_f}^{x=X_s} - (E_g+E_t) \Big/ x \Big/_{x=X_f}^{x=X_s}\right\}=\frac{1}{\pi}[E_m \times (\cos X_f-\cos X_s)-(E_g+E_t)(X_s-X_f)] \qquad (56)$$

Taking into account the relationship 20, we may write the expression 56 in the following form:

$$I_{avg}=\frac{1}{\pi}\frac{E_m}{R}[\cos X_f-\cos X_s-a(X_s-X_f)] \qquad (57)$$

or

$$\frac{I_{avg}R}{E_m}=\frac{1}{\pi}[\cos X_f-\cos X_s-a(X_s-X_f)] \qquad (58)$$

where $I_{avg}R/E_m$ is called the "torque factor."

Graphs of the equation 58 for different values of a and $\cos\theta$ are shown in Figure 8 and Figure 9.

Reference

1. ELECTRONIC CONTROL OF D-C MOTORS, K. P. Puchlowski. AIEE TRANSACTIONS, volume 62, 1943, pages 870–7.

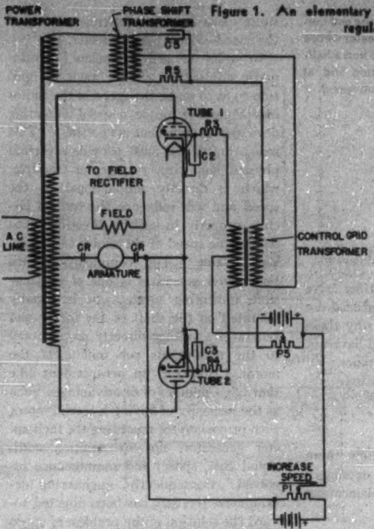

Figure 1. An elementary rectifier drive without speed-regulating circuit

depends upon the armature resistance and usually at rated speed will amount to say 8 to 12 per cent, with torque varying from no-load to full-load value. At lower speeds the percentage of speed regulation is higher, since, for lower values of E, IR constitutes a higher percentage of the armature voltage, and its influence is much more pronounced. The speed–load-current characteristics for a conventional d-c shunt-wound one-horsepower motor are shown in Figure 2.

However, in a rectifier system like the one represented in Figure 1, the interpretation of the speed–load equation must be properly modified. In the first place, neither the armature voltage E nor current I are straight d-c values any more, and one has to deal with the concept of average values. Also, the average value of voltage E across the armature is not constant any more, as it was in the case of a conventional d-c drive. When the speed-control potentiometer is set at a definite position, that is the firing angle of the armature-control thyratrons is constant during the time when torque is varied from no-load to full-load value, E itself becomes a function of torque, and in this way speed constitutes a much more involved function of I than that represented by the speed–load equation and graphs in Figure 2.

In order to clarify the meaning of the concept of armature voltage with reference to a rectifier–motor system, we refer to Figure 3, where the a-c anode supply voltage, the armature voltage, the voltage across the rectifying element, and the control grid voltage are represented as functions of time (or electrical angle). For the sake of simplification of the diagram, the latter has been drawn as for a single-phase half-wave system. For a single-phase full-wave as well as any multiphase rectifier, the considerations will be analogous. In order further to simplify the diagram, it has been assumed that the critical grid voltage of the thyratron tube is equal to zero, that is, the tube will break down at the point of intersection of the grid-voltage wave with the line of cathode potential of the tube. Let it be assumed further that the motor is stalled first and thus will behave as an ordinary resistive-inductive load.

Under these conditions, it is obvious that the thyratron will break down at the point T and will conduct current past point C up to the point M. The current pulse will have a shape as shown in Figure 3. The magnitude of the current may be very high here and will depend

1. Upon the firing angle of the thyratron tube.
2. Upon the armature resistance and inductance.

It should be noted in passing that by reason of the armature inductance the current will not stop flowing at the point C where the anode supply voltage passes through zero, since the inductive electromotive force of the winding will reverse its polarity and will tend to maintain the flow of current opposing the a-c supply voltage. Obviously, the current will stop flowing at the instant when the inductive voltage drop becomes equal to the applied a-c voltage.

After the motor has started running, and its speed has been established, the picture becomes entirely different. Now, a counter electromotive force is generated in the armature winding and, under steady-state conditions, represents a constant d-c voltage which may be assumed to be in direct proportion to the speed of the motor (under the assumption of constant magnetic flux). This counter-electromotive force E_g represented by the line O' in Figure 3 acts in the direction to oppose the flow of current and, consequently, to limit its value. The thyratron will break down at the point H (same instant as far as time is concerned) and will conduct the pulse of current up to the point P. Although, the counter-electromotive force cannot produce any current in the armature circuit (being blocked out by the rectifying elements), it actually will appear across the armature terminals over the period of time when the rectifier is not conducting and, contrary to a conventional d-c system, will overwhelmingly affect the reading of a d-c voltmeter connected across the armature. The armature voltage will have the shape as shown by a heavy line $O'GHKLNPQ$ . . . with respect to the zero line O (potential of the negative brush). The armature current will flow over a portion of the corresponding half-

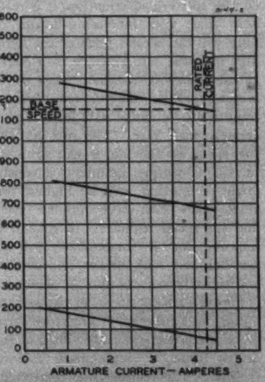

Figure 2. Speed-torque curves of a standard shunt-wound d-c motor supplied with direct current

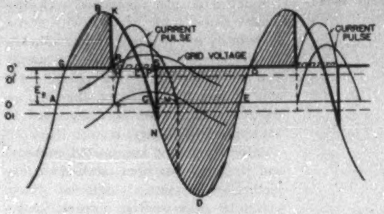

Figure 3. The voltage and current relations in a motor whose armature is supplied from a half-wave rectifier at zero and at approximately full speed

cycle actually much smaller than that for the stalled motor. The average value of the armature voltage drop *IR* which is proportional to the difference of the areas *HKL* and *LNP* and, consequently, the average value of current will be, of course, much smaller than that obtained for a stalled motor. Obviously, they both are determined by the torque of the motor.

If the load torque of the motor is increased, the armature current will also increase, as well as the *IR* drop, and it can easily be seen from Figure 3 that for the same firing angle of the thyratron tube the counter-electromotive force of the motor will have to decrease, and, obviously, speed will have to decrease. A very important point, however, is that the average value of voltage across the armature will also decrease, since this voltage will vary with counter-electromotive force as it can plainly be seen from Figure 3, instead of remaining constant as is the case for a conventional d-c drive. It must be emphasized that for any practical set of conditions the decrease in armature voltage *E* with increased torque will be very high. Thus, with reference to the speed–load equation, it is readily seen that, with increasing torque, not only will the second term of the numerator increase, but contrary to a conventional situation, the first term will decrease rapidly so that the revolutions per minute of the motor will drop at an extremely high rate. The rate at which the speed of the motor will drop with increasing torque, assuming a constant angle of ignition, will be highest for a single-phase half-wave rectifier system and the speed regulation in general will improve for a single-phase full-wave rectifier and will further improve with additional increase of number of phases.

Figure 4 shows curves of the armature voltage as a function of armature current for a one horsepower 230 volt d-c motor driven from a single-phase full-wave rectifier. The rectifier was connected in a system as shown in Figure 1, and constant angle of ignition was maintained throughout the test. The corresponding speed–armature-current characteristics of the same motor are shown in Figure 5. This striking behavior of a rectifier drive (compare Figure 2 for a conventional d-c drive) makes it clear enough why these drives in their elementary form have not acquired much practical importance.

Circuit for Improved Speed Regulation

As it was pointed out previously, there are two main reasons for an extremely poor speed regulation of the elementary rectifier drive for d-c motors:

1. **Armature voltage drop *IR*.**
2. **Consequent decrease in the armature voltage *E* with increasing torque.**

The latter reason is of prime importance and has a predominant effect on the behavior of a rectifier drive.

The situation can be substantially changed if the firing angle of the rectifier tubes (thyratrons or ignitrons) is adjusted automatically in accordance with torque variations, that is, advanced with increasing torque and delayed with decreasing torque so as to maintain constant speed for any particular setting of the speed-control potentiometer.

In order to provide means for automatic adjustment of the angle of ignition so as to make the speed of the motor independent of torque, a speed-indicating device or arrangement is necessary. This indicating device must provide a certain physical quantity, voltage for example, which is directly proportional to the speed and will influence the control circuits properly in such a direction as to restore the speed of the motor to its previous value. The commonly known tachometer generator is one of the possible indicating means. It is usually mounted on the shaft of the motor and generates a voltage directly proportional to the revolutions per minute of the motor. However, an arrangement like that has a number of disadvantages, such as the necessity of having a special motor with provisions for mounting the tachometer generator, not to mention additional cost, space, and maintenance involved. Consequently, engineering development recently has been directed toward the solution of the problem of speed indication in a manner which might avoid the use of any tachometer generator.

A circuit with a feedback from the armature voltage constitutes a definite improvement over the scheme shown in Figure 1, since the voltage across the armature can be maintained constant so that the main cause of poor speed regulation will be eliminated (see Figure 4 and Figure 5). In Figure 6 is shown a schematic circuit diagram of a rectifier drive for a d-c motor with substantially improved speed regulation resulting from the fact that at varying load the voltage across the armature automatically is maintained constant at a value corresponding to a particular setting of the speed-control potentiometer.

The circuit in Figure 6 consists of a

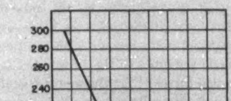

Figure 4. Average armature voltage as a function of load when armature is supplied from a single-phase full-wave rectifier for two different firing angles

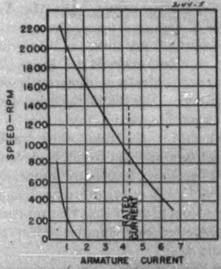

Figure 5. Speed-torque curve when armature is supplied from a single-phase full-wave rectifier for two different firing angles

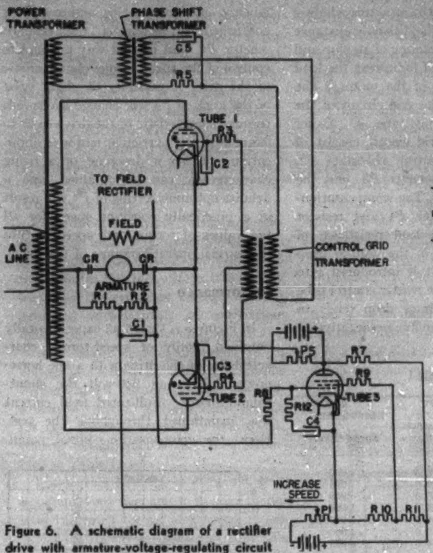

Figure 6. A schematic diagram of a rectifier drive with armature-voltage-regulating circuit

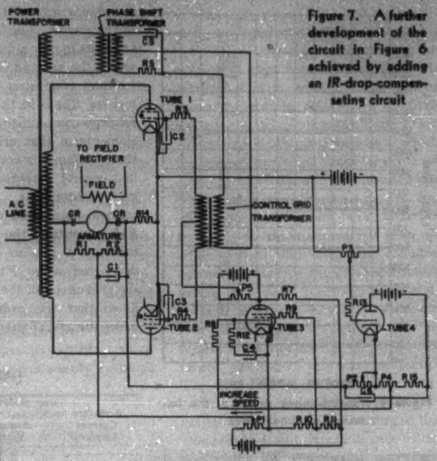

Figure 7. A further development of the circuit in Figure 6 achieved by adding an *IR*-drop-compensating circuit

single-phase full-wave rectifier system comprising the power transformer, thyratron tubes 1 and 2, the armature winding of the motor, and the speed-control circuit comprising the control tube 3 and its allied circuits. For the sake of simplification, auxiliary sources of d-c voltage are designated as batteries. As in the circuit shown in Figure 1, the voltage applied to the control grids of the thyratrons 1 and 2 consists of an a-c component superimposed on the d-c component. The a-c grid voltage component is obtained through the medium of the control grid transformer from a phase-shift circuit and lags the anode supply voltage by 90 degrees. The variable d-c component is developed in the following portion of the thyratron grid circuit: cathode—*R*2—portion of *P*1—*R*10—*R*11—*R*7—portion of *P*5—center tap of the secondary winding of the control grid transformer. Voltages across *R*2 and the active portion of the potentiometer *P*1 may be disregarded here since they are low as compared with voltages across *R*10, *R*11, *R*7, and *P*5, and since they practically cancel each other. The sum of d-c voltages across *R*10, *R*11, and *P*5 has a constant value whereas the voltage across *R*7, that is, the load voltage of the control tube 3 varies in proportion to the plate current of the preceding tube. This variable voltage determines the value of the d-c component of the thyratron grid voltage and, consequently, the firing angle of rectifiers 1 and 2. The foregoing combination of voltages is designed in such a way that, when the plate current of tube 3 is equal to zero, the thyratron tubes will supply maximum voltage to the armature of the motor. When the plate current of tube 3 is maximum, no voltage is supplied to the armature by thyratron tubes 1 and 2.

The control grid circuit of tube 3 can be traced as follows: cathode—active portion of *P*1—*R*2—*R*8—control grid. The voltage across the active portion of the speed-control potentiometer *P*1 is controlled arbitrarily, simply by turning the knob of that potentiometer. The voltage across *R*2 is proportional to the armature voltage of the motor and represents the feed-back voltage of the system. It should be noted that, although the armature voltage contains a very sharp ripple as plainly seen in Figure 3, a proper filtering effect is secured by means of circuit *R*1—*R*2—*C*1, so that the voltage across *R*2 does not contain any appreciable ripple.

When the motor is running at a certain speed, the voltage across the active portion of the speed-control potentiometer *P*1 and the voltage of opposite polarity across *R*2 will be balanced against each other in such a way that a resulting negative voltage of a few volts will be applied to the control grid of tube 3. The aforementioned grid voltage will correspond to a definite voltage across *R*7 and thus to a definite firing angle of tubes 1 and 2. However, if the slider of *P*1 is moved away from the cathode of tube 3, this balance will be momentarily disturbed. The control grid of tube 3 will now become more negative; the voltage across *R*7 will decrease, thus advancing the firing angle of the thyratron tubes. This will result in an increase in armature voltage and, consequently, in speed of the motor. The armature voltage and the speed will increase to such a degree that a new balance of the system will be established at a slightly more negative resultant grid voltage of tube 3. Thus, it can readily be seen that the speed of the motor will increase when the slider of *P*1 is moved away from the cathode of tube 3 and will decrease when *P*1 is turned toward the cathode.

Furthermore, it is apparent that the feed-back arrangement incorporated into the circuit Figure 6 will eliminate the variations of armature voltage with load and at the same time stabilize the operation of the system. Supposing that the torque applied to the shaft of motor is being increased, and in accordance with previous considerations the armature voltage tends to decrease, it is obvious that the grid of tube 3 will become slightly more negative just enough to advance the firing angle of thyratron tubes in such a way as to maintain the voltage across the armature at an essentially constant value. It is important to remember that

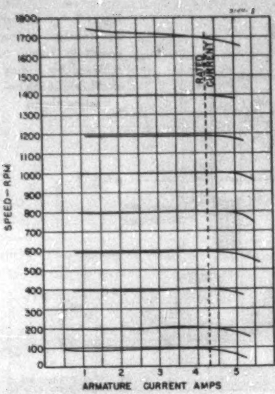

Figure 8. Speed-torque curves of the improved electronic drive

the control tube 3 provides the required high degree of amplification so that a change in the armature voltage amounting to a fraction of one volt will result in an appreciable shift of the angle of ignition of the thyratron tubes.

IR-Drop Compensation

The voltage across the armature is not a very accurate indication of speed, on account of the armature voltage drop *IR* which, even at higher speeds, may result in about ten per cent speed regulation and at lower speeds has a still greater effect on variation of speed with load.

In order to eliminate the influence of the armature voltage drop on the speed-torque characteristic of the drive, the schematic circuit diagram Figure 6 can be developed further by the addition of an *IR* compensating circuit. The feedback for this compensating circuit is taken from the armature current, and the firing angle of the rectifier tubes is additionally adjusted in accordance with load variations in such a way that the armature voltage will increase with load, providing proper compensation for the armature voltage drop.

A simplified schematic circuit diagram incorporating an armature-voltage-drop compensating circuit is shown in Figure 7.

As it can readily be seen, the diagram shown in Figure 7 has been derived from that shown in Figure 6 by the addition of another amplifier tube 4—high vacuum triode—and its allied circuits. The designations of circuit elements common to circuits in Figure 7 and Figure 6 are the same. A new resistor *R*14 has been introduced in series with the armature winding of the motor. This additional resistance amounts to only a fraction of an ohm and serves the purpose of indicating the load (torque) conditions of the motor. The resistor *R*14 is in the grid circuit of the *IR*-drop-compensating tube 4. Other elements of that grid circuit consist of the antihunting-control rheostat *P*2, bias-control potentiometer *P*3, and the grid resistor *R*13. The compensation-control potentiometer *P*4 and resistor *R*15 constitute the load resistance of tube 4. A portion of the voltage drop developed across *P*4 is introduced into the grid circuit of the master control tube 3, so that this voltage drop tends to make the grid of tube 3 more negative.

Table I

Armature Current (Amperes)	Armature Voltage	Speed (Rpm)
1.00	129.6	1,000
1.45	130.0	996
3.25	133.0	999
3.50	133.6	1,000
4.00	134.2	997
4.30	135.0	1,000
5.00	134.0	993
0.75	53.0	400
1.20	53.9	396
1.80	55.0	393
2.10	56.0	393
2.50	57.4	395
2.90	58.1	396
3.40	60.0	402
3.90	62.0	404
4.50	64.0	401

Referring to the grid circuit of the master control tube 3 (Figure 7), it will be seen that the resultant grid voltage of the aforementioned tube is a combination of three voltage components: the negative component obtained from the speed-control potentiometer *P*1, the positive component across the armature-voltage feed-back resistor *R*2, and the negative component across a portion of the *IR* compensation-control potentiometer *P*4. If it is assumed that the torque of the motor is being increased, the armature current will increase and so will increase the average voltage drop across *R*14, making the grid of tube 4 less negative. Consequently, the plate current of this tube will increase and so will increase the voltage drop across *P*4, making the control grid of the master control tube more negative. In accordance with previous considerations, this will result in an additional advancement of the firing angle of the thyratron tubes 1 and 2, so that the armature voltage will be increased additionally to compensate for the increase in the armature voltage drop.

It should be emphasized that the amount of compensating action can be controlled by means of the potentiometer *P*4, and in that way a complete control of the speed-torque characteristic of the drive is possible. A slight twist of the knob of *P*4 will change the speed-torque characteristic of the drive so as to adapt it to any particular motor or application. Either a drooping or a rising characteristic can be obtained, and a definite optimum setting of *P*4 will result in a practically constant speed for all the values of torque and speed within the normal operating range of the drive.

Performance

In Figure 8 is shown an experimentally obtained family of speed-torque characteristics of the drive with a one horsepower 1,750 rpm 230-volt d-c shunt-wound motor. Full-rated field current was maintained throughout the test. Here, the compensation-control potentiometer *P*4 was set at an optimum setting to give a close speed regulation over a 20:1 speed range. It should be noted that the setting of *P*4 was the same for the entire range of speeds, that is, one single adjustment was sufficient to take care of the armature-voltage-drop compensation for all the speeds available on the dial of the speed-control potentiometer *P*1. From the graphs shown in Figure 8 it may be seen that, within the armature current range from no-load current to full-rated current of 4.3 amperes, the speed of the motor did not vary more than by three

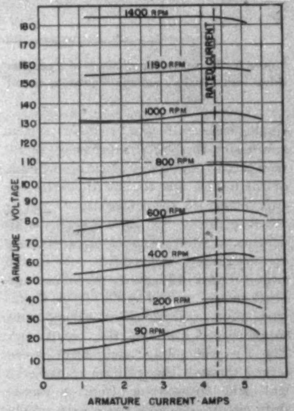

Figure 9. Armature voltage of the improved electronic drive

Compare with Figure 4

per cent for any speed within the speed range from the base (rated) speed of the motor down to $^1/_{10}$ of that value. At $^1/_{10}$ of the base speed, regulation was about 6 per cent. The increased drooping of the speed–load-current characteristics for currents exceeding the rated value of 4.3 amperes was caused by the current-limitation feature of the drive, not included in diagram Figure 7, which will be discussed later.

Experimental graphs of armature voltage versus load current corresponding to speed–current characteristics of Figure 8 are shown in Figure 9. Here, it is plainly seen how the armature voltage is being increased automatically to compensate for increasing armature voltage drop.

Since small changes in speed or in voltage can hardly be detected in graphs Figure 8 and 9, the actual values of speed, armature current, and armature voltage for two different settings of the speed-control potentiometer have been assembled in Table I.

From graphs shown in Figure 8, it is apparent that the motor is able to deliver full-rated torque at a speed as low as $^1/_{10}$ of its rated speed and that this speed is maintained essentially constant over the entire range from no-load to full-load torque.

Another important feature of the drive is that, in accordance with speed–load-current characteristics shown in Figure 8, the speed of the motor depends only on the setting of the speed-control potentiometer, that is, it is possible to come back to a required speed simply by selecting it on the dial of the control.

Automatically Controlled Acceleration of the Motor

The starting and acceleration of a d-c motor constitutes a problem in itself, especially when an automatic acceleration is to be considered. In general, there are two methods by which automatic acceleration can be achieved: the time-delay acceleration and the current-limit acceleration. In the first case the voltage applied to the armature is being increased gradually to its normal value, so that the motor is allowed to start smoothly. The current-limit acceleration is based on the fact that means are provided which do not permit the armature current to rise above a predetermined value. The current-limit method of acceleration has definite advantages in that it enables an acceleration consistent with the kind of load. Thus, the time of acceleration will be different for different loads, depending upon the mechanical inertia of the rotating system, so that starting will always take place under the most advantageous conditions, considering the time and the smoothness of acceleration.

An electronic-control system such as just described is exceptionally well suited to be developed still further, so as to include additional circuits which would perform the function of automatic current-limited acceleration of the motor. A diagram of a complete system incorporating all the features of circuits shown in Figure 6 and Figure 7, as well as performing the additional function of controlling automatically the acceleration, is represented in Figure 10. It will be noticed that a third high-vacuum control tube has been added to the circuit. This tube 5 is a pentode characterized by a sharp cutoff and has a power supply common with tube 3. The grid circuit of tube 5 includes the load-current indicating resistor *R*14 and the biasing potentiometer *P*6. The armature voltage indicating resistor *R*2 is in the plate circuit of tube 5. A closer examination of the circuit will reveal that the action of the current-limiting tube 5 is just opposite to that of the *IR*-drop-compensating tube 4. Thus, in general, an increasing armature current will tend to make the control grid of tube 5 less negative through the influence of the voltage drop across *R*14 and in that way will cause the plate current of tube 5 to increase. This will result in an increase of voltage drop across *R*2, and the control grid of the master control tube 3 will become less negative, so that the plate current of that tube will increase. In accordance with previous considerations, this will mean the delaying of the firing angle of the thyratron tubes 1 and 2.

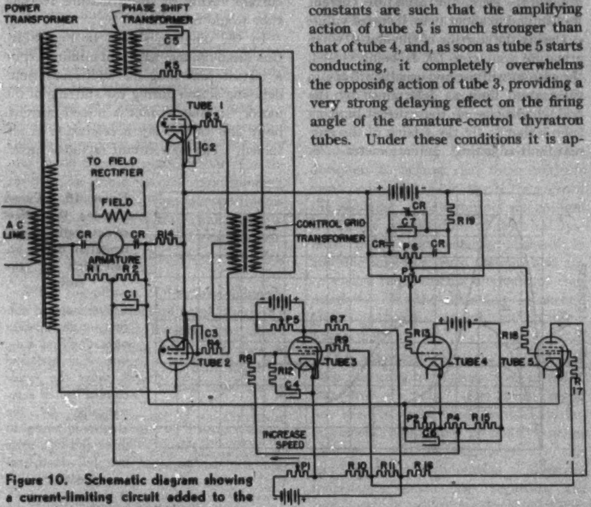

Figure 10. Schematic diagram showing a current-limiting circuit added to the basic circuit in Figure 7

The circuit is so designed that, within the normal operating range of armature currents, say from zero to full-rated current, tube 5 is inoperative, that is, does not conduct any current being biased off by a highly negative voltage from *P*6. Thus, within that range the armature current affects only the *IR*-drop-compensating tube 4, as it was previously described. If, however, the armature current increases above a definite value determined by the setting of the potentiometer *P*6, tube 5 will start conducting. The tube characteristic and the circuit constants are such that the amplifying action of tube 5 is much stronger than that of tube 4, and, as soon as tube 5 starts conducting, it completely overwhelms the opposing action of tube 3, providing a very strong delaying effect on the firing angle of the armature-control thyratron tubes. Under these conditions it is apparent that, when the armature current increases above a certain value because of an overload, for example, the tube 5 will start conducting, and the firing angle of the thyratron tubes will be delayed which will, of course, oppose the tendency of current to increase. With still increasing load torque, the motor will stall when the current in the armature winding reaches its maximum value, as determined by the setting of the potentiometer *P*6. The current limit is adjustable by means of potentiometer *P*6. In most practical cases it is kept within the range of, say, 1.8 to 2.5 times rated armature current of the motor.

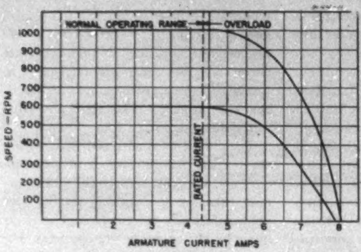

Figure 11. Curves showing the entire range of speed-torque characteristic

Current is held by the limiting circuit to about twice the rated value

In Figure 11 are shown two speed–armature-current characteristics of the drive for two different settings of the speed-control potentiometer taken over the entire range from no load to a maximum current corresponding to a locked armature. Here, the armature current was limited to about twice its rated value.

During the starting of the motor, when armature contactor *CR* is being closed, the current will rise and usually will attain the current limit as determined by the grid conditions of the current-limiting tube 5, that is, by the setting of the current-limit-adjusting potentiometer *P*6.

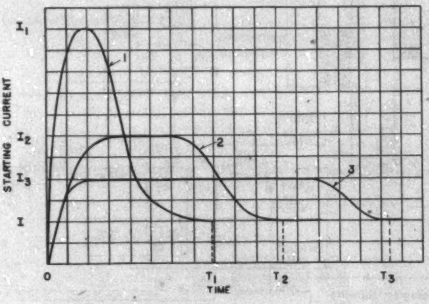

Figure 12. Curves showing the effect of automatic current limitation during acceleration of the motor

Curve 1 shows current inrush in motor operating from a d-c line. Curves 2 and 3 show the motor current limited automatically by the electronic control to three and two times the full load, respectively

In that way it is apparent that the starting torque of the motor will be limited also, and thus a smooth and shockless starting is obtained.

However, since the current-limiting circuit can become operative only after the average current has reached a definite value, it is obvious that the current-limit tube 5 will not affect the angle of ignition at which the first breakdown of the thyratron tubes will occur, immediately after the closure of the armature contactor *CR*. Thus, the first breakdown will take place at a firing angle which is not delayed by the action of tube 5 and is determined solely by the setting of *P*1. Under these conditions the first pulse of armature current may attain a considerable magnitude, being limited only by the resistance and inductance of the armature winding.

If the speed-control potentiometer *P*1 is set for a high speed, the first peak will be usually much higher than the peak-current rating of the armature rectifier tube would tolerate.

In the diagram shown in Figure 10, this disadvantage has been eliminated by a circuit providing an additional time-delaying action during the starting of the motor, so that in fact a mixed current-limit and time-delay acceleration is obtained. The new circuit includes capacitor *C*7, resistor *R*19, two normally open contacts of the contactor *CR*, and one normally closed contact of that contactor. When the armature contactor *CR* is open, it is apparent that the voltage across the capacitor *C*7, as well as across the potentiometer *P*6, is equal to zero. In that way the grid voltage of tube 5 is zero, and the tube is conducting full current, resulting in a high voltage drop across *R*2. Under these conditions the master control tube 3 will conduct full current, regardless of the setting of the speed-control potentiometer *P*1, so that the rectifier tubes 1 and 2 are not allowed to conduct.

At the instant of closure of the armature contactor *CR*, the voltage across *P*6 is still equal to zero, and thyratron tubes 1 and 2 cannot conduct. However, the voltage across the current-limit-control potentiometer *P*6 will rise gradually, following the charging of the capacitor *C*7 through the resistor *R*19. In that way the tube 5 is being biased off gradually, and the firing angle of the thyratron tubes will be advanced gradually, so that the magnitude of the first current pulses will be limited, even before tube 5 responds to the feedback from the armature-current-indicating resistor *R*14. The time delay provided by the capacitor *C*7 is of short duration corresponding to 3–4 cycles only, and the rest of the acceleration is controlled by the tube 5 in accordance with the feedback from *R*14, in a manner as previously described.

The process of automatically controlled acceleration of the motor can be understood best from Figure 12 where the starting current of the motor is plotted as a function of time. The ordinates of the curves 2 and 3 represent the average values of particular current pulses. Curve 1 represents the starting current of a motor with a d-c voltage applied to the armature at the instant *O*. Obviously, the current will not rise instantly because of the inductance of the armature winding. The starting current will quickly reach its peak I_1 and will drop to the value *I* determined by the load following the acceleration of the motor. If the

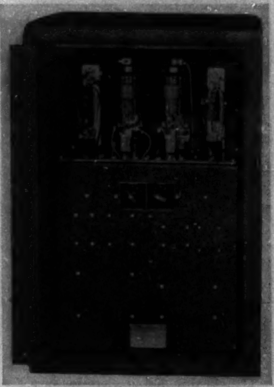

Figure 13. A one-horsepower single-phase electronic drive incorporating accurate speed regulation, *IR*-drop compensation, and current-limited acceleration

inertia of the load is high, this peak value I_1 will be limited only by the resistance of the armature circuit. The total time of acceleration is T_1, as shown on the graph. Curves 2 and 3 represent the average value of starting current as a function of time with the current limit set at three times the rated current I for curve 2 and two times the rated current for curve 3. T_1, T_2, and T_3 represent correspondingly the time of acceleration for each particular curve. It is apparent that, the lower the current limit, the longer will be the time of acceleration and the smoother the starting of the motor. By means of the potentiometer $P6$ (see Figure 10), the current limit can be varied and in that way the starting torque, the time, and smoothness of acceleration can be controlled.

When the armature contactor CR is being opened, it can be seen that the voltage across the biasing potentiometer $P6$ will drop instantly to zero, and the current-limit tube 5 will start conducting full current as soon as CR contacts start to move. The plate current of the master control tube 3 thus will rise to its full value corresponding to zero grid voltage, and, consequently, the rectifier tubes 1 and 2 will stop conducting, so that only a slight spark can appear on the main armature contacts, and no arcing will occur. It may be interesting to note that, owing to such a circuit arrangement, the main armature contacts need not be designed to handle the actual rupturing of the armature current, and this will often permit the use of contactors of smaller size to handle the armature circuit.

Conclusions

The control circuits described in this paper have included only basic elements needed for full understanding of the performance of the drive. The addition of a few more elements will secure a number of additional functions and features. A complete commercial control as shown in Figure 13 will usually include means for motor reversal, dynamic braking, antiplugging, and extension of speed range above the base speed by field weakening.

A control like that is a package unit complete in itself, and it incorporates all necessary devices to provide adequate protection against sustained overloads, low voltage, and short circuits.

The drive is normally controlled from a remote pushbutton station which contains forward, reverse, and stop pushbuttons and speed adjuster.

The important characteristics and features of the modern rectifier drive are as follows:

1. Wide and stepless speed range.
2. Automatic regulation to a preset speed.
3. Full torque at low speeds.
4. Smooth shockless current-limit acceleration with full control of starting torque.
5. Automatically controlled reversal of the motor.
6. Several preset forward and reverse speeds, if desired.
7. Elimination of all rotating parts except the motor.
8. Convenient remote mounting of small control stations.
9. Operation from a conventional a-c line.

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Radio-Noise Elimination in All-Metal Aircraft

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I. Introduction

RADIO noise is the result of interaction among four components of the airplane, namely, electrical equipment, the radio receiver, the electric wiring, and the antennas. These components are produced by three different groups of manufacturers who have until recently made no effort to co-ordinate with each other on the radio-noise problem, the solution of which requires that these components be considered conjointly and that each party accept his part of the total responsibility. Until recently, this joint responsibility was not recognized, but each party was held to a rigid set of specifications which restricted them all to a very narrow line of behavior and made them responsible only for the meeting of some specific requirements. As a consequence, the various manufacturers considered their obligation as fulfilled when these specific requirements were met, whether the radio system was noise-free or not.

With the advent of wartime demands for increased production rates, there is a tremendous added incentive to adopt any feasible method which promises to expedite construction and save material. The increased use of special radio equipment requires that this problem be approached from an engineering standpoint in order that the most economical use may be made of available radio-noise-elimination methods. An intense eleventh-hour effort must be made to apply these radio-noise-elimination methods, so that obsolete practices can be replaced by modern-design practices, and the designer will accept the responsibility for them. This may be done by the application of well-known principles of circuit coupling, shielding, and filtering to aircraft electrical systems.

Shielding as a means of radio-noise reduction was first applied to ignition systems which were found to be intense sources of radio noise.[1-4] The exacting requirements of this type of shielding led designers to believe that these methods must be applied meticulously throughout the airplane. This erroneous assumption led to the specification of electrical systems which were entirely enclosed in metallic shielding, with extreme and unnecessary requirements of electrical conductivity of the shielding elements. (Junction boxes, conduit, and bonding connectors.)

It is noteworthy that the utilization of such measures has not resulted in radio-noise-free systems. The main result has been to make these burdensome requirements absolutely prohibitive under wartime conditions. Consequently, this has forced a reconsideration of these requirements, particularly regarding the installation of conduit for circuit shielding.

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The elimination of the majority of conduit from military aircraft is resulting not only in economy of labor and material but also in minimizing vulnerability to gunfire and facilitating repairs in flight and in the field.

The weight saving is also considerable, as the removal of conduit brings with it the removal of junction boxes and plugs and in many cases reduces the need for wiring devices, such as terminal strips, connectors, and so forth, as circuits need not make as many junctions as before.

By the application of sound engineering principles it has been found that many of the restrictive measures once considered essential may be eliminated. Practically all conduit, with the exception of the conduit on the ignition system and some radio wiring, may be eliminated. This of course requires that the system of rigid detail specifications be abandoned and the emphasis put upon performance requirements of the completed airplane.

The recent change in policy of the Army and Navy specifications is a large step in the proper direction. This step will tend to exert the necessary pressure on the "three parties" previously noted to cause whatever co-ordination is necessary between them to establish the proper responsibility of each.

II. Nature of Radio Noise

There are numerous sources of radio noise, but as far as radio noise in aircraft is concerned they may be placed in two general categories. One is produced by atmospheric phenomena, and the other is produced by aircraft electrical equipment.

The radio noise produced by atmospheric phenomena is often of great intensity and manifests itself in the receiver output in the form of intermittent crashes from lightning discharges or a more or less continuous noise which ranges from a harsh crackling to a high-pitched rather musical tone.

Investigations of this type of noise have disclosed that the electrical conductivity of an airplane flying into charged precipitation particles causes changing field gradients such that currents flow to or from the airplane in order to neutralize this condition. Discharge currents from the airplane produce localized noise fields which couple to the antenna circuit.

Some attempt has been made to reduce the production of localized noise fields by the use of a trailing conductor attached to the tail of the airplane in order to dissipate the accumulated charges in a region where their field of influence does not reach the antennas. The success of these static dischargers depends to some degree upon the existing meteorological conditions being more effective in dry snow and sleet than in warm rain. The device is, at present, only one of many devices being tried as a solution to this problem.

Since we know that shielded loop antennas provide considerable relief from precipitation, static interference is due to the fact that the electric-field component of the total noise-influence field is large in comparison to its magnetic component where precipitation static effects are concerned. The loop is shielded against the electric-field component but is receptive to the magnetic component of both the noise and signal field. An over-all gain in signal-to-noise ratio results, as the electric- and magnetic-field components of the signal are equal at distances greater than several wave lengths away from the signal source.

The intensity of the aforementioned type of radio noise is extremely high, and only a partial solution to this problem has been obtained thus far. In the normal case this type of radio noise will easily override radio noise from any other source. This problem is well covered by other observers,[9,10] and is not under further consideration in this writing.

Under conditions and in regions where none of the disturbances already noted are in existence there still remains a certain background level of noise field intensity generated by cosmic disturbances. This noise field varies between 2 and 20 microvolts per meter, in the 0.2 to 50 megacycle range.[11,12]

The preceding paragraph represents the limiting background level for radio reception and thus provides a starting point in determining the degree to which noise-reduction measures must be carried out. This analysis is concerned chiefly with the determination and elimination of radio-noise coupling produced by electrical equipment associated with the airplane itself and with the elimination of as many coupling paths as possible by proper design of radio equipment and radio installations.[11,12]

The correction of radio-noise problems at the Douglas Aircraft Company between 1938 and 1940 was carried out mostly on a trouble-shooting and fix-it basis. The inadequacy of this approach led to the realization that the problem should be handled by using proper engineering methods in the first place so that conditions leading to the coupling of radio-influence sources to radio equipment could be avoided. Extensive bonding and shielding could then be reduced to the amount actually required to ensure a noise-free installation.

III. General Considerations

In order to provide a proper basis for attacking the problem it was necessary to institute a program of preliminary research work to indicate the scope of the problem and the most economical avenues of attack.

The objectives of this program were to determine all paths through which radio influences produced by aircraft electrical equipment were being coupled to the receiver in a manner to produce radio-noise output and to determine the means available to eliminate these coupling paths. The major noise coupling paths were found to be:

1. Induction coupling between unshielded receiver lead-in conductors and nearby electrical equipment (and wiring in conduit in cases where the conduit was not bonded effectively to structure). It must be noted that open wiring was not at this time being considered for installation in an airplane.

2. Conduction coupling through receiver power-supply leads, or through other conductors entering the receiver.

3. Various conbinations of induction and conduction coupling, such as conduction coupling to the transmitter.

At this point is was recognized that conductive noise-influence coupling was a problem separate from that of shielding, and that many former cases of radio noise actually caused by conductive coupling were blamed on imperfect shielding and bonding. The result was that more extensive care was given to shielding and bonding at a time when the accent should have been placed on a determination of the actual trouble and a proper analysis of the whole problem. The development of receiver filtering to eliminate conductive coupling as it was known to the automobile field was apparently being ignored by makers of aircraft receiving equipment.

The methods available for aircraft-radio-noise control were found to be no different than those applied to other fields, but the application of these methods to a specific device, the all-metal airplane, appeared to be capable of considerable modification. The following salient observations indicate the manner in which shielding, bonding, and filtering are related to the all-metal airplane:

1. The all-metal structure provides excellent shielding between the internally produced noise-influence sources and the antennas.

2. Compartmentation of the airplane provides further shielding between certain areas.

3. Inherent structure of the airplane provides incidental shielding from one point to another within a compartment.

4. The ground plane effect of both primary and secondary structure provides shielding whose effectiveness depends upon its geometry.

5. The effects of openings in the fuselage, or openings between compartments, in decreasing the effectiveness of such shielding depend also upon the geometry of the particular case.

These basic observations led to an enlargement of the original research program to include a consideration of open wiring, as there was definite evidence that noise-influence coupling was already restricted to localized points by reason of the inherent shielding ability of the airplane structure.

A detailed consideration of the function and application of shielding is given later.

6. Bonding from the standpoint of radio-noise reduction is entirely subservient to shielding and is not a means in itself. The use of bonding connectors, jumpers, and so forth, is simply a means of making shielding electrically effective. It is to be especially emphasized at this point that the ohmic resistance alone of a bonding connector is no measure of its effectiveness. Its impedance at the radio frequency under concern is usually of much greater importance, as a very short connector may have a reactive impedance of several ohms at 20 megacycles.

Bonding, to say the least, has been overdone to a fantastic degree, resulting in the accent on radio-noise reduction being placed on the bonds and bonding connectors themselves rather than on their function.

7. Were all shielding and bonding accomplished to the necessary degree to eliminate induction coupling to receiver lead-in conductors and were the necessary filtering incorporated in all receiver circuits to eliminate conduction coupling, there would be no necessity for the installation of separate filter units in the airplane. Since this cannot be accomplished overnight, the radio-interference filter remains a handy device to aid in reducing radio-influence-voltage (RIV) coupling where this results in actual receiver-noise output. The most legitimate use of such filters is in eliminating conduction coupling to receivers when it occurs to a degree resulting in noise output. In this application, the filter is placed as near the affected (receiving) equipment as possible, as it is most effective at this point and its weight is least.

It is obvious that as soon as the necessary filtering units are incorporated in the equipment adversely effected by RIV that the aircraft manufacturers will no longer be concerned with installing these units separately. If induction RIV coupling to lead-in conductors occurs, the choices of correction lie among shielding the lead-in, shielding the offending wiring (or device), or installing a filter between the point of coupling and the RIV source.

If shielding is done properly, the installation is free of induction coupling no matter what changes occur to the source of RIV. If filters are used instead, they become heavier as the RIV source is approached and each change in the electrical equipment may dictate new filter requirements.

The preliminary analysis has shown that the design of the completed radio system must take into accord all cofunctional parts of the system and that acceptance of the system must be based on the performance of the radio equipment as installed in the airplane and not on measurements made on the separate parts of the system. Such performance requirements must be augmented perforce by certain specific requirements where these definitely are known to be required. Such requirements must, however, be based on actual proof of their need rather than on the basis that their inclusion will provide a safeguard for conditions that may arise in the future. This attitude to date has been generally accepted with respect to shielding and bonding but has not been carried out with respect to filtering because:

1. Early experience with open-wiring systems has led to some transitional difficulties which were cured temporarily by the application of filters. This has led to an abnormally extensive use of filtering in order to cover up a poor job of RIV decoupling that should have been accomplished by proper placement and/or shielding of lead-in conductors. The outcome of this has been the arbitrary establishment of certain RIV limits to aircraft electric circuits and connected equipment units. The principles that apply to shielding and bonding also apply to filtering, and it is important that arbitrary limits or requirements of RIV, design requirements, and so forth, be stipulated only where these have been shown necessary by test under operating conditions.

2. Measurements of RIV on installed wiring or equipment are no measure of receiver-noise output unless a specific test is made to determine such correlation or coupling factors. Such a test must be made under the actual conditions of installation in the airplanes as any deviation from the actual conditions of installation in the airplanes will usually invalidate results entirely. It is hence a fallacy to ascribe other than academic significance to measurements made at any point other than the receiver output, unless the coupling factors to various noise sources have been determined. It is thus meaningless to stipulate any general limits of RIV, since acceptable limits vary tremendously from one installation to another.

IV. Shielding Design Considerations

Screening of magnetic and electric induction fields is attained by the use of shielding elements. In the practical case, these elements may take the form of an already existing metal fuselage, compartments or partitions therein, or portions of the structure so situated that their inherent screening effect can be utilized. Where necessary, shields or enclosures are constructed additionally.

1. All-Metal Structure

The all-metal structure of present military aircraft has been found to constitute a very excellent shield capable of screening internally generated noise fields from the antenna. That this shield is not complete structurally does not alter the basic premise and only means that the effect of openings, such as windows and plastic glass enclosures, gun turrets, and so forth, must be considered in the layout and placement of wiring. Such openings represent the possibility of induction coupling between antennas and internal-noise fields; however, tests show that this coupling can generally be neglected unless either the antenna or the noise-field source (usually a wiring bundle) is very close to the opening. It is usually a simple matter to route the wire bundle a satisfactory distance away from such openings or to shield it in some other manner. In the extreme case it may be necessary to utilize a piece of metal conduit to shield the conductors for a short distance if a test shows this to be necessary. Attenuation because of aircraft structure is usually greater than 60 decibels.

2. Electrical-Equipment Noise-Influence Fields

The induction fields associated with aircraft electrical equipment, such as generators, dynamotors, and so forth, are at their greatest intensity directly at the device; however, this intensity drops very rapidly with distance, so that it may be negligible when measured at a distance of a few feet. When parts of the aircraft structure intervene between a noise-field source and the point of observation, a further drop in field strength is noted because of the screening effect of the intervening structure. The attenuation obtained depends upon the geometry of the source circuit, the detection circuit, and that of the intervening shield.

3. Shielding of Receiver Lead-in

If the lead-in conductor of every receiver could be shielded totally from the receiver to the antenna insulator, there would be no problem with regard to noise-influence fields coupling to this conductor. It is, however, difficult to shield completely the lead-in conductors of aircraft receivers unless these are very short.

A compromise must therefore be made between what is desired and what may be obtained in practice.

4. Shielding of Wiring

Since the shielding of electrical wiring, other than antenna lead-in conductors, can be done without regard to the capacitance of the shield, it is possible that portions of the wiring coming into close inductive relationship with the lead-in can be more easily shielded than the lead-in. In this case, the shielding needs to extend only to the point at which the wiring is again shielded inherently by the structure. It is, of course, poor design to place dynamotors and other equipment known to produce very intense noise fields, close to the receiver lead-ins, thus pointedly aggravating the shielding problem.

There is no substitute for experience in the matter of determining just how far design can ensure a satisfactory installation when noise fields very close to the lead-in are concerned. The present analysis has greatly reduced the areas under consideration so that the cost of adding a little more protection than is actually necessary as a safety factor is small compared to treating the entire airplane in this manner.

5. Placement of Radio Equipment

The considerations of noise coupling to the lead-in immediately suggest the placement of the receiver, transmitter, and antenna switching relay very near the antenna lead-through insulator so that only those noise field sources within a few feet of the receiver lead-in need be considered.

This narrows the investigation to a small area at which radio-noise-control methods can then be concentrated.

Such placement will result in the following desirable advantages:

(*a*). That portion of the lead-in (from the insulator to the relay) which is common to both receiver and transmitter can usually be left unshielded entirely.

If this portion must still be several feet in length and *must* also be within a few feet of some unavoidable noise source, such as parallel wiring, it is possible to shield this portion, meeting the requirements of spark-over insulation, and low capacity, without great difficulty.

(*b*). Portions of the lead-in between the antenna switching relay and the receiver will require shielding only if they are in very close proximity to noise-field sources.

The shorter these portions are made the more nearly is the ideal case approached in which even those noise-influence fields in the same compartment with the receiver and lead-in are of no concern, as the coupling factor becomes negligible.

(*c*). The placement of the receiver chassis close to the antenna lead-through insulator reduces the radio-frequency impedance of the structure return of the antenna circuit of the receiver. This is exceptionally important as radio-influence current (RIC) enters the receiver through various external circuits, and while these currents may not affect the receiver directly, they flow from the receiver chassis ground to structure through this structure return impedance. This circuit is seen to be in series with the antenna circuit of the receiver and thus noise voltages will be coupled conductively into the antenna circuit.

The focus of attention has now been directed to the importance of equipment placement in order to reduce the area within which protective measures need be applied. When it has become evident to the designer that the total problem becomes much simplified by keeping RIV sources away from the lead-ins, or vice versa, and steps are taken early in the design stage to achieve this, the installation becomes so simple that visual inspection together with some experience will be all that is required to lay out the radio system with a good expectation of success. It is prudent in all cases to allow for a certain amount of auxiliary shielding (in critical or not easily appraised circumstances) that can be dispensed with if preliminary tests show this feasible.

V. Functional Reasons for Bonding

The reasons for bonding all-metal aircraft can be summed up as follows:

1. To obtain a low-resistance current path for electrical equipment utilizing the airplane structure as a return circuit. Bonding connectors used as a current return must meet the same requirements as to conductivity, contact resistance, impedance, and current-carrying capacity as is required of the circuit with which they are associated.

In this connection it should be noted that it is impossible to rivet and bolt together the metal parts making up the airplane without obtaining inherently excellent electrical conductivity. The ohmic resistance of a *DC*-3 airplane measured from nose to tip of tail was found to be 0.0002 ohm. From this it can be assumed safely that, except for the very largest conductors used, once a good connection is made to primary structure no consideration need be given the resistance of the structure itself between any two points on the airplane. However, in the event connection is made to secondary structure, ample consideration must be given to the resistance between this secondary structure and the primary structure of the airplane. This is of special importance in specific cases such as in bonding radio receivers or in the calculation of generator negative-lead resistance. The fallacy of adding an equal resistance to the conductor resistance for calculation of total circuit drops is indicated. This has been borne out by tests on both old and new *DC*-3 airplanes with regard to the determination of all voltage drops along the starter circuit. It was found that the ohmic resistance of the copper conductor was far greater than that of the structure in every case.

2. To minimize lightning damage. Present literature on lightning strokes to aircraft reveals that apparently the aircraft is never required to carry more than a thousand ampere-seconds during a single stroke. The best approximation obtained from data taken from direct lightning damage to aircraft indicates that if major external surfaces of the airplane are so bonded together that 1,000 amperes can be conducted between any two points for one second, ample protection against damage is assured.

The chance that the airplane will ever carry a cloud-to-ground stroke is remote. The strokes are usually cloud to cloud and are triggered by the airplane flying into an area of intense electrostatic stress.

The installation of bonding connectors capable of carrying 1,000 amperes for one second, across aileron hinges, flap hinges, and large control-surface hinges apparently offers adequate protection against welding of these hinges by reason of lightning current passing through these points. Such bonding connectors must be very short in order to possess the least possible radio-frequency impedance. Bonding connectors may be omitted from small surfaces, such as trim tabs, unless they become of larger size than they are at present. A $^{18}/_{64}$-inch copper braid has been found capable of meeting the required rating.

3. To insure the effectiveness of shielding, the d-c ohmic resistance to structure for any shield is of little consequence compared to the radio-frequency impedance to structure. The radio-frequency impedance of a bonding connector may be several thousand times the d-c resistance per unit length. Hence, in bonding such shields, emphasis is placed on obtaining the shortest possible bonding connector and not on obtaining any arbitrary value of d-c resistance to structure.

As this shield must be effective in screening both the electric and magnetic fields, it is necessary that the shield form a completed circuit. This usually calls for bonding to structure at both ends of the shield or conduit, as the case may be.

4. To provide a means for grounding the airplane when it lands. Upon landing, an aircraft must discharge to ground whatever electric charge has been assumed by the aircraft during flight. The use of conducting rubber in the landing-wheel tires will serve to conduct this charge to ground provided the runway surface is made of partially conducting material. The use of grounding wire from the gasoline truck to the airplane is recommended as a safety precaution at

all times to equalize any potential difference between them.

VI. Filtering

Correctly applied shielding and bonding will result in the elimination of induction coupling between RIV sources and receiver-antenna circuits including the lead-in conductors. Such isolation may not account for all the RIV suppression necessary, as conduction coupling may occur by means of direct transmission through the conductors interconnecting the radio equipment with the RIV source. When such noise coupling occurs to a degree causing radio noise, the insertion of filter units may become necessary.

A radio-influence filter is defined as a device incorporating shunt and series circuit elements so connected as to allow the passage of desired currents through the filter and to prevent the flow of undesired noise-influence currents. Such a filter is no different in principle than a power-supply filter, which allows the passage of direct current and prevents the flow of alternating current to the load, except that the radio-noise filter must discriminate against a much wider band of frequencies.

It is a principal contention that filters should be used only where they perform a specific function with respect to specific equipment. Filters of this type are easily designed to be mounted integrally with radio equipment, as they are usually very small because of the low current-carrying requirements.

Properly designed receiving equipment will have filtering incorporated such that proper operation will result when connected to the aircraft electrical system without further attention to this matter. When this is not the case, a separately packaged filter unit must be applied at some point between the receiving equipment and RIV source. Experience with aircraft-radio-noise problems has shown that filter design together with its proper incorporation into the circuit is a highly technical matter, dictated by the specific installation, and cannot be handled properly on a general basis. The same considerations make it extremely difficult to stipulate a meaningful limit of RIV allowable as either an electrical-equipment specification or as a limit to be applied to aircraft electrical circuits.

The design of a radio-noise filter is very critical, as brute force lumping of capacitors and inductors will not necessarily result in the desired characteristics.[18] A simple filter consisting of a series inductor and shunt capacitor designed to cut off at ten kilocycles will be practically useless at ten megacycles, as the distributed capacity of the inductor will make it resonant at some frequency below ten megacycles so that the inductor will actually be capacitative above the resonant frequency. The capacitor resonates with its inherent series inductance at a certain frequency and becomes inductive above this frequency, thus losing its value as a shunt reactance. A filter unit to provide a given attenuation over a frequency range of 200 kilocycles to 50 megacycles would hence require several stages, each stage being designed for a certain band. Fortunately, the noise-influence spectrum of almost all aircraft electrical equipment, *with the exception of the ignition system*, shows a great diminution of intensity with increased frequency so that at two megacycles very little radio-noise energy is left. This fortunately simplifies the filter-design problem with respect to the high-frequency end of the band. The series element must possess a large amount of inductive reactance in order to provide the desired attenuation at low frequencies as the RIV increases tremendously at the low frequencies as is to be noted in Figure 1. This does not become too troublesome from the weight standpoint for filters required to pass less than 50 amperes. If the filter is used as a source filter for heavy-current electrical equipment, the series element not only must carry the full-load current of the device but must meet a maximum-voltage-drop requirement (0.2 volt) and thus becomes very heavy. The current rating of aircraft generators has reached several hundred amperes and is still rising. The weight cost of any filters used must be multiplied by a factor which depends upon the amount of structure chargeable to the incorporation of one pound of equipment and the weight of gasoline required to carry it. This factor may increase the weight cost of a filter several times.

The RIV generated by thyratron rectifiers as used in some types of voltage regulators ranges up to 4,000,000 microvolts (four volts). This value is greatly in excess of that normally found in the worst electrical equipment. The design of a source filter for such a circuit presents almost insurmountable problems because of the limitation placed on capacitor size in the rectifier output circuit. The resulting required inductor size to attain even a moderate decrease in RIV is prohibitive for aircraft. In one case of the use of such a voltage regulator on a 400-cycle power system, this extreme RIV source was found to cause no trouble in the completed installation. This was due to the attenuation of the RIV resulting from the filtering action of the supply conductors, battery charger, and connected battery. No RIV of a disturbing level reached the communication receiver. This receiver was later found to be productive of radio noise with less than 500 microvolts on its supply leads, hence the original RIV (four volts) must have undergone roughly an 8,000 to 1 voltage diminution or 78 decibels in power.

Dynamotors are of low current rating in comparison to generators or other heavy-current equipment, and hence this filter is not representative of the larger ones that would be required for such equipment. Each filter section contains at least one series element and two shunt elements with their attendant connections, yielding several extra items capable of giving trouble. The addition of each filter represents an extra hazard to electrical equipment resulting from the possibility of short or open circuits.

The allowable temperature rise of some equipment is so high that no capacitor can be placed in direct contact with the motor frame without the risk of breakdown and resultant short-circuiting of the motor. This means that the economy of space attained by mounting the filter integrally with the motor or device base is lost, and the filter must be mounted separately.

The necessity for mounting the filter assembly separately arises also because of an ultrahigh premium on space in the vicinity of engine-mounted equipment and in other congested locations. Only in certain cases can source filters be incorporated neatly in the equipment itself. The space occupied by certain externally mounted dynamotor filters is greater than that of the dynamotor.

Experience has shown that considerable conservation of space and reduction of weight of filters consisting of coils and capacitors may be realized by the proper use of simple noninductively wound capacitors. For example, a certain filter weighing two pounds and occupying a space of 36 cubic inches may have an average attenuation of 50 decibels over the frequency range of 0.2 to 20 megacycles. A simple capacitor may have an average attenuation of 30 decibels over the same frequency range, will occupy a space of only eight cubic inches, and will weigh only a few ounces.

If use is made of capacitors as RIV filters, considerable judgment must be exercised in their electrical location. Improper electrical location of capacitors will result in an increase rather than a decrease in coupling to a RIV source. This arises from the fact that connection of a capacitor to conductors carrying RIC will increase the flow of RIC between the capacitor and the source. If these conductors are in the vicinity of the antenna lead-in or other conductors that can couple RIV to the receiver, the increased flow of RIC may result in an increase of receiver-noise output.

Since any capacitor will resonate at a certain frequency and will become an inductive reactance above this frequency, the length of the connecting leads to the capacitor should be held to an absolute minimum to extend its effectiveness to as high a frequency as possible.

It must be noted that some sources of RIV cannot be adequately filtered by means of simple capacitors. A magneto grounding conductor, for example, may not be shunted by a capacitor without affecting the operation of the magneto.

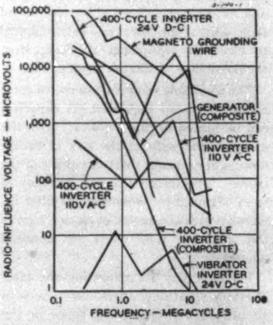

Figure 1. Typical frequency spectrum of RIV sources

A capacitor of this type is employed advantageously at the terminals of the booster coil.

A summary of major points concerning filter application follows:

1. Filtering should be designed as an integral part of all receiving equipment or other equipment being affected by RIV on supply and control conductors to such equipment. This avoids forcing the use of much larger, heavier, and more numerous filters elsewhere in the electrical system.

2. Where RIV induction coupling occurs to the receiver antenna circuit, isolation methods employing shielding of electrical circuits or of the exposed antenna circuit should be employed in preference to filtering of the electrical system or electrical equipment. Once such isolation is achieved it will be equally effective with future changes of electrical equipment provided such shielding has not been violated inadvertently.

3. Dependence on system or source filtering means each source must be equipped with a filter to reduce its RIV to a given limiting value. It has to date been impossible to ascribe a limiting value that is meaningful to a practical extent because of variations from one aircraft electrical installation to another.

The enforcement of such an arbitrary limit would be troublesome because of difficulties of measurement. This point is covered more fully under "Radio Measurements."

4. Simple capacitors may be used as RIV filters where such use will not result in malfunctioning of the equipment being filtered or cause other difficulties attributable to the increase of RIC caused by the admittance of the capacitor.

VII. Radio-Noise-Influence Producing Equipment

Ignition circuits, motors, inverters, generators, and their associated voltage regulators are the main body of equipment items which constitute the sources of RIV and RIC with which we are concerned. These items usually produce also a noise-influence magnetic field surrounding the equipment item itself. This field diminishes rapidly with distance and usually constitutes a hazard only when placed close to exposed receiver lead-in conductors. The noise-influence electric field is notably absent near these devices provided their cases are suitably grounded to structure. The use of an electric field probe to measure the possible radio-noise-influence effect of such an item is not practical, as the probe antenna does not simulate the loop configuration of a receiver lead-in conductor.

1. Audio-Frequency Components

It is generally found that the random-noise energy produced by these devices has its highest energy content at audio frequencies (the actual spectrum varies depending upon the function of the device) with rapidly decreasing energy at the harmonic frequencies extending into the radio spectrum. The high audio amplitude of commutator ripple requires an audio-noise decoupling filter in the microphone polarizing current circuit of the interphone amplifier. The background noise noted in other audio circuits, such as the circuits of radio receivers, also contains this ripple. The gain of these amplifiers is usually so low that this does not cause a bothersome noise level.

The shunting effect of the aircraft storage battery is of such magnitude in maintaining a low audio background noise level that when a battery cart is substituted for the airplane battery a considerable increase in audio background noise may be noted. This is due to the additional d-c resistance of the leads to the battery cart, acting to reduce the normal shunting effect of the battery. Consequently, any noise test made with the battery cart connected or auxiliary battery supply line connected in any manner differing from the normal system installation, should take this effect into account. The battery also acts as a filter capacitor at low radio frequencies, and the effect of extra battery cart lead length is much greater than at audio frequencies. This effect increases with frequency as the radio-frequency impedance of the leads becomes a larger factor than the d-c resistance.

2. Radio-Frequency Components

The radio-frequency spectrum of concern in this analysis begins at 180 kilocycles and ends at 30 megacycles, except where noted otherwise, for instance ignition circuits. The RIV produced by a 400-cycle instrument inverter is very low at 180 kilocycles in comparison with the audio-noise frequencies. The RIV is, however, high at 180 kilocycles compared with its value at 15 megacycles, as noted in Figure 1. The audio-frequency noise-influence voltage is of a magnitude of several volts, while the RIV at 180 kilocycles is rarely above 100,000 microvolts (0.1 volt) at the terminals of the dynamotor.

Representative curves, Figure 1, of RIV as a function of frequency have the following characteristics in common:

(*a*). Highest output at the lowest frequency.

(*b*). Output drops as frequency increases but may go through several resonant peaks before finally dropping to insignificant values.

(*c*). Output drops to negligible value beyond 15 megacycles.

Note that these observations also hold for measurements made on ignition grounding circuits in spite of the fact that secondary impulse currents are still of considerable amplitude at 500 megacycles. This apparently is due to the fact that little or no coupling exists between the ignition secondary circuit and the magneto primary circuit at these frequencies. Ultrahigh frequency coupling may still by-pass the magneto through other paths. The secondary ignition circuit of an airplane engine is a prolific source of radio-noise-influence fields which couple directly to the aircraft antennas. These fields are of considerable strength

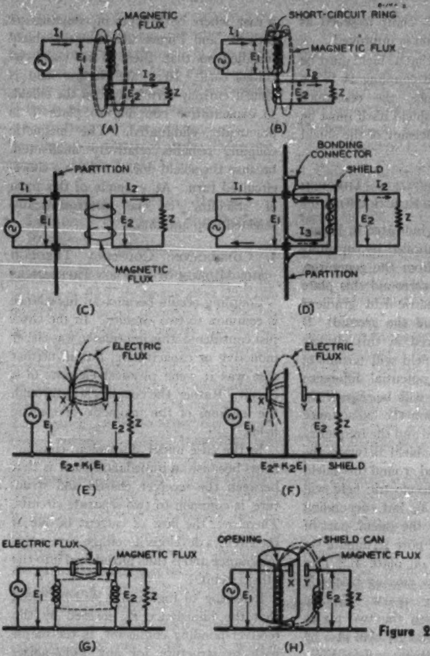
MAGNETIC FLUX
I_1
E_1
I_2
E_2
Z
(A)
SHORT-CIRCUIT RING
MAGNETIC FLUX
(B)
PARTITION
MAGNETIC FLUX
(C)
BONDING CONNECTOR
SHIELD
I_3
PARTITION
(D)
ELECTRIC FLUX
X
Y
$E_2 = K_1E_1$
(E)
ELECTRIC FLUX
$E_2 = K_2E_1$
SHIELD
(F)
ELECTRIC FLUX
MAGNETIC FLUX
(G)
OPENING
SHIELD CAN
MAGNETIC FLUX
(H)

Figure 2 (left). RIV coupling diagrams

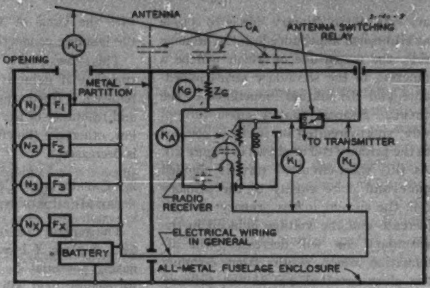
ANTENNA
C_A
ANTENNA SWITCHING RELAY
OPENING
K_L
METAL PARTITION
K_G
Z_G
N_1
F_1
N_2
F_2
N_3
F_3
N_X
F_X
K_A
TO TRANSMITTER
RADIO RECEIVER
ELECTRICAL WIRING IN GENERAL
BATTERY
ALL-METAL FUSELAGE ENCLOSURE

Figure 3. Aircraft radio-noise-coupling diagram

up to 500 megacycles. The problem of shielding this circuit in order to eliminate this radiation is one requiring detailed consideration. This subject will be considered further in a subsequent writing.

VIII. Fundamentals of Noise Coupling Circuits

The transference or transmission of electric energy involves coupling between two or more circuits. This is accomplished through the medium of impedances common to the coupled circuits. The common impedance may consist of resistance, capacitance, inductance, or any combination of these.

It is necessary to distinguish between induction and conduction coupling. Induction coupling takes place through the medium of the magnetic and electric fields and is referred to as inductive and capacitative coupling, respectively. Conduction coupling takes place where a noise-influence current flows through an impedance such as a bonding connector which happens to be common with another circuit, such as the antenna input circuit of a receiver. Such coupling occurs between RIV sources and receivers through the conductors connecting them and thus is not affected by shielding of these conductors.

In the circuits with which we are concerned, there is often no clear definition as to the type of coupling which is present, and frequently all are present at the same time. For analysis let us investigate each type separately. Before entering into the analysis, the question of terminology must be considered to avoid confusion.

In the discussion to follow the multiplier K is used to denote a coupling ratio.* For example, if a voltage E_1 is impressed on the primary terminals of a transformer, then the transformer secondary voltage will be: E_2-KE_1.

K, then, may be either larger or smaller than unity in the case of a transformer and in any event will be a complicated expression. The voltage appearing at the output terminals of a network may be obtained by multiplying the voltage impressed on the input terminals by K.

Assume a certain electrical network has a coupling ratio K_1, and in some manner the network is changed so that the coupling ratio is now K_2. Let us say further that this change is such that K_2 is smaller than K_1. The attenuation attributable to the change in the electrical network may then be expressed as: $A=\text{Attenuation}=K_1/K_2$. This may be expressed either as a simple numerical ratio or in decibels, in which case $DB=10 \log K_1/K_2$.

The "coupling ratio" is also conveniently used to express the relationship of two different quantities, in which case their measurement units and conditions of measurement should be clearly indicated. For example: the coupling ratio between the liaison antenna and the ignition system is 98 at 1.0 megacycles. The ratio 98 expresses the microvolts per meter measured with a Ferris Model 32*A* radio noise meter and 41-inch probe antenna at a test location three feet forward of the propeller hub, to the voltage measured with the same meter at the antenna with the antenna terminated only in the radio noise meter.

The convenience of the use of such a factor lies in the elimination of the need for the inclusion of many academic factors such as the effective height of the antenna, field pattern considerations, and so forth, which are difficult of determination. Such factors mean little when the over-all effect is all that is needed to evaluate a particular case.

1. Inductive Coupling Through the Medium of the Magnetic Field

Referring to Figure 2A, a generator impresses a voltage E_1 upon the primary side of a coupled circuit. The magnitude of the voltage E_2 induced in the secondary side of this circuit will depend upon the mutual inductance of the coupled circuits and the current in the primary circuit. The effect of coupling the secondary side to the primary side is to couple an additional impedance into the primary side, the magnitude of which will be a function of the secondary series impedance and the mutual inductance of the circuit. Suppose that a single turn of wire, short-circuited on itself, is coupled to the primary coil as shown in Figure 2B. As this short-circuited turn is coupled more and more closely to the primary coil, the current in the primary coil will increase and the voltage induced in the secondary coil will decrease. The attenuation which results from coupling the short-circuited coil to the primary coil may then be written as $A = K_1/K_2$.

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* This use of K should not be confused with the symbol K as commonly used to denote the coefficient of coupling between two circuits. For instance: $K=M/L_1L_2$ where M = the mutual inductance between the two circuits, and L_1 and L_2 are the inductances of the two circuits, respectively.

The case with which we have been dealing is very common in coupled circuits, and the coupling ratio is often much larger than unity. However, in radio-noise work we are mostly concerned with instances where the coupling ratio is very small, as betwen two conductors at some distance from each other.

Referring to Figure 2C, a generator is feeding a wire which passes through a shield, for example, a wire passing through a metal partition in an airplane. Coupling exists between this primary wire and another wire some distance away, and a voltage is induced in this secondary conductor. Precisely the same relations hold as in the previous discussion. $E_2 = K_1E_1$. Since the coupling between the two wires is of low order, K_1 will be much less than unity, and E_2 will be less than E_1.

Referring to Figure 2D, a metallic shield has been installed about the primary wire. At one end this shield is arranged to have good contact with the metal partition; at the other end the shield is connected to the partition by a bonding connector. It will be seen from this that there is a short-circuited turn again coupled to the primary circuit as in the first example. Consequently, a current I_2 will flow through the shielding and the partition comprising the closed turn, and there will be very little coupling to the original secondary wire. However, if the bonding connector is of high impedance, or poor contact is made, the closed turn will be incomplete, and there will be less coupling to the closed turn. As this process is continued until the closed turn becomes completely open, there will be less and less coupling to this turn, until finally the coupling is zero. When this occurs, the coupling to the secondary wire is again unaffected, and the shield about the primary wire might as well not be present, as far as magnetic field coupling is concerned.

In any event, the coupling ratios K_1 and K_2 vary as in the previous example, with the value of K_2 being lessened as the bonding connection is improved.

This shows clearly that a shield must be complete if inductive coupling is to be reduced. Furthermore, the resistance and reactance of the shield itself must be low, otherwise the efficiency of the shield is decreased.

2. Capacitive Coupling Through the Medium of the Electric Field

The general case is indicated in Figure 2E. A plate X is supplied by an alternating potential E_1 from the generator. An electric field will surround this plate and produce an electric-field gradient between the plate and the ground. If another plate is placed in this electric field, a part of this field will terminate on plate Y, and the potential difference between X and Y will be $E_2 = K_1E_1$, where K_1 depends upon the capacitance between the two plates and the frequency of E_1. Now erect a shield between the two plates X and Y and ground this shield as in Figure 2F. The electric field will still surround plate X, but, depending upon the geometry of the shield, part of the electric field will now terminate on the shield instead of on plate Y. This has the same effect as moving plates X and Y some distance apart. K_2, the coupling factor between the two plates will become smaller, and E_2' will also be smaller than E_2, hence: $E_2' = K_2E_1$. Again, we may express the attenuation attributable to changed conditions, that is, the introduction of the shield, by the relation $A = K_1/K_2$ or attenuation in decibels $= 10 \log K_1/K_2$.

3. Coupling Through the Medium of Both the Magnetic and Electric Fields Simultaneously

Unfortunately, coupling is seldom entirely either inductive or capacitative. Usually, there is a combination of the two, in varying degree. It is interesting to note at this point that the frequency range involved in radio-noise measurements is so great that a circuit such as is diagrammed in Figure 2H will show a very large change in the value of K with frequency. In capacitative coupling, the maximum power transfer is a linear function of the frequency; in inductive coupling it is a function of the frequency cubed.[10] Therefore, as the frequency is increased, the effect of inductive coupling (magnetic field) becomes of far greater importance than capacitative coupling (electric field). Often, a certain type of shield may reduce both types of coupling, but in many cases a shield will serve only to reduce one type. Figure 2G illustrates a case where both types of coupling are present, and Figure 2H shows a shield installed so that there is no short-circuited turn. Here the electric field is almost entirely terminated on the shield, so capacitative coupling to plate Y is practically eliminated. The magnetic coupling remains relatively unaffected, because the shield does not form a short-circuited turn. An example of this form of shielding is the electrostatically shielded loop antenna.

4. Conduction Coupling Through the Medium of Common Impedances

Coupling occurs because an impedance is common to two circuits. In the cases just considered, this impedance was either inductive or capacitative, but in neither case was it some physical element of a circuit. Rather, the coupling was through the medium of the magnetic or electric fields.

In the case under discussion, coupling exists because an impedance, such as that between the receiver chassis and structure, is common to two separate circuits. Therefore, the flow of current in one of the circuits develops a voltage across this impedance and is thus introduced into the other circuit.

Referring to Figure 3 it will be noted that the impedance Z_g, formed by the receiver-bonding connector, is the major path to structure for any radio-noise-influence currents which enter the receiver by any path whatsoever. Signal currents entering the receiver via the antenna also return to structure through this path. This path being directly in series with receiver-antenna input circuit, any voltage developed across this path will be as effective in producing receiver audio output as an equal signal voltage developed in the antenna itself.

In addition to the common impedance formed by the receiver bonding connector, there are other coupling paths internal to the receiver. Among these are the impedances formed by the cathode to grid capacitance and the impedance of the cathode to ground.

Noise-influence currents that enter the receiver may divide and flow through the cathode-to-chassis or cathode-to-grid impedance before returning to structure. The RIV drops across each of these common impedances are amplified as they pass through the balance of the receiver to the audio output.

All of the radio-noise-influence currents that enter a receiver must return to structure through the impedance of the bonding connector. For this reason, when a RIV filter is used, it should be a choke-input rather than a capacitor-input configuration, because the current through the shunt element of the latter type will be greater, thus resulting in a greater RIV drop across the impedance of the receiver bonding connector.

Evaluation of the various common impedances is discussed under "Measurements of Radio Noise."

IX. Coupling of Radio-Noise-Influence Sources to the Radio Receiver

Many sources producing extreme amplitudes of RIV are so installed that this influence is never observed at the receiver. It must be clearly understood that a RIV source will be only of practical concern when it is coupled in some manner to one or more circuits of the radio receiver to a degree that the noise or other disturbance level occurring in the receiver output is greater than that permissible. Such coupling may occur to the antenna lead-in conductor of the receiver, to internal elements of the receiver (as by coupling between the cathode and the grid of a tube, where a difference of potential attributable to RIV modulates the cathode), and by means of RIC flowing through a common ground impedance. Well-designed radio receivers employ filter elements in all leads entering the receiver which are found to be critical with regard to RIV coupling to the low-level stages in the receiver. It is unfortunate that many aircraft receivers to date have not been given proper attention in this respect, thus complicating the aircraft-radio-noise problem tremendously and requiring the filtering job to be completed by the installer of the receiver. This has led to much unscientific use of filters in an attempt to find a panacea for all radio-noise ailments.

Radio noise output resulting from the induction coupling of wiring carrying RIV to unshielded lead-in conductors is a function of the following factors:

1. Amplitude of the radio-noise-influence voltage and current (RIV and RIC, respectively) on the offending wiring.
2. Degree of inductive coupling. RIV is the major factor in capacitative coupling, and RIC is the major factor in magnetic coupling.
3. Degree of conduction coupling.

Induction coupling exists to a troublesome degree only where the unshielded lead-in is in close physical proximity to unshielded wiring and is of no concern with respect to wiring in other parts of the airplane. In the region where coupling is of concern the degree of coupling is roughly proportional to the lengths of the lead-in and wiring exposed to mutual coupling, and to the amplitude of the RIV or RIC of the wiring in this region and is inversely proportional to varying powers of the distance between the lead-in and wiring.

A few general notes:

1. Most electrical equipment possessing commutating contacts or other current-switching elements produce RIV at their terminals which varies over large ranges of amplitude from one item to the next.
2. It is highly desirable that the design of electrical equipment be directed to the maximum possible reduction of such inherent radio influence.
3. Filtering elements can be applied to equipment to reduce the inherent RIV, but such procedure is wasteful unless it is determined first that the desired effect cannot be attained more economically elsewhere. In the usual case there are many more sources of RIV than there are units of equipment (receivers, and so forth) being affected. Source filtering is hence to be avoided where possible.
4. The cases in which correction *must* occur at the source are those in which the natural shielding of the all-metal structure is not effective. Such cases are represented by the engine ignition and electrically operated propellers of either all-metal or nonmetal airplanes. In nonmetal airplanes, the need for source filtering on or shielding of wiring obviously increases greatly.

X. Measurements of Radio-Noise-Influence Voltage, Current, and Fields

A considerable amount of time and effort has been expended in the last few years in the field of radio-noise measurements the largest portion of which has been done in the broadcast field.[11–16]

The term RIV has been used generically to represent voltage, current, or power. This generalization has obscured the fact that many coupling problems are functional with current rather than voltage. Magnetic field coupling is of this nature, as power transferred is a function of current, mutual inductance, and frequency cubed. Electric induction is a function of voltage, capacity between circuits, and frequency to the first power;[10] hence, it is seen that as frequency increases the coupling of radio-frequency power attributed to magnetic coupling (current loop) becomes definitely predominant as the frequency term is to the third power. It is thus desirable that the term radio-influence current (RIC) be used where measurements or effects functional with current are concerned.

Much progress has been made in the measurement of the absolute values of RIV of electrical equipment and circuits, but there has been a dearth of information concerning the evaluation of RIV in terms of actual receiver-noise output. An immense amount of aircraft-radio-noise research involving measurements of quantities having no relation to actual radio noise output has resulted from a lack of cognizance of the importance of the determination of such coupling factors.

A comparison of the broadcast-radio-noise problem to that of aircraft follows in order to note certain important differences in these two fields.

In aircraft, the placement of electrical and radio equipment, to take advantage of natural factors tending to reduce noise coupling from point to point, is under the control of the designer to a much greater degree than it is in the case of industrial-noise interference to broadcast listeners, where little such control can be exercised. The principle of filtering at the source applies very well in the broadcast field, and the application of noise filters to noise-influence producing equipment has gained well-earned headway.

This principle, however, does not apply to the airplane because the desired controls can be exercised to a very considerable degree.

The home broadcast receiver is coupled to RIV sources predominantly through the medium of its antenna and unshielded lead-in. Suppression of noise influence hence must occur at the source as there is no other way of decoupling the antenna from the source, as the antenna and the noise-influence source are both coupled to the identical free space.

In aircraft, the engine ignition system and electrically operated propellers are also in this category, and it is found that noise suppression must occur at the source by the employment of well-designed shielding.

All other electrical RIV sources and wiring are inherently shielded from the antennas by the all-metal fuselage. Internal lead-in conductors can be shielded against induction coupling to wiring, where necessary; hence, the problem is reduced to that of eliminating conduction coupling to the receiver. This is a filtering problem which is most easily solved by proper filtering at the receiver. Any attempt to attack this problem by source filtering leads to complications involving weight and specialized design of equipment to accommodate the filter.

In aircraft, radio-noise measurements must be made with the most compact equipment possible, while in other fields a truck full of equipment is no handicap.

The aforementioned points should be kept in mind as the measurement of radio noise in aircraft is being considered.

1. Radio-Frequency Measurements—General

The complete determination of all coupling factors involved in the coupling of RIV to receivers involves both a quantitative and qualitative analysis.

Quantitative analyses are those in which absolute amplitudes of RIV or RIC are measured at the terminals of electrical equipment or at various points in the electrical circuit, or in which absolute amplitudes of receiver-noise output are measured. From these measurements over-all coupling ratios can be determined without the necessity for determining intermediate coupling ratios from point to point. The coupling of a single source to the receiver usually involves many transformations of noise energy from one medium to another, each transformation experiencing a certain degree of attenuation or amplification as the case may be. The complete evaluation of these changes from point to point becomes extremely laborious if not impossible in all but the simplest cases. Fortunately, this is usually quite unnecessary. Intermediate coupling ratios may be of great academic value to the research engineer wishing to determine where and how coupling occurs from point to point but may be of no value to the engineer wishing to obtain only a performance test of the complete installation.

Qualitative analyses involve the determination of where and by what medium (induction or conduction) radio-noise coupling occurs. A qualitative analysis hence seeks to determine the conditions which cause coupling and to discover means for eliminating such coupling. Such measurements can be made from point to point with little consideration of absolute amplitudes of voltage, current, or field strength as the over-all (performance) quantitative measurement can be depended upon to determine the success of the methods employed. The academic usefulness of the qualitative analysis is in determining engineering-design practices which can be utilized by the designer and in trouble shooting when an unexpected case of radio-noise trouble occurs in the completed airplane.

2. Instruments for Measuring Radio Noise

The first measurements of radio noise were made by adapting an audio-power-level meter to measurement of the radio noise output of a conventional radio receiver. This was done by incorporating a time-delay response so that the meter would be quasi-peak reading in order to give a reading more nearly on the basis of the nuisance value, or masking value, of the noise voltage. It was soon found that measurements taken with such a meter connected to the output of a conventional radio receiver showed extreme variations when used with different receivers and that RIV measurements could be duplicated only if a standardized radio-frequency channel were provided. With the development of a standardized radio-frequency noise meter, radio-noise indications on the basis of nuisance value could be easily accomplished. This type of meter as exemplified by the Ferris Model 32*B* or *RCA* 312*A*, is suitable for the measurement of absolute amplitudes of RIV from point to point of the electrical system and to measure field strength of the electric component of radio-frequency and noise fields. It is not suitable to measure radio-frequency currents. The loop probe can be used to determine relative magnetic field strengths but will not indicate absolute values without specific calibration. The unit measured is the modulated microvolt in the case of noise-influence voltage measurements, and the modulated microvolt per meter, in the case of noise-influence field measurements. The following difficulties arise when this type of meter is used for aircraft-radio-noise measurements:

(*a*). Quantitative measurements made on the receiver input (antenna) circuit involve connecting the meter within a foot or two of the point to be measured. This is impossible in smaller airplanes during flight because of inaccessibility. Any attempt to use the meter remotely meets with trouble attributable to unsatisfactory grounding, noise pickup on the extended lead, and switching complications. The problems of switching such a meter from point to point during flight become very complicated and add a definite hazard, as the pilot must be able to regain control of the radio equipment at a moment's notice.

(*b*). Presently available equipment of this type is not rugged enough to stand continued use in aircraft (without excessive time being spent in adjustments and recalibration) and must be confined to the laboratory.

(*c*). The technique of measurement becomes so critical at two megacycles and above that none but those experienced in the technique of radio-frequency measurements can hope to obtain valid and consistent results.

(*d*). Battery packs required to operate these meters in aircraft bring the total weight to 45 pounds.

(*e*). The cost of the meter is high ($400). and its availability at present is practically nil.

To these difficulties is added the fact that all measurements made with a radio noise meter are only of academic value until they are evaluated in terms of receiver noise output. This type of meter hence has a high academic value in the laboratory and for field-strength measurements but a low practical value for production or prototype airplane-radio-performance tests.

Most of the difficulties arising from the use of a radio noise meter may be overcome by the use of an audio noise meter, in conjunction with the aircraft receiver under concern. The physical advantages of an audio noise meter may be noted as:

(*a*). Extreme simplicity of operation, and therefore suitable for production testing.

(*b*). No switching problem.

(*c*). Can be used as remotely as required.

(*d*). Can be used on the output of each receiver to obtain further simplicity of switching if desired.

(*e*). Portable and light in weight (eight pounds).

(*f*). Rugged.

(*g*). Inexpensive.

(*h*). Can be constructed from easily obtained materials.

(*i*). Reads directly the noise heard in the ear phones. No evaluation of coupling factors, receiver characteristics, and so forth, necessary.

(*j*). If the receiver is calibrated, the audio measurements can be evaluated in terms of radio-frequency noise-voltage input to the receiver-antenna terminal.

Such use of an audio noise meter is covered in detail under "Audio-Frequency Measurements." In the following discussion regarding radio-frequency measurements it must be noted that an audio noise meter or a pair of headphones with any aircraft-communication receiver will serve in qualitative determinations. If the receiver is calibrated in terms of audio output as a function of sine wave, 30 per cent modulated radio-frequency input, the combination will also serve for quantitative measurements when used with the receiver connected to its regular (aircraft) antenna. Such calibration will, of course, correspond only roughly to a calibration made into a noise source. The difference is, at the moment, of no great concern. The receiver used in this manner gives the truest possible analysis of the effect of radio noise on its output and also gives one more reason for testing the installation with the receiver to be used.

3. Radio-Frequency Measurement Procedure

(*a*). *Electric Fields; Use of Electrostatic Probe.* Where absolute determinations of field strengths are to be made, it is essential that the point of measurement be at a distance greater than twice the wave length from the source of the field in order that localized field configurations (attributable to the rapidly varying intensity of the induction component as a function of distance) do not confuse the measurement. Absolute field-strength measurements inside an airplane are possible but meaningless, since the requisite distance from the field source is not obtainable and the airplane structure distorts the fields so that a highly irregular pattern is obtained.

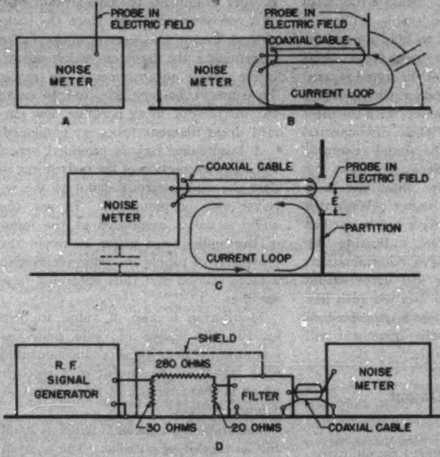

Figure 4. Circuit arrangements for measurement of field strength and filter attenuation

A major use of the radio noise meter is in the location and survey of electric fields, in which case the electrostatic probe is used. See Figures 4A and 4B. The electrostatic probe operates on the principle that a potential difference will exist between the conductor and ground (airplane structure) when the probe is placed in an electric field. If this conductor is connected to a voltmeter, an indication of the intensity, extent, and configuration of the field may be obtained by moving the probe about in the field. Obviously, if the lead connecting the probe to the voltmeter is also in the electric field, the measurements will be obscured by pickup on the lead. Since absolute values are not being measured, this will not invalidate such measurements if given proper attention. If a coaxial line is used between the voltmeter and the probe, such pickup will be eliminated. Where the line is in a noise-influence field there will be a loop formed between the coaxial cable and the ground plane as shown in Figure 4B. This loop will still be evident even though the radio noise meter is not metallically connected to ground, because of the capacity of the meter to ground as shown in Figure 4C. The effect of this loop can be decreased by making a good ground on the radio noise meter and running the coaxial cable in such a way that the loop between the cable and the ground plane is of very small area.

Figure 4C shows a setup which may be used for measuring field strengths. This setup conceivably may be used in the determination of field strengths about an engine. The voltage on the probe will be the potential difference between the probe and structure, and is shown in the figure as *E*. The formation of a loop will also develop a voltage at the end of the coaxial line if there is noise field coupling to the loop. The voltage developed across the coaxial line resulting from loop coupling must be at least ten decibels below the voltage *E* under measurement in order not to confuse the measurement. A test to determine if such pickup occurs must be performed before field measurements are made. Remove the probe from the coaxial cable end, and make the test setup so that the voltage attributable to loop coupling is a minimum.

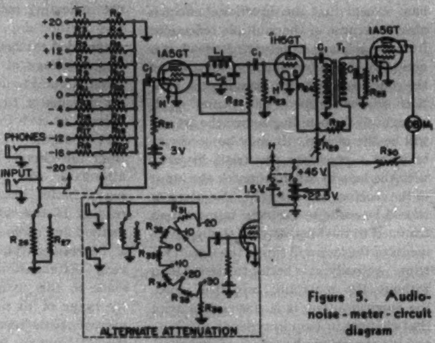

Figure 5. Audio-noise-meter-circuit diagram

R_1—496,855 ohms
R_2—5,145 ohms
R_3—493,860 ohms
R_4—8,140 ohms
R_5—489,150 ohms
R_6—12,850 ohms
R_7—481,700 ohms
R_8—20,300 ohms
R_9—469,900 ohms
R_{10}—32,100 ohms
R_{11}—451,300 ohms
R_{12}—50,700 ohms
R_{13}—421,600 ohms
R_{14}—80,400 ohms
R_{15}—375,200 ohms
R_{16}—126,800 ohms
R_{17}—302,000 ohms
R_{18}—200,000 ohms
R_{19}—186,000 ohms
R_{20}—316,000 ohms

R_{21}—1 megohm
R_{22}—20,000 ohms
R_{23}—30,000 ohms
R_{24}—25,000 ohms
R_{25}—600,000 ohms
R_{26}—600 ohms
R_{27}—4,000 ohms
R_{28}—10,000 ohms
R_{29}—10,000 ohms
R_{30}—10,000 ohms
R_{31}—341,900 ohms
R_{32}—108,109 ohms
R_{33}—34,184 ohms
R_{34}—10,809 ohms
R_{35}—3,418 ohms
R_{36}—1,580 ohms
C_1—0.1 microfarads
C_2—0.002 microfarads
C_3—1.0 microfarads
L_1—2.0-henry choke
M_1—0-1 milliamperes d-c

T_1—Driver transformer, tube to class-*B* grid: primary/$^1/_2$ secondary 2.66:1

It should be noted here that when a probe of this type is used with a radio receiver the input circuit must be maintained in constant trim. As the probe approaches structure, the capacity to structure is increased, upsetting the trim of the receiver and forming a loop. (This difficulty is not so troublesome with a radio noise meter as the input circuit is purposely designed to be less critical with respect to changes in source impedance.) If a coupling source is close to this loop, the receiver-output level will rise. Therefore, when exploring for an electric field, the antenna trimmer should be operated to keep the input circuit in constant trim. One alternate method which will work, when the tested field strength is of considerable intensity, is to throw the antenna trimmer as far *off* trim as possible. This makes the input circuit less sensitive to changes in source (probe) impedance but also reduces the sensitivity greatly.

(*b*). *Magnetic Fields; Use of Loop Probe.* Frequently a magnetic field will be present and the electrostatic probe will fail to give any direct indication of its presence except that this field constantly obscures electric-field measurements. In order to measure the magnetic field directly, the electromagnetic probe may consist of a half-dozen turns of wire, three inches in diameter and may be mounted either on the end of a coaxial cable or connected to the test apparatus by a twisted pair of wires.

It will be found that this probe is used in much the same manner as an ordinary loop, except that the directional effect is absent because of the multiple reflections from the airplane structure. Instead, it is necessary to determine the location of the coupling source by changing the position of the probe. The nearer it is brought to the coupling source the greater will be the pickup. It may be necessary to place a capacitor (by trial) in series with the loop in order to match the input of the receiver more closely. No attempt should be made to keep the input circuit trimmed to the loop, as very slight movements of the loop will throw the circuit off trim. Actually, it is better to use the loop without the matching capacitor, since the receiver input is so far out of trim that changes in loop impedance attributable to the proximity effect of structure have little effect.

(*c*). *Measurement of Filter Attenuation.* It is well known that the attenuation characteristics of any electric filter are dependent upon the impedances of the input and output terminations.[16] In the practical case, it is almost impossible to obtain a meaningful determination of these impedances because the noise source is a very complicated pattern of resonant and antiresonant impedances over the radio-frequency spectrum. The same is true of the impedance looking into either end of the supply line as well as when looking into the receiver at its supply terminals. Formalized filter formulas are of little use in the design of such units, hence most filter design has proceeded on a cut-and-try basis. A partly standardized method of filter measurement is that shown in Figure 4D. The (400-cycle modulated) signal generator is terminated in 30 ohms in order to maintain the accuracy of the output attenuator. The 280-ohm series resistance is used to decouple the generator from the filter. The 20-ohm shunt impedance represents the input termination of the filter. This value is, of course, highly arbitrary, since the radio-frequency impedance of the device to which the filter is connected is highly variable and is extremely difficult to determine, as such determination must be made with the instrument operating into its normal load.

It should be noted that the resistances used for the decoupling network must be noninductive up to the highest test frequency used.

The filter-attenuation data may be obtained by holding constant input to the filter and measuring the output as a function of frequency. This method is unsatisfactory, however, because the range of output amplitudes encountered requires scale multipliers to be used with the indicating meter. Scale errors with this method are considerable. The recommended method is to hold a constant signal voltage at the noise-meter input terminals and to adjust the signal generator attenuator to produce this constant voltage, at each frequency, with the filter first connected and then disconnected from the circuit. The signal generator attenuator can be depended upon for much better accuracy than the scale reading of the noise meter. With this method any radio receiver can be used as a constant-output-level indicating device. Attenuation-curve determinations made in this manner will agree within the range of 10 to 20 decibels with like measurements made when connected to the RIV source, but the agreement will hardly be better than this.

XI. Radio-Noise-Output Measurements

To meet the requirement for an instrument usable in the quantitative analysis of noise components in the audio output of radio receivers and to meet the requirements of the United States Army and Navy for an instrument for use in aircraft-radio-noise acceptance tests, an audio noise meter was developed. The circuit of this instrument is shown in Figure 5 and the frequency response characteristics of the circuit are shown in Figure 6.

This instrument is a vacuum-tube voltmeter reading a quasi-peak value of the noise signal. Only the nuisance value of the noise signal present in the audio output of a receiver is of interest, and this instrument more nearly approximates this value than does an rms reading instrument.[17] The proper response has been accomplished by the use of a resistance-capacitance network proportioned to require ten milliseconds charge time and 600 milliseconds discharge time in accordance with the standard time constant for noise-measuring instruments.[13]

A filter limits the frequency response of the instrument to the frequency band required for the intelligible transmission of speech. (See Figure 6.) This was found necessary to prevent frequencies which contribute nothing to the nuisance value of the audio voltage under measurement from producing meter indication. The meter is calibrated in decibels using a 0-decibel reference level of one milliwatt in 600 ohms. By means of an input attenuator, the full-scale range of the meter is extended from −32 decibels to +28 decibels in steps of four decibels. The −32-decibel sensitivity is necessary in order to indicate the background level of a receiver under test, since the background level of the receiver is one factor determining the basis on which quantitative audio measurements are made. To conserve battery life and to make the instrument more portable, low current drain filament tubes are employed.

A headphone jack is provided across the input connection of the instrument so that the audio output signal of the receiver may be monitored. In use, the audio signal is monitored at the input of the audio noise meter to avoid the possibility of tuning the radio receiver to a radio station and thus obtaining false readings.

Monitoring is also of value to help identify the source of radio noise, as certain equipment items produce an audio noise signal in the output of a receiver which is characteristic of that particular equipment item.

The audio noise meter is made as small and as rugged as possible. The over-all dimensions are 12 by $6^1/_2$ by 5 inches. All batteries are self-contained and have 30 hours capacity.

1. AUDIO-NOISE OUTPUT AS A FUNCTION OF RADIO-NOISE INPUT

The use of the audio noise meter as a means of quantitative evaluation of the nuisance value of radio noise affecting a radio receiver is now to be considered.

A fundamental starting point of this evaluation is the background level of the receiver measured with all noise sources inoperative, the receiver-antenna lead-in disconnected, and the antenna terminal of the receiver grounded to the receiver chassis through a capacitor simulating the capacitance of the antenna. The background-level determination must be made at frequencies throughout the range of the receiver as the background level varies greatly with frequency.

Having determined the receiver background level with no input signal, it is then possible to determine the relative noise output produced by various RIV sources and evaluate measures taken to reduce coupling paths which contribute to receiver-noise output. Coupling sources whose effect is or has been reduced to the receiver background level need not be given further consideration.

In the case of evaluating ignition noise coupled to the antenna, the background level must be measured with the lead-in and antenna normally connected. The limiting background level in this case is the atmospheric noise level, and since the atmospheric noise level is constantly changing, such measurements cannot be taken as a fixed reference level. Thus, if the radio noise, because of ignition or other noise influences on the outside of the airplane, are reduced to the background level at one time, they may be above the background level at another time because of a reduction of the atmospheric noise level.

In order to consider the basis for the determination of permissible radio-noise-output levels, it is necessary to review the calibration curves of a typical aircraft receiver with particular consideration of ratios of signal-to-signal plus noise (hereinafter shortened to "signal-to-noise ratio").

The sensitivity of a receiver in microvolts is defined as the input voltage in microvolts required to produce a 50-milliwatt output with a signal-to-noise ratio of at least six decibels.

Calibration curves of a typical receiver are given in Figure 7. These curves are obtained by feeding a radio-frequency sine wave signal, modulated 30 per cent at 400 cycles, to the receiver from a signal generator connected to the receiver through a standard dummy antenna.

Curve *A* of Figure 7 represents the calibration curve of this receiver with the volume control 50 per cent full open in terms of knob rotation. This curve is similar to the curve obtained with the volume control full open on a receiver having insufficient audio gain to take full advantage of the sensitivity of the radio-frequency channel. This is determined by the fact that the bend of the curve is caused by the background noise of the audio channel instead of the background level of the radio-frequency channel. The background level of the receiver with no signal is seen to be about 0.02 milliwatt. If this is arbitrarily increased six decibels by noise voltages the level will come up to 0.06 milliwatt. This total noise level is very low, and the desired signal can be received with a good margin over the background noise, provided it is of sufficient intensity. A noise voltage of 9.5 microvolts is allowable, but a signal voltage of 200 microvolts will be required to make the output level 50 milliwatts, which is approximately the level required with this receiver for reception in an aircraft during flight. If this signal level is not obtainable the volume control must be advanced to obtain the required output level.

Advancing the volume control brings the operating curve to that of curve *B* of Figure 7. The background level indicated by the flattening of this curve to the left is caused by noise inherent in the first radio-frequency stage or by improperly filtered rectifier noise. This level is seen to be three milliwatts; a six decibels rise in output level brings it to ten milliwatts. A noise voltage of approximately 1.5 microvolts will accomplish this, while a signal voltage of 7.0 microvolts will suffice to raise the output level to the required 50 milliwatts. Therefore, there is still a good margin between signal and noise. In fact, the usable audio gain could even be slightly higher than that shown.

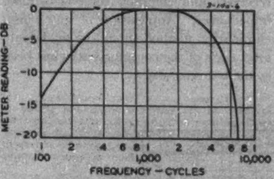

Figure 6. Audio-noise meter frequency response

So far, the effect of atmospheric noise level in limiting reception when the receiver is used in an airplane has not been discussed. The minimum level ranges from 2 to 10 microvolts per meter. Assume that a field strength of two microvolts per meter will place a noise voltage of four microvolts at the input terminals of the receiver, resulting in three milliwatts of noise output. Reception, even under the best conditions, is limited to a radio-frequency signal of at least two microvolts, hence we have one basis from which to determine permissible noise levels from RIV sources. Permissible noise levels for each specific receiver installation are now under investigation.

The effect of raising the audio gain above that required to obtain the full benefit of the sensitivity of the radio-frequency channel is illustrated by curve *C* of Figure 7. Here, the background level with no signal is seen to be 200 milliwatts. A six-decibel increase in background level will increase the output nearly to the receiver saturation point, so that any signal received would be barely intelligible above the noise.

From the operator's point of view, the maximum amount of audio gain, in proportion to the radio gain available, is obtained when the background level is six decibels below 50 milliwatts, or whatever level is required to give a proper margin above acoustic noise (engines, and so forth). This case is illustrated in curve *D* of Figure 7, which represents excess audio gain, indicated in curve *C*, reduced by lowering of the volume control to the precise point at which a six-decibel signal plus noise-to-noise ratio is obtained.

The radio-frequency signal is seen to be 0.6 microvolts, and the sensitivity of such a receiver is 0.6 microvolts. This is about eight decibels better than that required under the best possible atmospheric conditions. Obviously there will be nothing gained in designing a receiver with better than a one-microvolt sensitivity as long as the minimum atmospheric noise will exceed this (at the terminals of the receiver).

In making other radio-noise measurements the above considerations also apply in great measure. It is obviously undesirable to have the ratio between audio and radio gain at such a figure that a small noise voltage will send the receiver up to the audio-overload point. It is equally undesirable to have the background level so low that an inordinate signal voltage is required to drive the output stage fully.

(*a*). *Permissible Audio Noise Level Utilizing the Background Level of the Receiver as a Reference.* This method is a logical development from the previous considerations and is the simplest test that can be made. With the audio noise meter connected to the output of the receiver and switched to the proper termination, 600 or 4,000 ohms, turn the volume control full on and read the background level with the audio noise meter. Then operate a test noise-influence source whose level is great enough to overload the receiver. There should be at least an output level increase of 20 decibels to prove that operation is not confined to the limited volume range indicated by curve *C* of Figure 7.

If this is not obtained with the receiver at maximum sensitivity, either the noise-voltage input is too small or the receiver is operating too close to the overload point, as indicated by curve *C*.

It is preferable, at least for small noise voltages, to operate the receiver on the linear portion of the curve. Also, when noise tests are being made, if the noise voltages are high and the receiver is operated close to the overload point, as in curve *C*, it will be impossible to determine whether corrective measures have reduced the noise voltage input from 100 to 10 microvolts. In such cases, the gain control must be retarded until the noise voltage does not bring the receiver to the overload point.

The test may now proceed with respect to the noise sources in the airplane. It is suggested that noise-reduction measures be employed until the remaining noise level is within three decibels of the background level of the receiver alone. The disadvantage of this method is that noise reduction may be carried to much lower noise levels than actually required in practice. The following method eliminates this difficulty.

(*b*). *Permissible Noise Level on the Basis of Power Level Required for Reception.* This method requires that a family of curves such as those given in Figure 7 be available for each frequency tested. Also, an estimate of the output power level required during operation of the airplane will be necessary. For example, some requirements are that for operation in a cockpit with 120 decibels of noise, there should be available at least 35 to 50 milliwatts of signal output. With these data as a reference base, the receiver is placed in operation and the output level read. Considering the conditions as represented by curve *B*, the output level with no signal input is seen to be about four decibels (reference 0 db = 1 mw in 600 ohms). Until permissible noise levels are established, it is just as well to assume a value of six decibels as a proper margin.

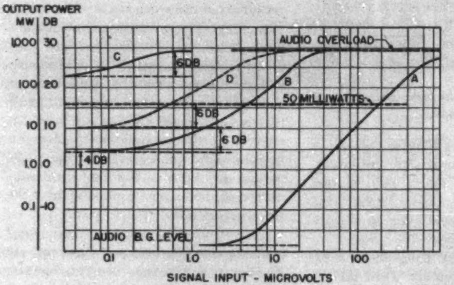

Figure 7. Typical aircraft-receiver calibration curves

Referring to curve *B*, six decibels below 50 milliwatts is seen to be about 12 milliwatts which corresponds to input noise voltage of about 1.5 microvolts. Utilizing this method it is possible to make measurements with a radio noise meter directly connected to the antenna, provided the antenna can be terminated in an impedance representing a receiver. This will then give a correlation between radio-noise-voltage input and audio noise output of the receiver.

This method still does not take atmospheric noise levels into consideration. As more work is done on this subject, the present arbitrary assumptions with respect to permissible noise levels will be replaced by actual values determined by test.

(*c*). *Determination of Paths of RIV Coupling to the Radio Receiver.* There are three paths of coupling to a radio receiver through which radio-noise influences may produce receiver-noise output; namely, the antenna lead-in, common impedances internal to the radio receiver, and common receiver-bonding impedances. Application of any noise-elimination method should be made only after each of the coupling paths has been evaluated separately.

RIV coupling through the medium of the common impedance formed by the receiver bonding connector(s) may be evaluated by disconnecting the antenna lead-in from the receiver-antenna terminal and connecting the antenna terminal to the receiver chassis. This connection eliminates coupling to the antenna lead-in and coupling because of the common impedance formed by the receiver-bonding connection. A reference reading is taken on the audio noise meter with the RIV source operating, and the antenna terminal is then connected to the aircraft structure instead of the receiver chassis. Any increase in receiver output (antenna terminal connected to structure) above the reference level (antenna terminal connected to receiver chassis) is due to RIV drop across the common impedance formed by the receiver-bonding connector(s). This form of coupling may be controlled by bonding the receiver to structure by means of the shortest possible bonding connectors. It should be noted that the radio-frequency impedance of a given bonding connector is proportional to its length and for a given length the impedance increases as the square of the frequency. Hence, every effort should be made to reduce the length of receiver bonding connectors to an absolute minimum.

Having controlled one coupling path, RIV coupling through the medium of common impedances internal to the receiver may now be evaluated by connecting the receiver-antenna terminal to the receiver chassis. Any increase of receiver output when RIV sources are operated is due to RIV drop across common impedances internal to the receiver. If this type of coupling is found to exist, it may be controlled by the application of RIV filters in the receiver power or remote-control wires which are responsible for carrying the radio-noise-influence currents into the receiver.

The antenna lead-in may now be reconnected to the receiver and evaluation made of RIV coupling to this conductor. This is done by noting the audio-noise-meter reading with the RIV sources operating and not operating. Any increase in receiver output level when the RIV sources are operating will be due to RIV coupling to the antenna lead-in. Coupling to this conductor may be controlled by separating physically the lead-in from wiring carrying radio-noise-influence currents, by shielding wiring carrying radio-noise-influence currents in the immediate vicinity of the lead-in or by shielding the antenna lead-in conductor.

Evaluation of the various RIV coupling paths should be made in the order given in order to prevent masking of one coupling path by another. If tests for one coupling path are to be made without benefit of control over all other paths, the evaluation of the result must consider the effect of the uncontrolled coupling paths.

COMMENTS

This writing has not proposed to offer any basic facts not heretofore available but to assemble and apply them in an engineering manner to obtain an economical solution to the problem.

The full utilization of the methods proposed in this writing requires teaching the subject to the electrical and radio-design engineers and enforcement through written design material. This can be accomplished by writing a basic "Radio and Electrical Layout Design Requirements Manual" for aircraft electrical engineers. The purpose of this manual is to make the designer aware of the problem so that he will then seek further assistance from the proper consultants. If properly done, this manual will not conflict with Army-Navy specifications.

Such manual material should hence be of general nature supplemented by material applying to each separate airplane model. The general Army-Navy specification covering shielding and bonding is used as a basis from which to derive an engineering shielding and bonding specification giving detail practice applying only to one model as a guide to the engineer. Such an engineering specification usually incorporates deviations from the Army-Navy specification that must be specifically approved by the Armed Services.

This intermediate step is necessary to ensure that general requirements necessarily written into a basic specification are not applied to specific airplanes to which they do not apply.

The question of how to handle radio-noise-suppression-design information on drawings is one that must be settled at the individual plant. It is recommended that the central point of responsibility for radio-noise-suppression design be placed upon the trained personnel in the electrical design group. Such items as bonding jumpers must be determined specifically as to quality and placement by the trained personnel and so shown on drawings. These items must not be left to the discretion of personnel untrained in the technical aspects of this problem.

The same applies to any and all references on drawings calling for cleaning or surface treatment utilized for the purpose of obtaining incidental bonding between parts.

Conclusions

1. Proper forethought to the layout of radio equipment will pay large dividends in the economical solution of radio-noise problems.

2. In all-metal aircraft the shielding effect of the structure greatly simplifies the economical solution of radio-noise problems in comparison to those of the nonmetal aircraft and in comparison to ground-receiving stations. Shielding and filtering is reduced greatly in that its application is restricted to those few points at which disturbance occurs. In the latter cases radio-noise influences are much more apt to require treatment at their sources which are usually very numerous.

3. It is desirable to design all equipment to produce the least possible RIV, but it is meaningless to ascribe arbitrary limits to RIV as measured at equipment terminals or throughout the electrical system in an attempt to protect radio-receiver performance.

This derives because RIV measurements on installed electrical wiring or equipment do not yield a measure of radio noise output unless a direct correlation has also been determined. At the higher frequencies noise coupling is functional with current rather than voltage, and steps taken to reduce RIV may increase RIC and result in a greater coupling. Radio-frequency current cannot be measured with available instruments; hence, each specific case of coupling must be eliminated by cut-and-try methods.

(a). The services of radio test engineers competent to handle radio-noise problems are an absolute necessity wherever radio equipment is installed, as the individual units are not yet advanced sufficiently in design to work together with other electrical-equipment items without interference.

Once a working "mockup" has been proved, the subsequent installation then can be made at modification centers without skilled technicians. Such tailoring cannot be obviated or even helped by setting general design limits to RIV as noted heretofore and only serves to distract attention from the practical job of making specific equipment work in a specific airplane.

4. Output measurements made with the proper type audio-noise meter are the most accurate with respect to actual evaluation of the effects of radio-influence sources.

5. The requirements of simplicity for production aircraft-radio-noise measurements dictate the use of an audio noise meter and prohibit the use of a radio-frequency noise meter. The radio-noise meter is not suitable for such use but remains a valuable laboratory tool.

6. The importance of proper bonding of the receiver to structure cannot be overstressed. The achievement of receiver bonding of low radio-frequency impedance reduces filter requirements to a minimum.

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Turn Ratio of the Capacitor Motor

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IN a previous paper[1] the basic equations for the stator- and rotor-winding currents of the capacitor motor were established in terms of certain coefficients which were functions of the fundamental constants of the machine. Application of these relations to establish the current loci was made to typical design data taken from earlier papers by Morrill,[2] and Puchstein and Lloyd,[3] based on a single value of the ratio of auxiliary-winding turns to main-winding turns. It is the purpose of this paper to extend the investigation to determine the effects upon current loci, and upon certain performance characteristics, of changing the turn ratio.

Application of the basic equations is made for three values of turn ratio, and it is definitely assumed that all other fundamental constants remain constant when the ratio is changed. This implies that the ratio of copper in the auxiliary and main windings changes, and that the dimensions of the machine and the distribution of windings also change. The converse case of a constant copper ratio is reserved for future study. For the present study application is made to the four-pole 110-volt one-fourth-horsepower single-phase motor discussed in the previous paper,[1] and the machine constants are as listed there, except that k is assigned values of $^1/_2$, 1, and 2.

It was necessary to compute coefficients M, N, P, Q, U, and W for each value of k by substitution in equations 21–26 of the previous paper.[1] These computations were made for ten values of per-unit speed ranging from $S=1.0$ to $S=0.0$, and for six values of capacity reactance from $X_c=20$ ohms to $X_c=500$ ohms at each speed. The variation of the coefficients in relation to k is shown in Figures 1 to 6, inclusive, for a constant speed $S=0.96$. Values of I_a and I_b were computed for each value of speed and capacity reactance using equa-

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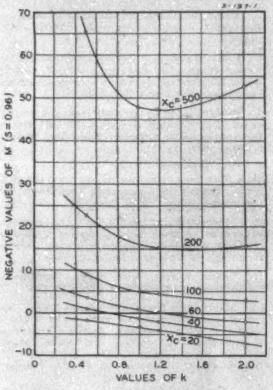

Figure 1. *M* as a function of turn ratio

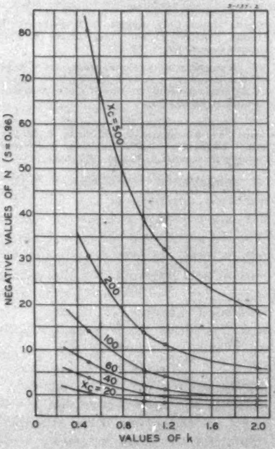

Figure 2. *N* as a function of turn ratio

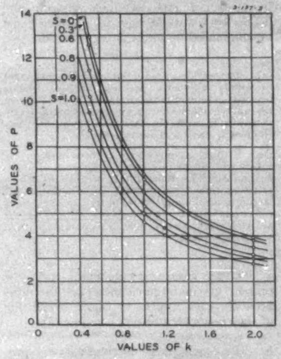

Figure 3. *P* as a function of turn ratio

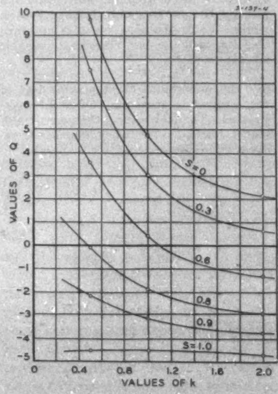

Figure 4. Q as a function of turn ratio

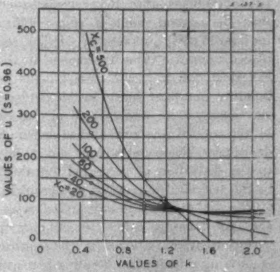

Figure 5. *U* as a function of turn ratio

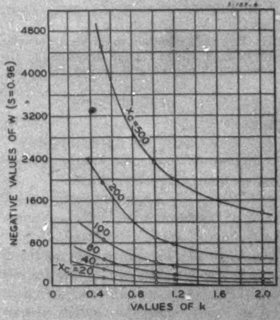

Figure 6. *W* as a function of turn ratio

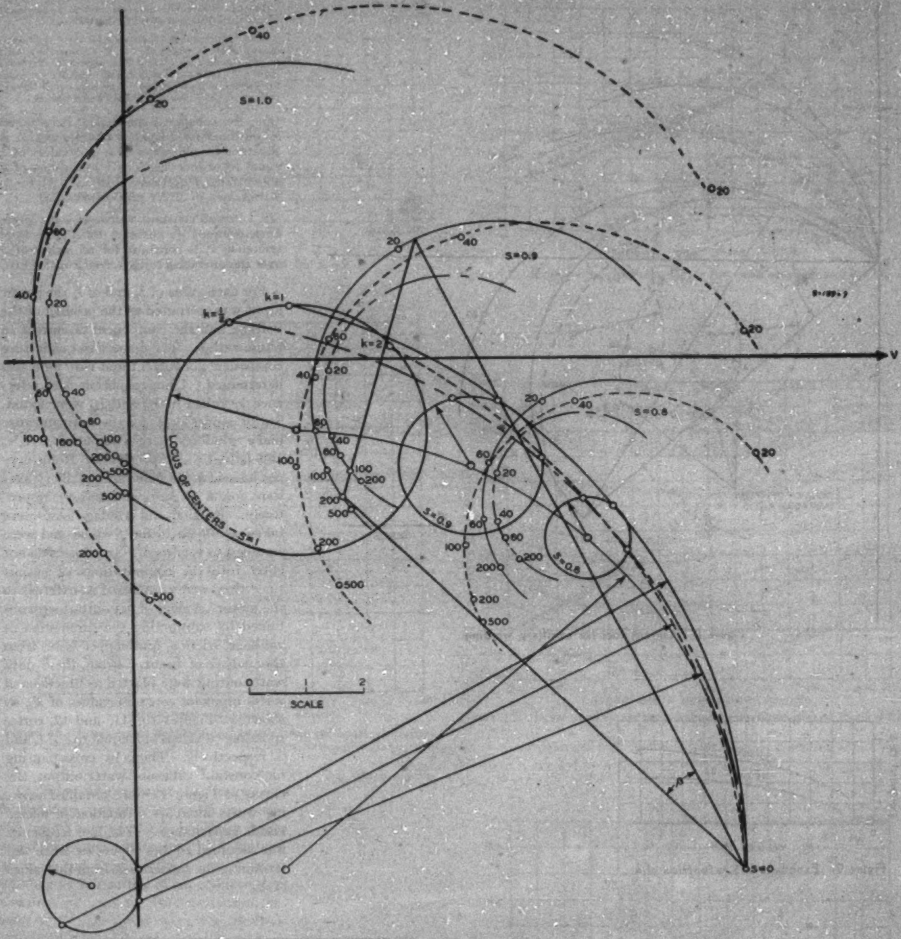

Figure 7. Current loci for main winding

tion 20 of the previous paper.[1] The values for $S=1$, $S=0.9$, $S=0.8$, and $S=0$ are plotted in Figures 7 and 8 for each value of k. Study of these figures shows that certain observations made in relation to Figures 8 and 9 of the previous paper[1] must be generalized to account for changes in turn ratio. It will be noted that

(*a*). The blocked-rotor value of I_a is independent of k.

(*b*). Current loci are circles for all values of k.

(*c*). For each fixed value of k the centers of current loci for constant S lie on a circular arc.

(*d*). The centers of circles for constant S traverse a circular locus as k is varied, and the centers of these latter loci also lie on a circular arc.

(*e*). The centers of the circular arcs passing through the centers of loci for constant S also traverse a circle as k is varied. For values of k between one half and one these centers can be assumed to lie on the axis of co-ordinates perpendicular to V (as shown in Figure 7 for I_a) and can be determined by the perpendicular bisector of the line joining the center of the circle for $S=1$ to the blocked-rotor point.

(*f*). The radii for circles of constant S are functions of k.

1. In Figure 7 it is found that for $k=1$ the radius of any circle of constant S is $S^{2.9}$ times the radius of the circle for $S=1$. The exponent decreases from 2.9 for values of k below one and increases for

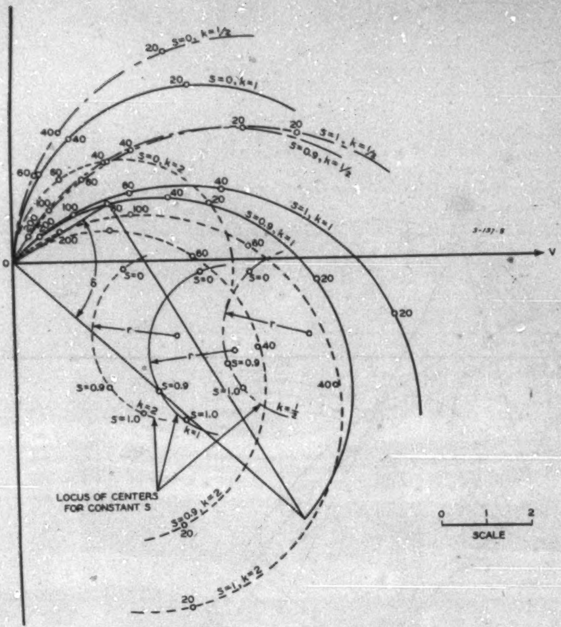

Figure 8. Current loci for auxiliary winding

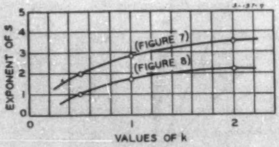

Figure 9. Exponent of S as function of k

values of k above one. The relation of the exponent to the turn ratio is shown in Figure 9.

2. In Figure 8 it is found that for $k=1$ the radius of any circle of constant S is $S^{1.75}$ times the radius of the circle for $S=1$. The exponent decreases from 1.75 for values of k below one and increases for values of k above one. The relation of the exponent to the turn ratio is shown in Figure 9.

(*g*). For constant k the angle β, determined as outlined for Figure 8 of reference 1, is constant for all values of S and values of k equal to or less than one. For values of k above one, β decreases with S. At $k=2$, β decreases in direct proportion to S.

(*h*). For all constant values of k the angle δ, determined as outlined for Figure 9 of reference 1, is constant for all values of S over the operating range ($S=0.9$ to $S=1.0$).

For each value of I_a and of I_b the power input was computed as the product of the voltage and the component of current in phase with it. The respective quadrature components of current and vars were also determined. Using equation 5 of reference 1, values of I_{2a} and I_{2b} were found. Stator copper losses for the main and auxiliary windings were computed as $I_a^2r_a$ and $I_b^2(r_b+r_c)$, respectively. Rotor copper losses for the main and auxiliary axes were found as $I_{2a}^2r_2$ and $k^2I_{2b}^2r_2$, respectively. Friction and windage losses were taken as 13 watts for $S=0.96$ and were assumed to vary as S^2. Iron losses do not enter into the computations of output since they were considered as external to the motor. Values of net output were obtained by subtracting the summation of windage, friction, and copper losses from the values of input. From these data watts output were plotted as functions of watts input for constant values of X_c, as shown in Figures 10, 11, and 12, corresponding to values of k equal to $^1/_2$, 1, and 2, respectively. Then, by cross-plotting for constant values of watts output, the curves of Figure 13 were obtained showing watts input as a function of microfarads capacitance. The line frequency was assumed to be 60 cycles when determining the capacitance from the values of X_c.

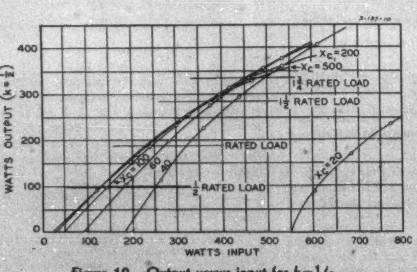

Figure 10. Output versus input for $k={}^1/_2$

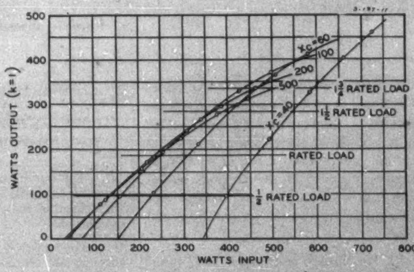

Figure 11. Output versus input for $k=1$

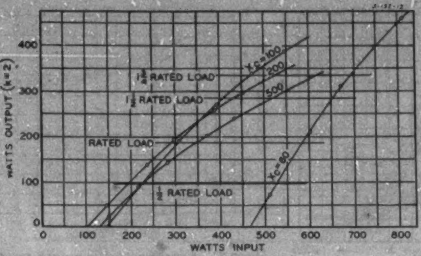

Figure 12. Output versus input for $k=2$

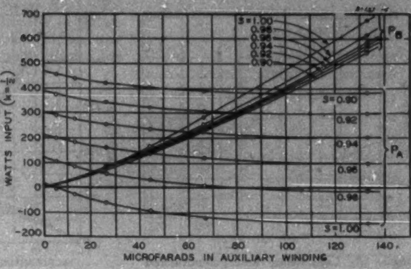

Figure 14. Input watts versus capacitance for constant speeds and $k={}^1/_2$

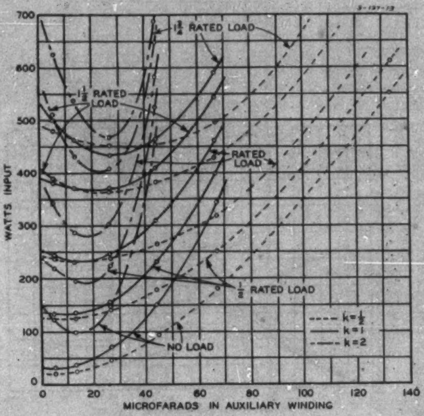

Figure 13. Input versus auxiliary-coil capacitance for constant output

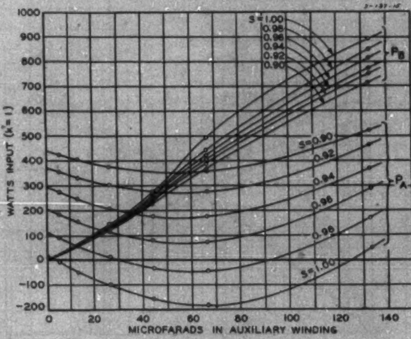

Figure 15. Input watts versus capacitance for constant speeds and $k=1$

In order to determine the values of capacitance at which the watts input to the main and auxiliary windings were equal the curves of Figures 14, 15, and 16 were plotted showing the input to each winding as a function of capacitance, for constant values of S. Intersections of the curves of like speed gave the desired values of capacitance. These values of capacitance were then plotted as functions of speed as shown in Figure 17.

By a similar procedure curves of equal (but opposite) vars for the two windings were computed and plotted as shown in Figures 18, 19, and 20. From the intersections of curves of corresponding speed the values of capacitance to produce a line power factor of unity were found. These values are plotted as a function of per-unit speed in Figure 21.

In a similar manner curves of volt-amperes for each winding were plotted as functions of capacitance. These are

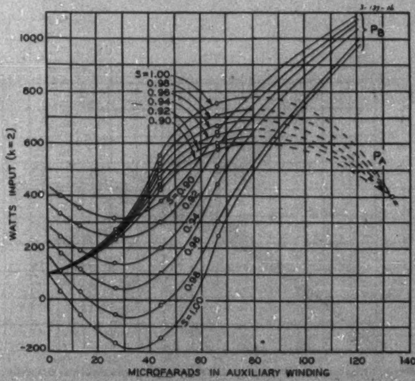

Figure 16. Input watts versus capacitance for constant speeds and $k=2$

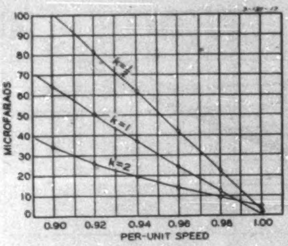

Figure 17. Capacitance versus per-unit speed for equal watts

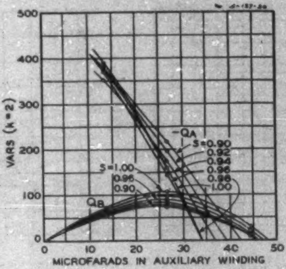

Figure 20. Vars versus capacitance for constant speeds and $k = 2$

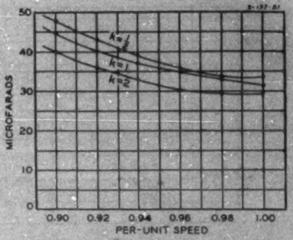

Figure 21. Capacitance versus per-unit speed for equal vars

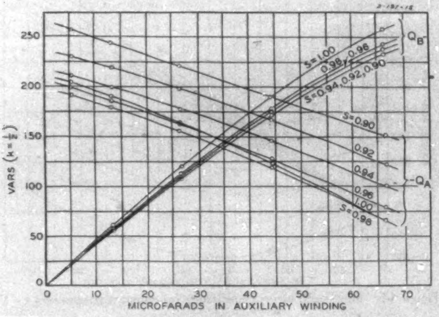

Figure 18. Vars versus capacitance for constant speeds and $k = ^1/_2$

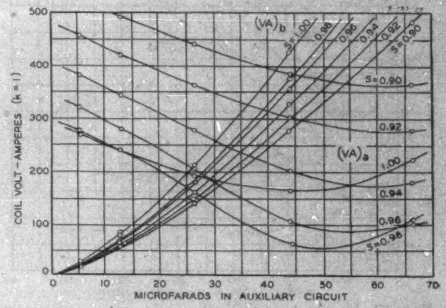

Figure 22. Volt-amperes versus capacitance for constant speeds and $k = 1$

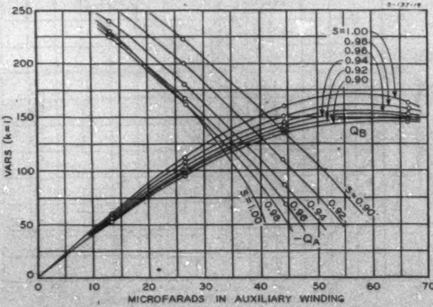

Figure 19. Vars versus capacitance for constant speeds and $k = 1$

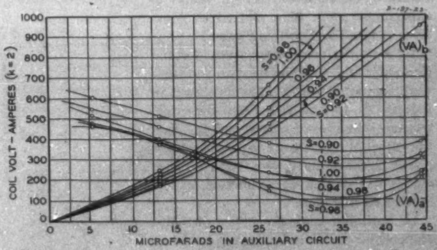

Figure 23. Volt-amperes versus capacitance for constant speeds and $k = 2$

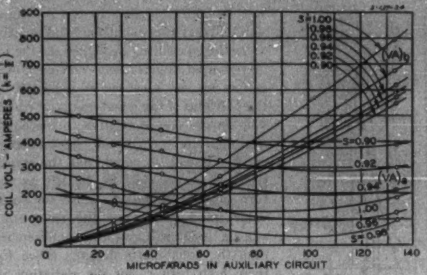

Figure 24. Volt-amperes versus capacitance for constant speeds and $k=1/2$

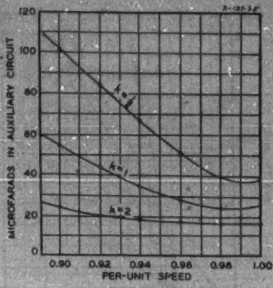

Figure 25. Capacitance versus per-unit speed for equal volt-amperes

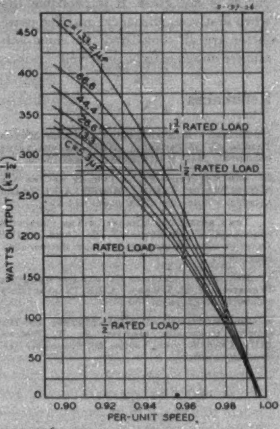

Figure 26. Watts output versus per-unit speed for $k=1/2$

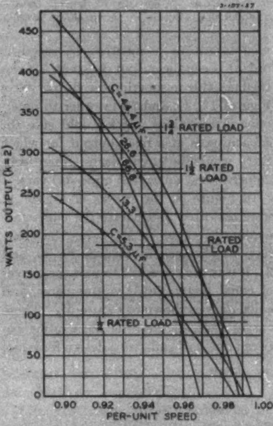

Figure 27. Watts output versus per-unit speed for $k=2$

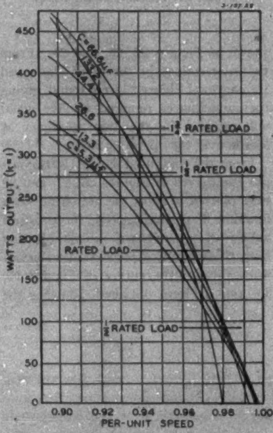

Figure 28. Watts output versus per-unit speed for $k=1$

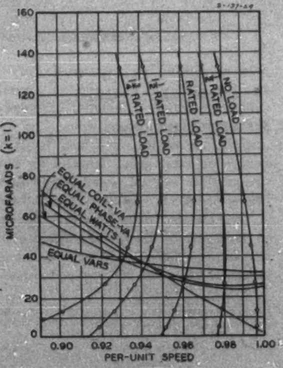

Figure 29. Capacitance versus per-unit speed for constant outputs and $k=1$

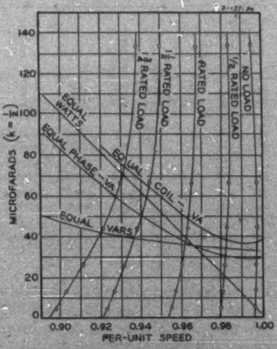

Figure 30. Capacitance versus per-unit speed for constant outputs and $k=1/2$

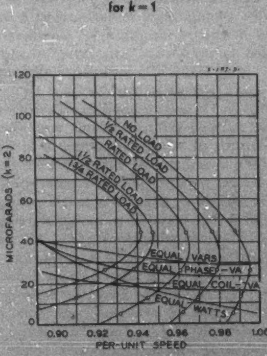

Figure 31. Capacitance versus per-unit speed for constant outputs and $k=2$

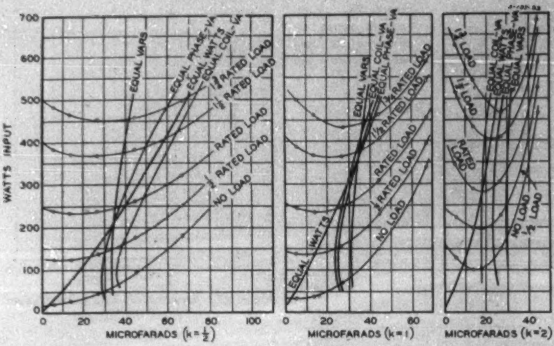

Figure 32. Equal-relations curves

shown as Figures 22, 23, and 24. From the intersections of curves of like speed the curves of Figure 25 were found, giving the capacitances required to give equal volt-amperes in the two windings at speeds over the operating range.

From the computed data of watts output the curves of Figures 26, 27, and 28 were plotted to show the net output as a function of per-unit speed, for constant values of capacitance. Cross-plotting from these curves for constant values of watts output, the curves of Figures 29, 30, and 31 were obtained, showing the microfarads as a function of speed in order to keep the output constant. It was then possible to superimpose the curves of Figures 17, 21, and 25 in order to determine intersections which would give the capacitances at a fixed output for which there would be equal watts, equal volt-amperes, and equal (but opposite) vars.

In Figure 32 the curves of Figure 13 were replotted to separate the curves according to values of *k*. Using the intersections obtained in Figures 29, 30, and 31, it was then possible to plot the curves of equal watts, equal volt-amperes, and equal vars as shown in Figure 32. These curves indicate that for turn ratios between one-half and two a design for equal watts is preferable to a design for equal volt-amperes since for all loads up to rated value the efficiencies are higher and the required capacitance is less. The line power factors are less, however, as indicated by the positions of the equal-watt and equal-volt-ampere curves relative to the equal (but opposite) vars or unity-power-factor curve. It will be noted that the equal-relations curves shift to the left as the turn ratio is increased, hence for some values of *k* above two the equal-volt-ampere design will give higher efficiencies than the equal-watts design. Since for these high turn ratios the variation in capacitance between the equal-relations curves is not great, it is probable that when first cost, efficiency, and line power factor are all considered the equal-volt-ampere design is to be preferred.

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Aircraft Transformers—Small and Light

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THE purpose of this paper at the present time is to make available for the aircraft industry knowledge and information on the newest types of transformers specially developed to obtain extreme small size and the lightest possible weight. It is felt that the aircraft engineers should be given every opportunity to take advantage of the latest developments without the delay of extensive research. Every effort has been made to comply with their persistent demands for equipment as small and as light as possible.

Introduction

The necessity for light weight and small size in aircraft transformers is very evident, since any reduction in space and weight will reflect in larger capacities for fuel and bomb loads. Various types of transformers[1] have been developed recently, such as, three-phase to single-phase, single-phase to three-phase, two-phase to three-phase, three-phase to two-phase, combination a-c to d-c transformer-rectifier self-regulated units, miniature instrument type, and a line of general purpose aircraft units with kilovolt-ampere ratings ranging from 100 volt-amperes to five kilovolt-amperes, with weights ranging from 0.8 pound to 9.5 pounds and operating at ultimate temperatures of 225 degrees centigrade. Included are vibrator inverter transformers, voltage stabilizers, high-voltage and low-voltage booster ignition coils.

General Features

The construction used on many of these designs features open core and coils and screw-type terminals. On those units intended for high temperature operation the terminal boards are made of Mycalex, and all internal connections are joined with silver solder. For normal temperature design, cloth-base Textolite is employed in terminal-board construction.

In design, careful attention is given to locate the center of gravity as low as possible and as near the mounting surface as practicable. All units are also designed to withstand vibration tests specified by the United States Army and Navy.

Core materials have been selected to operate at high flux density but not to saturate. Laminations ten mils thick, which provide for reasonable losses and allow practical manufacturing, are chosen for 400-cycle designs.

The choice of insulating materials is most important to permit operation of aircraft units at high temperatures far above those in past practices, furthering the primary object of weight reduction. All insulation in the special high-temperature units, including winding spools, layer insulation, wrappers, and conductor-turn insulation, is glass cloth and glass fiber impregnated with a specially developed resin capable of operating at 225 degrees centigrade continuously.

To prevent oxidation of the copper wire at elevated temperatures the wire is cadmium plated prior to application of the glass fibers.

Overloads for short periods are permitted, during which the temperature may exceed operating values. This means that $1^1/_2$ to 2 times normal loads can be tolerated for a few minutes with no damage to the transformers.

Life tests have been completed on a four-pound two-kilovolt-ampere unit which operated in excess of 1,000 hours at 225 degrees centigrade. The unit was exposed to 95 per cent relative humidity, while cool and not operating, for eight hours in each 24-hour cycle. At the end of the life test the efficiency and heating had not changed appreciably, and continued operation was satisfactory.

Table I. Heating Data for Various Altitudes With and Without Dural Plate

Altitude	Load in Watts	With Dural Mounting Plate	Ambient Temperature (Deg. C.)	Ultimate Temperature (Deg C) Outer Coil	Inner Coil	Top of Core
Sea level	2,000	Yes	30	172	208	172
Sea level	2,000	No	30	188	245	212
12,000 feet	2,000	Yes	30	181	217	186
12,000 feet	2,000	No	30	202	262	227
25,000 feet	2,000	Yes	30	198	229	199
25,000 feet	2,000	No	30	218	281	244
40,000 feet	2,000	Yes	30	206	242	208
40,000 feet	2,000	No	30	233	296	259

Altitude Performance

Load tests have been made at sea level on a two-kilovolt-ampere 400-cycle high-temperature transformer to determine the extent of heating at various altitudes. To produce the same heating at other altitudes as obtained at sea level, the load was then changed. The transformer is almost always mounted on a Dural plate or channel in the aircraft structure which will dissipate considerable heat. A comparison was made on transformer heating by making load tests with the test unit mounted on a 12-inch square Dural plate $^3/_{32}$ inch thick. The unit was then freely suspended in air to omit the plate. Heating data are tabulated in Table I.

Test data tabulated in Table II show that the core dissipates most of the total losses as indicated by constant core temperature and the reduction in coil temperature with reduced loads. It may be observed that for constant ambient the allowable output at 40,000 feet is 79 per cent of normal sea-level capacity. However, since the lower ambient temperatures encountered at high altitudes will compensate for additional heating, it is safe to operate the transformer at full capacity at all times.

Table II. Output Loads at Various Altitudes for Same Core Temperatures

Altitude	Load in Watts	Ambient Temperature (Deg C)	Ultimate Temperature (Deg. C.) Outer Coil	Top of Core
Sea level	2,000	30	172	174
12,000 feet	1,830	30	156	174
25,000 feet	1,680	30	148	170
40,000 feet	1,570	30	144	170

Test data tabulated in Table III show comparative temperature rises of core and coils over an ambient temperature of minus 40 degrees centigrade. By comparing these data with the data from Table I (with Dural mounting plate) it may be observed that cold ambients are not so effective as warmer ambients on heat dissipation at the higher altitudes.

The effects of an air blast on heat dissipation are observed in Table IV. A velocity of air, measured to be 3,380 feet per minute, was forced over the unit for the purpose of obtaining, first, the decrease in heating at rated load of 2,000 watts and, second, the allowable increase in load to obtain the same heating as is encountered

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Table II. Output Loads at Various Altitudes and Low Ambient Temperature

Altitude	Load in Watts	Ambient Temperature (Deg C)	Ultimate Temperature (Deg C)		
			Outer Coil	Inner Coil	Top of Core
Sea level	2,000	−40	99	146	108
12,000 feet	2,000	−40	123	164	127
40,000 feet	2,000	−40	171	214	171

at sea level with no forced ventilation and with use of the Dural plate.

Regulation, efficiency, and exciting current data are tabulated in Table V.

Standard Line for General Purposes

A standard line of single-phase high-temperature lightweight transformers for general purpose aircraft applications has been established. The kilovolt-ampere ratings are: 0.100, 0.250, 0.500, 1.000, 2.000, and 5.000. The voltage ratings are 120 volts high voltage and 28.5 volts low voltage. Frequencies of 380 cycles minimum to 420 cycles maximum are standard, although use of higher frequencies is not objectionable. An operating temperature rise of 200 degrees centigrade is normal and makes possible large reductions in size and weight when compared with transformers constructed with conventional materials.

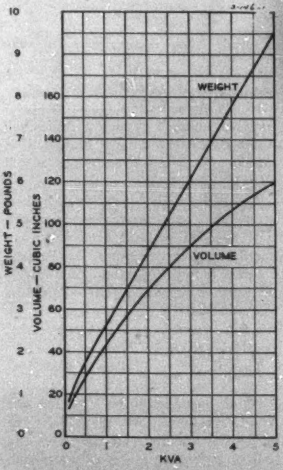

Figure 1. Weight and volume characteristics of high-temperature aircraft transformers

Table IV. Effects of Air Blast on Kilovolt-Ampere Rating at Various Altitudes

Altitude	Static Pressure Inches of Water	Load in Watts	Ultimate Temperature (Deg C) of Inner Coil
25,000 feet	0.27	2,000	117
40,000 feet	0.14	2,000	153
25,000 feet	0.27	2,670	212
40,000 feet	0.14	2,300	214

Weights and volumes are shown in Figure 1. Construction and appearance may be observed from Figure 2.

When selecting mounting location for these transformers, consideration should be given to the high temperature at which they operate.

Three-Phase Transformers

The special high-temperature materials used in the single-phase units just described are also adaptable to the construction of three-phase 400-cycle units. For example, a 32-kva three-phase unit was made with dimensions of 5 by 9 by 12 inches and weighing only 43 pounds. A 7.5-kva three-phase unit was made with dimensions of $2^3/_4$ by 7 by 7 inches and weighing only 13 pounds. Three-phase units will weigh approximately 20 per cent more than single-phase units for the same kilovolt-ampere rating.

Figure 2. High-temperature aircraft transformer rated: 400 cycles, 2.0 kva, 120 to 28.5 volts

Dimensions $2^3/_8$ by $4^1/_4$ by $4^5/_8$ inches

Transformer-Rectifier Units

Developmental work is well advanced on two self-regulated transformer rectifier units to operate directly from an engine-driven a-c generator; these units are rated at 100 and 200 amperes, respectively, at 30 volts, d-c output. The input frequency and voltage may vary independently of each other. The d-c output voltage may be selected at any value between 26 and 30 volts and held constant regardless of load current variations from no load to full load. The circuit includes a control reactor, a three-phase high-temperature transformer, a voltage-sensitive variable resistor, and a selenium rectifier. Units rated at 3.0 kw and 6.0 kw will weigh approximately 30 pounds and 55 pounds, respectively.

Phase-Changing Transformers

A definite trend toward increased use of single-phase electrical equipment on aircraft has been noted. Many planes, particularly large bombers, have, as standard equipment, three-phase auxiliary generators, which, of course, are of limited capacity. A single-phase load connected to any one phase of the power supply would cause considerable unbalance in the three-phase voltages, which would also reduce the available volt-ampere capacity of the generator. Therefore, it became desirable to develop a transformer to furnish a single-phase voltage and which could reflect balanced watts at equal power factors in the three generator phases. Several designs have been completed in

Figure 3. Three-phase to single-phase aircraft transformer rated: 400 cycles, 0.115 kva, 115 volts three-phase to 115 volts single-phase

Dimensions 2 by $3^3/_8$ by $5^1/_4$ inches

Table V. Electrical Data

Rating in Kilovolt-Amperes	Excitation Current in Per Cent of Full-Load Current	Voltage Regulation (Per Cent)	Efficiency (Per Cent)
0.100	42	13	87
0.350	35	11	90
0.500	26	10	91
1.000	10	7	92
2.000	7	6	93
5.000	6	5	94
32.500	5	4	95

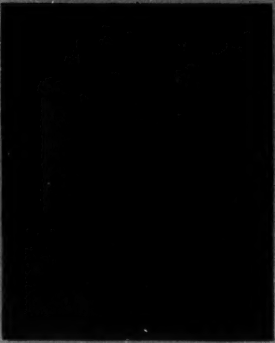

Figure 4. Single-phase to three-phase transformer rated: 400 cycles, 48 volt-amperes, 115 volts single-phase to 115 volts three-phase

Dimensions $2^{1}/_{16}$ by $2^{5}/_{8}$ by $2^{11}/_{16}$ inches

Figure 5. Instrument autotransformer rated: 400 cycles, 9.0 volt-amperes, 120 to 28.5 volts

Dimensions $1^{3}/_{8}$ by $1^{3}/_{8}$ by $1^{3}/_{4}$ inches

various ratings. Their efficiency is 90 per cent, and they operate at unity input power factor. Load variations do reflect in some unbalanced input power factor, but the output voltage regulation is not seriously affected. It is essential that phase rotation always be maintained in the stipulated direction and that the frequency remain constant. One such unit has been produced which will deliver 115 watts at 115 volts and 400 cycles from a three-phase 1,000 volt-ampere generator for the operation of a single-phase radio compass of an automatic pilot. This unit weighs 2.38 pounds and has dimensions of $2^{1}/_{8}$ by $3^{7}/_{8}$ by $5^{1}/_{2}$ inches. Refer to Figure 3 for a construction view.

For operation of small gyro instruments three-phase motors have been found most suitable. This makes it necessary on such aircraft as fighters, which are equipped with a single-phase generator, to use a single-phase to three-phase transformer for operation of these instruments. A transformer using a special nonlinear split-phase circuit is now available to supply 115 volts, three-phase, at 400 cycles from a 115-volt single-phase generator supply. This supply will operate two *KB-1* or *KC-1* General Electric gyro instruments having a total load of 48 volt-amperes. This transformer is so designed that it compensates for the difference in starting and operating load power factors and thus permits rapid acceleration of the gyro motors; however in general, this type of transformer should be designed for specific loads in which the power factor and voltamperes are essentially constant. The transformer is both small and lightweight, weighing only eight ounces. Refer to Figure 4 for a construction view.

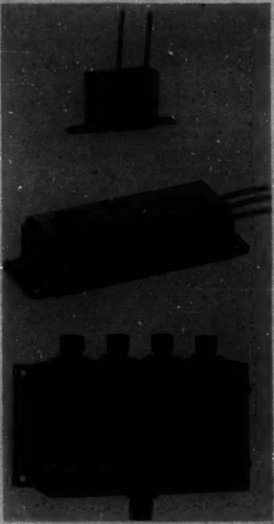

Figure 6. Reactor ballasts for fluorescent lighting on aircraft

There are other aircraft applications that require the use of two-phase to three-phase or three-phase to two-phase transformers. Such units can be made small and lightweight by use of special steels and insulating materials.

Transformers for Other Aircraft Applications

Small single-phase transformers of 4.5- and 9.0-watt ratings are available for operation of other flight instruments such as the remote indicating compass. These units weigh only two and three ounces, respectively, and are minute in physical size. Refer to Figure 5 for construction view.

Small linear a-c reactors, required for certain miniature instruments, have also been developed. One such unit, rated 74 volts, 0.03 ampere, 400 cycles, and designed for assembly within the instrument has a volume of only $1^{1}/_{4}$ cubic inches and weighs but two ounces.

Reactor ballasts are available for fluo-

Figure 7A (left). Low-voltage ignition booster coil

Figure 7B (right). High-voltage ignition booster coil

rescent lighting in aircraft having a suitable a-c power system. They serve the same purpose as in commercial practice except that they are designed for light weight and small size and 400 cycles. Refer to Figure 6 for typical units.

Voltage-Stabilizer Transformer

A recent development in voltage stabilizers for 60-cycle service is equally adaptable for 400-cycle, or even 800-cycle, aircraft systems. These units are used for those instruments which require a constant regulated input voltage. If the frequency is constant, the load, power factor, and input voltage may vary over wide limits; if the load and its power factor are constant, the input line voltage may vary; the frequency may also vary over a ten per cent range and yet provide for a constant output voltage held to within approximately three per cent.

Booster Ignition Coils

In the past it was common practice to use high-voltage ignition booster coils for providing positive ignition in starting of aircraft engines. Recently, low-voltage booster coils have been made available, which operate directly from the battery and furnish rated current through the low-voltage winding of the magneto.

This low-voltage current is then stepped up by transformer action within the magneto to provide a high-voltage output suitable for ignition starting purposes. This system is being used extensively and is more practical in that it simplifies wiring and reduces radio interference. Refer to Figures 7A and B for typical units.

Figure 8. Vibrator-type inverter transformer rated: 24 volts d-c to 16–16 volts a-c at 60 cycles with radio inteference suppressed

Inverter Transformers

Inverter transformers are available for those applications requiring a conversion of power from d-c to a-c or requiring a d-c voltage change. The frequency of the vibrator, which is an essential part of the equipment, is limited to a preferred 120 cycles. This prevents their adoption, generally, to 400-cycle systems. They are now used extensively to provide a-c voltage to a special field winding of power-turret Amplidyne generators for controlling voltage fluctuations caused by residual flux. Without such control the turret will become unstable, thus reducing its accuracy. A small two-pound inverter transformer operating directly from the battery will provide the necessary control. Refer to Figure 8 for a typical unit.

Inverters can be made to operate directly from the variable battery voltage to provide a self-regulated a-c or d-c output voltage held constant within approximately three per cent. Wave form is almost equivalent to sine wave and entirely suitable for most applications.

A circuit[1] is usually employed in which the vibrator contacts do not break current. This provides for long reliable life of the equipment. Improvements have been made recently to correct the output wave form, increase the efficiency and provide stabilized output voltage.

Conclusions

The transformers discussed in the preceding paragraphs are those which seem most needed for aircraft applications. In many cases they have replaced heavy equipment and also have made it possible for certain generators to serve dual purposes.

There may be many other applications not covered by the foregoing paragraphs; however, it is hoped that the information disclosed herein may be of value in solving transformer problems in the aircraft industry.

Reference

1. New Type of D-C to A-C Vibrator Inverter, O. Kiltie. AIEE Transactions, volume 59, 1940, April section, pages 245–7.

Application of 720-Cycle Carrier to Power-Distribution Circuits

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LOW-frequency carrier has been applied extensively to distribution circuits for remote control of street lights, window lights, off-peak water heaters, and similar services. Since carrier makes available a control system whose operating flexibility can be duplicated only by the use of a number of separate wire or pilot circuits, it permits saving large amounts of copper and other critical war materials. The necessity of reducing illumination or "blacking out" lighting circuits under war conditions has emphasized the value of the direct control provided by a carrier system. More recently the importance of carrier control for air-raid sirens and for signaling air-raid warnings to defense centers and civilian-defense personnel has been demonstrated.

These types of application generally require remote control of a large number of devices from a central point on the power system and economically favor the use of a relatively low-frequency carrier which can be transmitted efficiently through power-distribution transformers in order to reach the 115- or 230-volt power supplies available for operation of the receiving controllers. The carrier frequency of 720 cycles commonly used in these applications has been selected after carefully evaluating the relative economic values of transmission efficiency, simplicity of equipment, and receiving-controller sensitivity required to prevent interference from frequencies normally present on the power system.

This paper presents methods by which the 720-cycle transmission characteristics of power-distribution systems can be predicted to ensure successful operation of the carrier channel. The applicability of these methods has been confirmed by calculations, calculating-board studies, and field measurements conducted on actual installations over a 15-year period. To simplify the application effort required, the methods utilize approximations which do not always yield precise results, but the over-all error is generally conservative.

Basically, the application problem is one of transmitting energy over a power system at 720 cycles instead of at 60 cycles, so the application engineer follows, in many ways, the same steps as with 60-cycle engineering. Even though the 720-cycle voltage used is only a few per cent of the 60-cycle system voltage, there are similar problems of voltage drop or rise, favorable location of the generator (transmitter), watts generated and watts delivered, and vars that must be supplied either at the transmitter or at other points.

Although conductor size, and spacing, and other factors are involved, the usual 60-cycle conception is that lines up to 30 miles length are considered short lines—if a solution of high accuracy is required—or up to approximately 120 miles if only fair accuracy is required. In extending this conception to 720-cycle transmission it must be remembered that short and long lines are relative terms depending on the impressed frequency. A 120-mile 60-cycle transmission line often can be considered a short line, since its distributed capacitance causes a no-load voltage rise at the receiving end of only three per cent at 60 cycles, but this same no-load voltage rise is obtained at 720 cycles if the line length is decreased in the same ratio as the frequency is increased. For this reason a ten-mile line at 720 cycles and a 120-mile line at 60 cycles are both short lines within the same limits of accuracy, and 720-cycle transmission problems can generally be analyzed with reasonable accuracy using short-line conceptions for transmission distances up to ten miles. Beyond this distance the errors introduced by applying short-line methods increase but are generally conservative (unfavorable to the application) for even greater distances. Hereafter the term "short line" is understood to include transmission over overhead lines up to ten miles distance.

Short-line applications are most easily solved when it is practical to couple the 720-cycle transmitter into the 60-cycle system so that, at both frequencies, power flows toward the loads in the same direction in all sections of the feeders. In these applications the 720-cycle performance can be predicted from values which usually have been determined previously in order to plan properly the 60-cycle operation of the system, such as voltage drop, conductor size and spacing, total load kilovolt-amperes, power factor, and the system short-circuit kilovolt-amperes. It is then unnecessary to know how the load is distributed over the system since the effect of this factor is included in the actual 60-cycle voltage drop.

In applications where data of this kind are not available, it is usually easier to determine the 720-cycle performance from factors which take into account the 60-cycle kilovolt-ampere load, the distribution of the load, and the system short-circuit kilovolt-amperes. This is also the best method for long-line applications, as well as for short-line applications in which power does *not* flow toward the load in the same direction at both 60 and 720 cycles, because, in these cases, the relation between 60- and 720-cycle voltage drops often becomes complex.

Complicated systems involving long lines or elaborate interconnections with many sources of 60-cycle power may require the use of a network analyzer or calculating board if a highly accurate solution is required.

The two schemes commonly used to couple the 720-cycle transmitter to the power system are the series and the shunt coupling arrangements. With series coupling the 720-cycle voltage is introduced in series between the load and the 60-cycle generator. With shunt coupling it is introduced in parallel with the load and 60-cycle generator. In either case the 720-cycle loss in the generator depends on the 720-cycle voltage present, the generator 60-cycle reactance (or the 60-cycle short-circuit kilovolt-amperes) and the 60-cycle kilovolt-ampere rating of the load on which the 720-cycle voltage is impressed. For a very large ratio of 60-cycle short-circuit kilovolt-amperes to load kilovolt-amperes, the physical size of the 720-cycle transmitter is smaller, using series coupling, but below some smaller value of this ratio the transmitter physical size is smaller when using shunt coupling.

On any system it is always possible to locate the transmitter or transmitters to provide the desired coverage. As examples, the control voltage can be impressed on only a portion of one primary feeder, or on one entire primary feeder, or on all primary feeders from one substation, or on a number of substations fed

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from a common higher-voltage transmission system. In each of these examples different values of 60-cycle load and short-circuit kilovolt-amperes are encountered, which in turn influence the size of the transmitter and the choice between series and shunt coupling.

720-Cycle Characteristics of 60-Cycle Lines and Apparatus

When 720 cycles is superimposed on a 60-cycle circuit, for purposes of calculation, all values which are affected by frequency must be corrected as follows:

1. The reactance of the feeder conductors and the leakage reactance of transformers are proportional to frequency and hence are increased in the ratio of 720/60 or 12 times. But the series resistance of the feeder conductors and transformers increases relatively little with frequency, and therefore the series resistance can be safely neglected at 720 cycles for determining current flow or voltage drop.

2. Distribution-transformer susceptance, which determines the transformer exciting current, is directly proportional to frequency and, hence, can be safely neglected.

3. A distribution transformer can be represented by the transformer leakage reactance in series with the secondary load. This reactance is increased 12 times by the frequency increase. The transformer voltage drop is also increased, being of the order of ten per cent at 720 cycles compared to two or three per cent at 60 cycles.

4. To determine the characteristic of the 60-cycle load, it is convenient to divide the load into the two following types:

(*a*). Noninductive loads such as lights, ranges, water heaters, and heating appliances whose 720-cycle impedance can be represented by a pure resistance numerically equal to the 60-cycle load impedance. Hereafter these are classed together as lighting load.

(*b*). Inductive loads composed of motors are treated as though the motors stand still since there is no transfer of mechanical power through the motor shaft. Thus the locked-rotor reactance determines its 720-cycle impedance. The 60-cycle locked-rotor reactance of a typical three-phase motor is 20 per cent. The 720-cycle reactance is increased 12 times and is equal to 240 per cent of the motor full-load 60-cycle impedance. Single-phase motors show a similar 720-cycle impedance increase. The amount of motor load present can be determined in order to obtain an accurate solution, but this usually does not justify the additional complexity of the solution. The usual practice is to consider the entire load including the motors as lighting load. This approximation is on the conservative side, since the smaller load impedance used indicates somewhat greater than actual 720-cycle voltage drop and power loss.

5. The capacitive reactance attributable to conductor capacitance or power-factor-correction capacitors is inversely proportional to frequency and hence is divided by 12 at 720 cycles. A small amount of capacitance present corrects the system power factor and decreases the 720-cycle voltage drop. Excessive amounts of lumped capacitance have the opposite effect, but, if undesirable effects do occur, they can always be overcome by applying corrective measures at the capacitors. The short-line methods outlined hereafter neglect the effect of distributed capacitance, and the effect of lumped capacitance is treated separately.

Base Used for 720-Cycle Voltage Calculations

For convenience the 720-cycle voltage at all points on the system is referred to a 115-volt 60-cycle base. Hence, the 720-cycle voltage at any point is the voltage at that point on a 115-volt 60-cycle base multiplied by the transformation ratio of the transformer (or transformers) intervening between this point and a 115-volt 60-cycle circuit. The voltages considered on a 115-volt 60-cycle base are directly applicable to 115-volt receiving controllers. The 230-volt controllers required operating voltages exactly twice those for 115-volt controllers. But in practice a 230-volt controller receives exactly twice the voltage received by a 115-volt controller at the same location, so the results are equally applicable if controllers of either or both voltage ratings are actually applied.

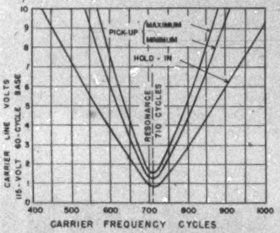


Figure 1. Frequency-voltage characteristics of 720-cycle carrier relay as used in General Electric types *SCS*-15 and *SCS*-17 controllers

Permissible Range in 720-Cycle Received Controller Voltage

The frequency-voltage characteristics of the 720-cycle controllers are shown in Figure 1. The 710-cycle operating voltage range is 1.5 to 15 volts on a 115-volt 60-cycle base. Because of motor slip in the 60/720-cycle conversion at the transmitter, the transmitted frequency usually ranges from 713 to 716 cycles under different load conditions, with a total variation from 707 to 723 cycles if the standard allowance of 60 cycles ± $^1/_2$ cycle is made for transmitter-power-supply variation. For this complete frequency range, the recommended controller voltage range is 1.75 to 15 volts. If sequence coupling is used so that a controller must not operate on one feeder when another feeder is energized, the maximum recommended interfering voltage is one volt.

720-Cycle Power Consumption by Controller

The 720-cycle power consumption of a controller is approximately the same as the 720-cycle power absorbed by a 60-cycle load of one kilovolt-ampere, so it usually represents a small per cent of the total 720-cycle power. In view of other unfavorable assumptions the controller power consumption is ignored unless it exceeds 20 per cent of that taken by the feeder 60-cycle load. Beyond this point the effect of the controllers should be included by adding one kilovolt-ampere of 60-cycle load for each additional controller.

Short-Line Voltage-Drop Equations

Equations 1 and 2 are well known and entirely acceptable in determining 60-cycle voltage drop, but for reasons outlined below, equation 3 is preferable for use at 720 cycles.

Per cent volts drop

$$=\frac{(\text{kva})d\ (R\cos\theta+X\sin\theta)}{10(\text{kv})^2} \qquad (1)$$

where

kva = load in three-phase kilovolt-amperes
d = length of line in miles
θ = load power-factor angle
R = line resistance in ohms per mile
X = line reactance in ohms per mile
kv = line kilovolts

An equation for 60-cycle voltage drop can also be written as:

Per cent volts drop

$$=\%IR\cos\theta+\%IX\sin\theta \qquad (2)$$

where

$\%IR$ = IR volts expressed as a percentage of the received volts
$\%IX$ = IX volts expressed as a percentage of the received volts

If a 720-cycle voltage is superimposed so that power flows toward the load in the same direction at both 60 and 720 cycles and if the assumptions in the preceding paragraphs numbered one to five apply, then the current through each load per volt of impressed load voltage is the same at both frequencies. The $\%IX$ is directly proportional to frequency. The $\%IR$ is the same at both frequencies, but, in view of the assumption made in paragraph 1, the 720-cycle value of R or $\%IR$ is zero. Also θ is 0 under the assumption

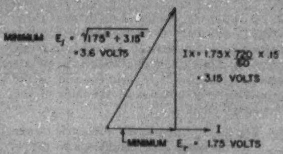

MINIMUM $E_f = \sqrt{1.75^2 + 3.15^2}$
$= 3.6$ VOLTS
$IX = 1.75 \times \frac{720}{60} \times .15$
$= 3.15$ VOLTS
I
MINIMUM $E_r = 1.75$ VOLTS

Figure 2. Feeder input volts required when the 60-cycle %*IX* between the controller and the input point is 15 per cent

in paragraph 4*b*. It can now be seen that the equations 1 and 2 are based on approximations which are not valid at 720 cycles because, if R or $\%IR$ and θ are 0, the per cent volts drop is 0.

By solution of Figure 2 on a per-unit basis, equation 3 is obtained which is exact within the limits of the assumptions previously made.

$$E_f = E_r\sqrt{1+(0.12\times\%IX)^2} \qquad (3)$$

where

E_f = feeder 720-cycle transmitted volts on 115-volt 60-cycle base
E_r = 720-cycle volts received at 115-volt controller
$\%IX$ = total 60-cycle IX volts from transmitting end of feeder to the controller expressed as a percentage of the 60-cycle volts at the receiving controller

To determine accurately the total 60-cycle $\%IX$ it is often necessary to divide a primary feeder into a number of sections for which the 60-cycle load or current is constant for each section. The 60-cycle $\%IX$ can then be determined for each section. The $\%IX$ to the end of the feeder is the sum of the $\%IX$ for each section traversed in reaching the end of the feeder. Adding the distribution transformer and secondary feeder $\%IX$ gives the total $\%IX$ to reach the controller.

Feeder 720-Cycle Input Volts Required

Substituting in equation 3 the minimum required value of E_r, and for $\%IX$ a value applying to the last controller (or customer) on the last feeder distribution transformer gives the minimum E_f necessary to provide control at all points on the feeder. If the 60-cycle $\%IX$ for the last controller is 15 per cent, the 720-cycle voltage relations are as shown in Figure 2. Figure 3 shows the minimum E_f required for other values of 60-cycle $\%IX$.

E_f is easier to calculate than is the actual feeder 60-cycle voltage drop. For the usual overhead feeder circuits, X does not vary much with conductor size or spacing. Thus E_f depends chiefly on the feeder 60-cycle characteristics of load and distance, while the 60-cycle voltage drop depends chiefly on load, distance, conductor size, and load power factor.

Figure 4 shows values of 60-cycle $\%IX$ for one per cent 60-cycle volts drop and for various 60-cycle load power factors and values of line X/R. Since average feeder load power factors are well below 80 per cent, the curves show that $\%IX$ changes only slightly for a given X/R for power factors encountered. Values of X and R are given in standard-wire tables.[1] Values of X/R range from approximately 0.35 to 2.5 for the usual overhead feeder conductor spacings and for copper-wire sizes from number 6 to 0000, respectively. For this range of X/R and a 50 to 80 per cent power-factor range, Figure 4 shows that $\%IX$ varies from 0.35 to 1.08. For the largest wire size 0000 and 70 per cent power factor, the 60-cycle $\%IX$ is approximately equal to 60-cycle per cent volts drop.

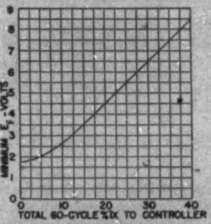


Figure 3. Feeder input volts as function of the 60-cycle %*IX* between the controller and the input point

In view of the preceding, if data are available on the 60-cycle voltage drop, Figure 4 is used to determine the total $\%IX$ and thereby the required feeder 720-cycle volts from Figure 3.

If the 60-cycle drop is unknown, it usually can be estimated based on the maximum voltage drop that can be tolerated without getting unsatisfactory 60-cycle voltage at the last customer. Power-factor correction capacitors should be disconnected and voltage regulator blocked in determining the 60-cycle voltage drop.

Even though different wire sizes are used on different sections of the feeder, it is usually possible to estimate an average value of X/R which is weighted for the relative amount of voltage drop in each section. The value obtained from Figure 4 (for this average value of X/R), when multiplied by the total 60-cycle per cent volts drop then gives the total 60-cycle $\%IX$. If the average value of X/R taken corresponds to the largest value for any section of the feeder, the solution becomes very simple, although in some cases this assumption may lead to the application of larger 720-cycle transmitter equipment than is actually required.

If data are not available on 60-cycle voltage drop, the 60-cycle $\%IX$ can be calculated for each section of the feeders using equation 4.

$$\text{Feeder 60-cycle } \%IX = \frac{(\text{kva})\, dX}{10(\text{kv})^2} \qquad (4)$$

Feeder 720-Cycle Power Input Required

For feeders previously defined as short lines the feeder 720-cycle kilowatt and kilovolt-ampere input depends on the following factors:

1. The 720-cycle-feeder volts input.
2. The 60-cycle-feeder load.
3. The 60-cycle $\%IX$ for each section of the feeder.

The curves of Figure 5 are based on five volts input on a 115-volt 60-cycle base. This voltage is more than is required for most feeders but has been used because the standardized series-coupling arrangements provide five volts in series with the feeder. The actual feeder voltage varies from five volts to some lower value which depends on the application conditions. Since power varies as the square of the voltage, each value from Figure 5 can be corrected for any other voltage.

If the same 720-cycle voltage (on a 115-volt 60-cycle base) is impressed on feeders having the same 60-cycle per cent reactance, the same 720-cycle power input

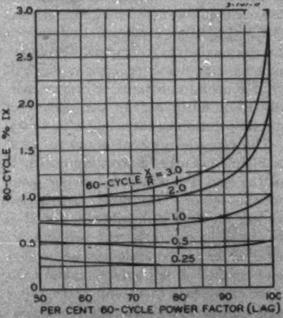


Figure 4. %*IX* based on one per cent 60-cycle volts drop as function of load power factor and feeder *X/R* ratio

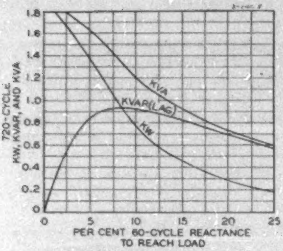


Figure 5. The 720-cycle power input with five volts (on a 115-volt 60-cycle base) impressed on a feeder with 1,000-kva concentrated 60-cycle lighting load

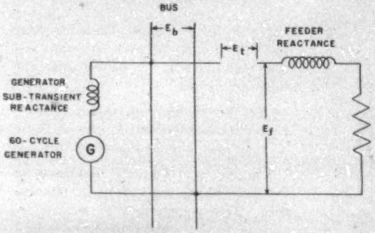


Figure 6. The 720-cycle voltages superimposed on a 60-cycle system using series coupling

Shown single phase for simplicity

per 1,000 kva of 60-cycle load is required for each feeder. Values obtained from Figure 5 can be prorated in direct proportion to 60-cycle kilovolt-ampere rating to cover larger or smaller systems.

As explained later values from Figure 5 do not include additional power losses occurring in the power system because of 720-cycle current flow through the generators and through irrelevant loads which usually parallel the generators. Also allowance must be made for losses in the 720-cycle coupling apparatus, which are usually made fairly high in the interest of stable operation and reasonable first cost.

The 60-cycle per cent reactance used in Figure 5 can be determined readily if all the load is concentrated at one point on the feeder. On most distribution feeders the load is distributed, and it is necessary to determine the per cent reactance for an equivalent concentrated load circuit. A fair approximation is to move all the load to a point on the feeder where the same 60-cycle power loss is obtained. Frequently, there is no load up to a certain point, and beyond this point the load is uniformly distributed. The same 60-cycle power loss is then obtained if all the load is concentrated at a point one-third the distance from the point to the end of the feeder. For this case the per cent reactance can be obtained from the 60-cycle power factor and 60-cycle voltage drop data using equation 5.

Equivalent 60-cycle per cent reactance

$$= \%IX[V_{d1}+{}^1/_3(V_{d2}-V_{d1})] \qquad (5)$$
$$= \%IX({}^2/_3V_{d1}+{}^1/_3V_{d2})$$

where

$\%IX$ = value of 60-cycle $\%IX$ (from Figure 4)
V_{d1} = 60-cycle per cent volts drop to the first customer on the feeder
V_{d2} = 60-cycle per cent volts drop to the last customer on the feeder

When the 720-cycle voltage is simultaneously impressed on two or more feeders the 60-cycle feeder load kilovolt-amperes used to enter Figure 5 is the sum of the loads on all these feeders and the per cent reactance for the equivalent concentrated load circuit is also expressed on this base.

Distribution-Feeder Characteristics Based on Operating Experience

Since the 60-cycle characteristics of feeders are subject to wide variations, it is evident that the 720-cycle feeder characteristics are also subject to variations. However, operating experience on a large number of installations indicates that the 720-cycle variations are not as wide as might be expected from the aforementioned considerations. In many applications the 720-cycle energy is superimposed so that it flows toward the load in the same direction as the 60-cycle power. Also only one transformation is usually involved to reach any controller, and all of the assumptions in the preceding paragraphs numbered 1 to 5 apply with negligible exceptions. Thus operating experience leads to the following conclusions:

1. A 720-cycle voltage of four volts (on a 115-volt 60-cycle base) is adequate to provide complete control at all points on any feeder.

2. A 720-cycle voltage of as little as three volts is adequate if the system characteristics are favorable or if control is only required at favorable points such as at points near the substation or near the end of the primary feeder but not at the ends of the secondary circuits.

3. For determining feeder 720-cycle power input under peak-load conditions as previously described it is usually conservative to consider the total load concentrated in series with a 60-cycle reactance of ten per cent on the feeder load base.

Schematic Diagrams for Series and Shunt Coupling

The essential differences between the series- and shunt-coupling arrangements are shown in Figures 6 and 7. It is understood that generator reactances shown include reactance in the intervening transmission system if the 60-cycle generator is remote from the point at which the 720-cycle coupling is made.

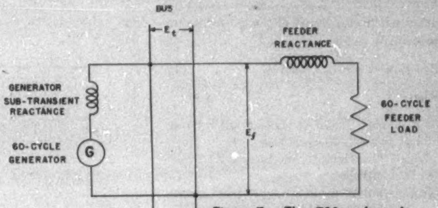


Figure 7. The 720-cycle voltages superimposed on a 60-cycle system using shunt coupling

Both series- and shunt-coupling arrangements include circuits (not shown) which prevent interference with the normal flow of 60-cycle current and prevent appreciable flow of 60-cycle current through the source of 720-cycle voltage. The 720-cycle transmitted voltage E_t is shown as being introduced directly on the 60-cycle system to simplify these pictorial explanations of the 720-cycle operation.

Theory of Operation of Series-Coupling Arrangement

In the series arrangement of Figure 6, the 720-cycle voltage E_t is introduced in series with the load on the feeder over which control is required. The 720-cycle current flows through the complete series circuit as illustrated. The relations between the voltage are:

$$E_f = E_t - E_b \qquad (6)$$

$$E_b = E_t - E_f \qquad (7)$$

The 720-cycle impedance of a feeder of given 60-cycle voltage and per cent re-

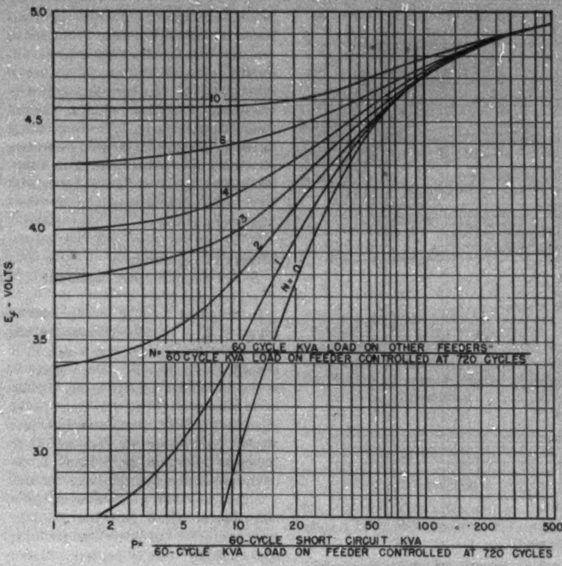


Figure 8. Feeder input volts obtained with five volts in series with a feeder for 60-cycle values of load on the feeder, load on other feeders, and short-circuit capacity at the coupling location

Each feeder has concentrated lighting load and ten per cent reactance on its own base

actance is inversely proportional to the feeder 60-cycle load kilovolt-amperes. The generator 720-cycle impedance depends almost entirely on the generator 60-cycle subtransient reactance which also determines the generator 60-cycle rms symmetrical short-circuit kilovolt-amperes. Therefore the generator 720-cycle impedance is inversely proportional to the generator 60-cycle short-circuit kilovolt-amperes. Hence, it follows that, if the 60-cycle ratio of generator short-circuit kilovolt-amperes to feeder load kilovolt-amperes is large, which is usually the case, the series coupling arrangement is highly efficient.

Values of E_f, when E_t equals five volts, are plotted in Figure 8. If carrier is coupled simultaneously to all feeders on the bus, as in Figure 6, the load on other feeders is zero, and the curve for $N=0$ gives the applicable values of E_f. It is seen that if the short-circuit kilovolt-amperes is at least 26 times the load on the feeder over which control is required, the feeder 720-cycle voltage is four volts or more. When the bus is fed by a single generator (or transformer bank) the minimum value of E_f is obtained under feeder peak-load conditions since the lowest ratio of short-circuit kilovolt-amperes to load kilovolt-amperes is then obtained. If the same bus is fed by two or more generators and some may be out of service during light loads, the ratio of generator short-circuit kilovolt-amperes to feeder load kilovolt-amperes is least for the operating condition under which the generators in service carry the maximum 60-cycle load in per cent of their rating. Since the generators usually carry the highest per cent load under bus peak-load conditions the minimum value of E_f is usually obtained during station peak-load conditions but in exceptional cases may occur under light-load conditions.

When other feeder loads are connected across the bus, the value of E_f is increased because this load is in parallel with the generator reactance, and the resultant 720-cycle impedance of the bus is decreased. Figure 9 shows Figure 6 modified to include other feeders on the same bus. In Figure 8, E_f is plotted for values of N from 1 to 10 applicable to the circuit of Figure 9. To visualize the significance of these curves, it is convenient to consider that all feeders have the same 60-cycle load, since N is then equal to the number of the other feeders on the bus. Note that if P is constant, E_f increases as N increases or as more feeders are added. In practice, P usually increases as N increases because larger generator (or station transformer) capacity is required to carry the increased load. With $N=4$ (that is, a total of five equally loaded feeders) a value of four volts is obtained for E_f even if the short-circuit kilovolt-amperes is zero.

It is fairly typical in substation layouts to have four feeders of equal kilovolt-amperes rating present. For this case $N=3$; however, if it is assumed that the maximum load on the feeder over which control is required is 50 per cent greater than the average load on the other feeders, a value of $N=2$ is obtained. From Figure 8 it is seen that for $N=2$ and $E_f=4$, the 60-cycle short-circuit kilovolt-amperes must be 16 times the kilovolt-ampere load on the most heavily loaded feeder. If the short-circuit kilovolt-amperes is higher than this, E_f is higher, and more efficient operation is obtained. The minimum value of E_f for Figure 9 is almost always obtained during the station peak load.

Values of E_f obtained from Figure 8 should at least equal the 720-cycle input volts previously determined necessary to provide adequate 720-cycle control over the actual feeders.

The arrangement of Figure 9 is used frequently for sequence coupling in which carrier is transmitted in sequence through coupling transformers in each of two or more feeders. The 720-cycle voltage is then impressed in succession on each feeder or group of feeders. In this case the maximum value of E_b in Figure 9

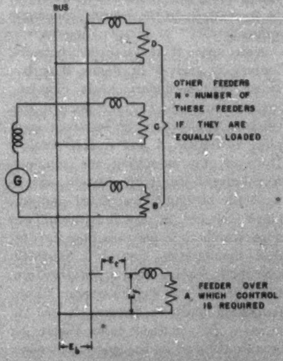

BUS
D
C
B
A
G
OTHER FEEDERS
N = NUMBER OF THESE FEEDERS IF THEY ARE EQUALLY LOADED
E_t
E_f
FEEDER OVER WHICH CONTROL IS REQUIRED
E_b

Figure 9. The 720-cycle voltages superimposed on a 60-cycle system using series coupling to obtain independent control over one of several feeders

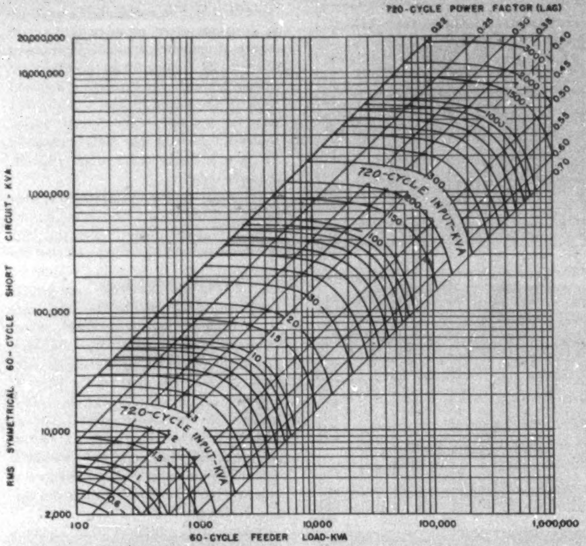


Figure 10. The 720-cycle input kilovolt-amperes and power factor using shunt coupling to superimpose five volts on the system of Figure 11

should not exceed one volt in order to use sequence coupling safely. Thus, using the customary transmitted 720-cycle voltage of five volts, values of E_f obtained from Figure 8 should be four volts or more under all substation operating conditions. Otherwise, a suitable 720-cycle shunt is required across the bus to decrease E_b to the required minimum value.

Note that if a tie exists between feeders *A* and *B* in Figure 9 or between their secondary loads, circulating current in the tie may prevent the efficient application of sequence coupling to feeders *A* and *B* individually. However, by energizing the coupling transformers on both feeders simultaneously at 720 cycles, equal and in-phase voltages E_t are introduced in-series with both feeders simultaneously so that simultaneous control is provided over both feeders even when they are tied together. On a given system this arrangement tends to decrease E_f and to increase E_b obtained from Figure 8, because the load on the feeder controlled at 720 cycles becomes the sum of the load on feeders *A* and *B*, and this decreases the value of *P*.

If the resulting operating flexibility cannot justify the operating time required to operate controllers on all feeders when using feeder sequence coupling as shown in Figure 9, it is feasible to impress 720-cycle voltage on a group of two or more feeders simultaneously. In order to obtain maximum E_f and minimum E_b it is then preferable to make the phase of E_t different for each feeder in each group as follows:

A. If two radial feeders are controlled simultaneously, through a simple connection of the coupling transformers the voltage is introduced 180 degrees out of phase on each feeder so that, if the feeder loads are identical, no 720-cycle current flows through the bus, and hence E_b is zero, and E_f is equal to E_t.

B. If three radial feeders are controlled simultaneously, through a simple connection of the coupling transformers equal voltages E_t can be introduced 120 degrees out of phase in each feeder so that, if the feeder loads are identical, no 720-cycle current flows through the bus. In practice the feeder loads may not be identical, but, assuming the maximum possible unbalance, the values of E_f and E_b will not restrict the operation any more than the corresponding limiting values obtained by using individual feeder sequence coupling (that is, energizing the feeders one at a time at 720 cycles).

Here again values of E_f should be compared with those previously determined necessary to provide control over the actual feeder or feeders. If more voltage is required, it can be provided with either of two methods. A 720-cycle shunt can be connected across the feeder bus to decrease the bus impedance at 720 cycles, or coupling transformers capable of providing the necessary 720-cycle voltage can be selected. The choice between these methods is one of economics unless the inevitable increase in the interfering voltage E_b causes interference between controllers on feeders provided with sequence coupling. If so, the former method is mandatory, if the advantages of sequence coupling are retained.

Theory of Operation of Shunt-Coupling Arrangement

In the shunt-coupling arrangement of Figure 7, E_t is impressed across the bus and the voltage across the feeder E_f is equal to the voltage E_t. The voltage E_t is also impressed directly across the 60-cycle generator. The 720-cycle power input to the feeder was determined previously (see Figure 5). The generator 720-cycle kilovolt-ampere input is directly proportional to the generator rms symmetrical 60-cycle short-circuit kilovolt-amperes. Tests show that the 720-cycle power factor of 60-cycle generators is approximately 15 to 20 per cent. Hence if the generators are directly connected to the bus, approximately 20 per cent of the 720-cycle kilovolt-ampere input to the generators represents actual kilowatts.

Using the shunt-coupling arrangement under the conditions assumed for Figures 10 and 11, the total 720-cycle input kilovolt-amperes and power factor can be determined directly if values of 60-cycle short-circuit kilovolt-amperes and feeder load kilovolt-amperes are known. Since carrier control is usually required under all load conditions, the 720-cycle input is usually determined for a feeder load based on peak-load conditions and for a short-circuit capacity based on all generators in service under peak-load conditions, making a reasonable allowance for future increases in these values because of system growth.

Values obtained from Figure 10 are based on five volts, 720 cycles on a 115-volt 60-cycle base but may be corrected for any other 720-cycle voltage, since the 720-cycle power factor is constant and the

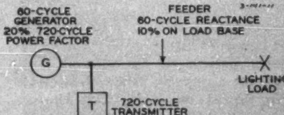

60-CYCLE GENERATOR 20% 720-CYCLE POWER FACTOR
FEEDER 60-CYCLE REACTANCE 10% ON LOAD BASE
G
LIGHTING LOAD
T
720-CYCLE TRANSMITTER

Figure 11. Diagram on which Figure 10 is based

720-cycle kilovolt-amperes varies directly as the square of the voltage.

If the generators are located at a remote point on the system, the 720-cycle kilovolt-ampere input toward the generators is still limited by the same reactance which limits 60-cycle short-circuit kilovolt-amperes, and reasonably accurate values can be obtained from Figure 10, providing 720-cycle standing-wave effects on the transmission line (or lines) to the generators can be neglected. When the transmission-line reactance has a negligible effect on the 60-cycle short-circuit kilovolt-amperes the error attributable to neglecting standing-wave effects can be ignored. Even if the transmission-line reactance is not negligible in determining 60-cycle short-circuit kilovolt-amperes, reasonable accuracy can be obtained neglecting standing-wave effects for line distances to the generators up to 32 miles (corresponding to one-eighth wave length at 720 cycles). For line distances in excess of 32 miles the values obtained from Figure 10 are subject to errors which vary with the system characteristics.

The two tuning schemes used with shunt-coupled transmitters are usually designated as constant-voltage tuning and maximum-current tuning. To use the coupling equipment for protection against interference from a neighboring interconnected 720-cycle transmitter, constant-voltage tuning must be used. (See "Interference Between Two or More Interconnected Transmitters on the Same Power System.") Otherwise, the tuning scheme requiring the least 720-cycle kilovolt-amperes rating is used. This is usually the maximum-current tuning scheme.

Constant-Voltage Tuning Scheme

If this scheme is used, the 720-cycle power source (converter) and coupling circuit are tuned so that no over-all reactive voltage drop occurs between the point at which the 720-cycle voltage is generated in the converter and the 60-cycle bus of the system. The 720-cycle voltage on the 60-cycle bus is essentially independent of the system 60-cycle values of feeder load kilovolt-amperes or short-circuit kilovolt-amperes. The 720-cycle converter furnishes the 720-cycle kilovolt-ampere input to the 60-cycle system plus the kilowatts coupling loss.

Maximum-Current Tuning Scheme

If this scheme is used, the converter and coupling circuit are tuned for maximum 720-cycle current into the 60-cycle bus when the 60-cycle values of feeder load and short-circuit kilovolt-amperes are a maximum.

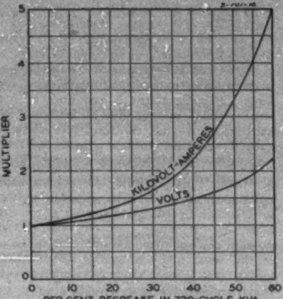


Figure 12. 720-cycle multipliers used for maximum-current shunt-coupling arrangement when system-switching conditions decrease the short-circuit capacity during peak-load conditions

When tuned, the converter furnishes the 720-cycle kilowatts input to the 60-cycle system plus the kilowatts coupling circuit loss. The 720-cycle reactive kilovolt-amperes required by the 60-cycle system is neutralized by a reactive kilovolt-amperes of opposite phase provided by the coupling circuit. With this scheme the over-all transmitter circuit becomes detuned under light 60-cycle load conditions, and the 720-cycle volts transmitted decreases. However, since the transmitter output voltage required to operate the controllers under light-load conditions drops more than the decrease in 720-cycle volts transmitted, limiting conditions are still obtained at peak load. But if the 60-cycle generator or transmission-line switching changes during peak-load conditions, the 720-cycle transmitted voltage drops because of detuning, and it is necessary to multiply values of 720-cycle kilovolt-amperes and volts by the multipliers obtained from Figure 12.

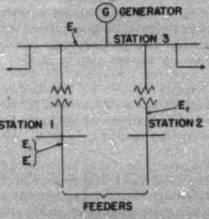


Figure 13. Voltages introduced on power system by transmitters at two stations

Interference Between Two or More 720-Cycle Transmitters on the Same Power System

Assume that transmitters are applied to provide independent control over the two feeders out of stations 1 and 2 in Figure 13. When transmitting separately station 1 and 2 transmitters impress voltages of E_1 and E_2, respectively, as shown. E_1', the interfering voltage on station 1 feeder produced by station 2 transmitter on station 1 feeder, should not exceed one volt; otherwise, incorrect operation may result.

If both transmitters are operated simultaneously and synchronized in phase, the total voltage on the station 1 feeder is E_1+E_1'. If both transmitters are synchronized 180 degrees out of phase the resulting voltage is E_1-E_1'. In general, the transmitters tend to synchronize but, because of the high reactance in the interconnecting ties, the operation is unstable, and the transmitters drift in and out of synchronism, thereby producing "beats" at a low frequency of between zero and five cycles per second. Under the condition of 180 degrees out-of-phase operation, station 2 transmitter has the effect of decreasing the voltage provided by station 1 transmitter from E_1 to E_1-E_1'. If the transmitter at station 1 is arranged to provide independent control over more than one feeder out of the station using series sequence coupling, then the value of E_1+E_1' should not exceed one volt.

The following analysis is usually employed to determine the magnitude of the interfering voltage such as produced by a transmitter at station 2. The interfering voltage is caused by a 720-cycle current flowing through the transformer bank and the line to station 3. At this point it may be considered that the current produces a voltage E_3 across an impedance combining the 720-cycle impedances of the generators and the outgoing lines terminated in the other substation loads. This voltage is then transmitted over the outgoing lines to reach the other substations. The first controller at each substation receives the maximum interfering voltage, but this is less than E_3 because of the voltage drop in transmission.

For estimating purposes E_1' is obtained from equation 8 which is based on the following assumptions, both of which tend to indicate a somewhat higher interfering voltage than would actually be obtained:

1. All the 720-cycle current from station 2 transmitter flows through the generators at station 3.

2. There is no 720-cycle voltage drop from station 3 to the first controller at station 1.

$$E_1' = E_3 = \frac{E_2 \,(\text{station 2 60-cycle short-circuit kva})}{\text{station 3 60-cycle short-circuit kva}} \quad (8)$$

Theoretically, intertransmitter interference becomes more pronounced on an interconnected system as the ratio of the total number of transmitters to the total number of substations increases, since the total potential interfering voltage becomes the sum of the interfering voltages produced by each transmitter operating alone. In practice, however, it is usually feasible to arrange the coupling circuits to avoid difficulty of this kind.

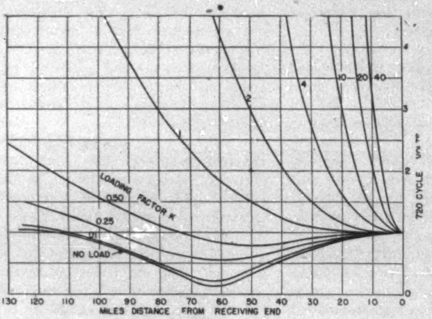


Figure 14. 720-cycle volts along uniformly loaded number-2 Brown and Sharpe overhead line with one volt at receiving end

As an example in the previous comparison of series and shunt coupling, it was shown that the voltage across the bus to which the feeder is connected is inherently lower using series coupling; therefore intertransmitter interfering voltages are lower. The relative interfering voltages produced vary greatly with the application conditions, but studies made of actual systems permit deducing some general conclusions:

1. Using series sequence coupling to control feeders individually at each substation, the intertransmitter interference in this type of application is almost negligible. By applying transmitters at each station so that each transmitter produces no objectionable interference over its own feeders, the slight additional interference caused by simultaneous operation of transmitters is usually acceptable.

2. Using series bus coupling in which each transmitter provides control over an entire bus or substation an intertransmitter interference problem may arise if two or more simultaneously operated transmitters provide control over 30 per cent or more of the total 60-cycle system load. However, this interference may be prevented effectively by the use of a 720-cycle shunt connected at some central point on the system (such as across station 3 bus in Figure 13) or by the use of 720-cycle shunts connected across the bus at some of the substations where transmitters are located. If this type of coupling is used at all substations on the system, to prevent intertransmitter interference one large 720-cycle shunt is required, or else smaller shunts are required at enough of the substations so that the total 60-cycle load capacity at these stations is at least 50 per cent of the total 60-cycle system peak load.

3. Using shunt coupling for control over individual substations, an intertransmitter interference problem may arise if two or more simultaneously operated transmitters provide control over ten per cent or more of the total 60-cycle system load. This means practically that interference preventive measures are almost always required if two or more shunt coupled transmitters are applied on the same system. As an example, if the maximum-current tuning arrangement of shunt coupling is selected to permit use of a smaller converter, the coupling circuit tends to act as a 720-cycle shunt drawing a leading current which increases the interfering voltage. In contrast, if constant voltage tuning is used with a resulting larger converter, the use of auxiliary circuits allows the coupling circuit to act as a 720-cycle shunt tuned for minimum 720-cycle impedance which tends to reduce the incoming interfering signal. However, with a strong interfering signal a larger coupling equipment may be required to allow for the additional 720-cycle current that flows through the coupling circuit, and this form of operation makes inadequate provision for preventing interference to other transmitters using series coupling on the same 60-cycle system.

For these reasons, traps are often required in series with the substation transformer bank when shunt coupling is used, in order to reduce the intertransmitter interference to the value often inherently provided by series coupling alone and, as always, provided by series coupling plus a 720-cycle bus shunt.

Shunt coupling is ideal for applications which call for one transmitter to provide control over an entire system which has no interconnections to other systems.

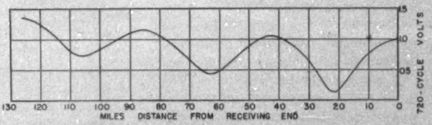


Figure 15. 720-cycle volts along number 2 Brown and Sharpe overhead line at no load with one volt at receiving end

Conductor capacitance increased to show effect of many short tap lines

Effect of Capacitance and Long Lines

720-Cycle Voltage Distribution Along Long Lines at No Load

On long lines the effect of capacitance and reflection must be considered. Figure 14 shows the variations in 720-cycle voltage along an overhead line with one volt at the receiving end for various loading factors. The scale of distance is taken from the receiving end because the 720-cycle voltage at any point is the resultant voltage of a direct voltage that would exist there if the line were infinitely long, plus a reflected voltage whose magnitude and phase are dependent on the distance from the point to the actual receiving end of the line.

For the curve marked "no load," the only line load present is that due to line capacitance and leakage. If the 720-cycle transmitter is located 31 miles (one-eighth wave length) distant from the receiving or far end of the line, this curve shows that a transmitted voltage of 0.7 volt is required for one volt at the receiving end. At intermediate points the the voltage can be read directly off this curve based on the distance from each point to the receiving end, the general characteristic being a steady rise in voltage from the transmitter all the way to the far end of the line.

Similarly, for a line length of 62 miles and a transmitted voltage of 0.14 volt, the line voltage rises steadily to one volt at the receiving end. With the line length between 62 and 124 miles a steady voltage drop occurs until an intermediate point is reached beyond which a steady voltage rise occurs up to the receiving end. Thus for a line 124 miles long and a transmitted voltage of 1.05 volts, the voltage falls to 0.14 volt at the center of the line and then rises to one volt at the far end.

The wave length of the line is not appreciably affected, and the general shape of the no-load curve is the same for a considerable range of overhead conductor spacings and sizes. Assuming the line leakage is constant, the minimum voltage point of 0.14 volt for the number 2 Brown and Sharpe conductor becomes, respectively, 0.5 volt for number 10, or 0.08 volt for 4/0 conductor. If 1.05 volts is transmitted over a 125-mile 4/0 line, the voltage at the center of the line is reduced to 0.08 volt because of reflection or so-called standing-wave effects. Hence, to provide the required minimum of 1.75 volts 720 cycles at all points on the line, the transmitted voltage required on the same 115-volt 60-cycle base is 23 volts, without any allowance for distribution transformer or secondary voltage drop. This voltage corresponds to 20 per cent of the 60-cycle line voltage and would be impractical to apply for a number of reasons. A somewhat more practical solution would involve terminating the line in a 720-cycle shunt having an impedance equal to the characteristic line impedance (approximately 300 ohms resistance based on one conductor to neutral). The result would then be a small but continuous voltage drop to the receiving end of the line. However, this solution rarely is required on long distribution feeders for reasons outlined in the following paragraphs.

720-Cycle Voltage Distribution Along Long Distribution Feeders

On distribution feeders there is almost always some 60-cycle load present, and even at "no load" the 720-cycle controllers alone impose some load as previously explained. However, it is feasible to use the different curves in Figure 14, when the load approximates a uniform distribution so that loading factor K can be computed from equation 9.

$$K = \frac{\text{kva}}{(\text{kv})^2} \qquad (9)$$

where

kva = three-phase 60-cycle load kilovolt-amperes per mile (for single-phase feeders the per-mile per-conductor load impedance is reduced 50 per cent, and twice the actual load kilovolt-amperes per mile is then used in equation 9)

kv = 60-cycle line-to-line kilovolts

For convenience in visualizing the significance of the curves in Figure 14, assume that the feeder is a one-kilovolt three-phase 60-cycle feeder. Values of loading factor then are equal to the actual 60-cycle load kilovolt-amperes per mile. When a three-phase 60-cycle load of 0.1 kilovolt-ampere per mile is added to a 125 mile unloaded number 2 Brown and Sharpe three-phase feeder the voltage at the center of the feeder increases from 0.14 to 0.23 volt, while requiring only a slight increase in transmitted volts. For a 60-cycle load of one kilovolt-ampere per mile the standing-wave effects are almost negligible, regardless of the feeder length. The maximum error in neglecting completely the reflected voltage at a point 40 miles from the receiving end of the line where the greatest error is obtained is only 15 per cent.

Feeders on which standing-wave effects are prominent only at light load present a simple application problem, provided transmission is only required at heavy or medium load, such as for off-peak load control. Street or highway lights are seldom controlled at periods of minimum load because of the power-demand characteristics of rural customers.

Another factor influencing the 720-cycle voltage distribution along long feeders is the presence of tap lines. Frequently long lightly loaded lines with many taps are encountered in rural distribution. The taps are generally less than one-eighth wave length (31 miles) long so it is unlikely that reflection from the end of an unloaded tap may cause a minimum voltage point along the tap, or that an unloaded tap line one-quarter wave length long may have a short-circuiting effect on the main line.

Very short tap lines can be represented electrically by a lumped capacitance connected across the main feeder. Figure 15 is based on a line having eight miles of very short tap lines for each mile of line. Since the line capacitance has been increased nine times by adding the tap lines, we might expect that the wave length, which is dependent inversely on LC, would be decreased to one third its former value, and a comparison of Figure 15 with the no-load curve in Figure 14 indicates that this is exactly what happens. The low-voltage point of 0.14 volt at a distance of 21 miles is exactly the same as for the corresponding point at 63 miles on the no-load curve of Figure 14.

In practice tap lines are rarely ever so short that they do not improve the voltage distribution along a long line. The voltage along the line is the resultant of a direct voltage received directly from the transmitter and the voltages reflected from the ends of the main line and each tap line. The reflected voltages are often received out of phase because of the different lengths of the paths traversed. Therefore, if the line is long enough to have one or more points of minimum voltage, the minimum voltage is higher than for a line of the same wave length with no taps. In other words, tap lines generally improve the voltage distribution along lightly loaded long lines, and if taps are present in sufficient numbers, 720-cycle standing waves are completely absent.

Cable Distribution Feeders

Because of the reduced conductor reactance and increased capacitance of cable as compared with overhead lines, short cable feeders generally show considerably less 720-cycle voltage drop for the same loading, but the 720-cycle performance generally can be estimated using the methods suggested previously for short overhead lines. However, this disregard of the distributed capacitance generally tends to give pessimistic results regarding 720-cycle voltage drop and transmitted power, and the conclusions may require a larger 720-cycle transmitting equipment than is required actually.

Cables are seldom encountered on rural or other long, lightly loaded lines, but at 720 cycles cable is superior to overhead lines, because its higher attenuation gives it more freedom from standing-wave effects and results in a more uniform voltage distribution.

Effect of Shunt Capacitors at 720 Cycles

The reactance of a capacitor is inversely proportional to frequency. If the frequency is increased from 60 to 720 cycles, the capacitor current increases 12 times for the same voltage. The general effects of capacitors are outlined in the following paragraphs. Where these effects are adverse to the 720-cycle operation, they can always be eliminated by taking corrective measurements at the capacitors. (Refer to "720-Cycle Corrective Measures for Shunt Feeder Capacitors.")

Bus Capacitors

If a bus capacitor is present at a substation it usually can be used to assist in the application of a carrier transmitter at the substation. If series coupling is used, it then is generally preferable to install the coupling transformers in the feeder so that the capacitor is on the bus side of the coupling transformers. If Figure 8 is used to determine the required feeder 720-cycle volts, it can be considered that each capacitor 60-cycle kilovolt-ampere at the aforementioned location has the effect at 720 cycles of reducing the bus

60-cycle short-circuit kilovolt-ampere by 144 kva. If the capacitor kilovolt-amperes is sufficient to reduce the apparent short-circuit kilovolt-amperes to zero, this means that the capacitor and the 60-cycle generator (or station transformer) act as a parallel resonant trap circuit of high impedance at 720 cycles. As previously explained, a high bus impedance is undesirable when series coupling is used; but, by using a reactor (having 0.7 of one per cent 60-cycle reactance on the capacitor base) in series with the capacitor, a low impedance 720-cycle bus shunt is obtained which is very beneficial to the 720-cycle operation with series coupling.

If other factors favor the use of shunt coupling at a substation having a bus capacitor, this capacitor usually can be used as a part of of the coupling capacitor assembly.

Primary-Feeder Capacitors

Primary-feeder capacitors generally have the effect of increasing the length of the line. Thus a short line with a large capacitor may have some of the standing-wave effects obtained on long lines without capacitors. It is well known that at 60 cycles the flow of leading current through the line reactance causes the capacitors to give a voltage rise which is fixed and largely independent of the load. If the line reactance is considerably less than the value required for series 720-cycle resonance, the preceding is true also at 720 cycles. Thus, if a 60-cycle voltage rise of 0.2 of one per cent is obtained, the line reactance is increased 12 times, the capacitor reactance is reduced to one twelfth of the 60-cycle value and the per cent voltage rise is increased 144 times at 720 cycles. Hence the 720-cycle voltage rise is approximately 29 per cent under the same conditions.

Under 60-cycle conditions of no feeder load and a line reactance of 0.7 of one per cent on the capacitor base, the line and capacitor reactances are equal and form a series resonant circuit at 720 cycles in which the current flow is limited only by the line-conductor resistance. Hence the capacitor greatly reduces the feeder 720-cycle impedance and causes a rapid rise in voltage along the feeder ending in a high voltage at the capacitor.

Under 60-cycle conditions of no feeder load and a line reactance greater than 0.7 of one per cent on the capacitor base, the line reactance is greater than the capacitor reactance at 720 cycles and the feeder 720-cycle input current becomes lagging. Starting at the substation and moving toward the capacitor, the feeder 720-cycle voltage then gradually decreases until a low-voltage point is reached at some intermediate point and then gradually rises to some higher value at the capacitor. The lowest voltage is obtained at a point where the line reactance from the point to the capacitor form a series resonant circuit at 720 cycles with the capacitor reactance.

Under full-load conditions the load usually prevents excessive voltage rise or pronounced resonance effects, but under light-load conditions these effects must be considered. Under some conditions, the presence of feeder capacitors may require more 720-cycle power input under light-load conditions than under heavy loads.

Where it is feasible to control the size and location of capacitors, successful 720-cycle operation often can be obtained in spite of large primary feeder capacitors without taking corrective measures at the capacitors. Many feeders take the form of a line running to a load center with the load distributed in a circle about the load center. On a feeder of this type the operation of 720-cycle controllers is not required between the station and the load center, so the 720-cycle voltage distribution is unimportant along this part of the circuit. If the capacitor is located between the station and the load center so that a 60-cycle voltage rise of approximately 1.4 per cent is obtained at the capacitor, the 720-cycle capacitor reactance is one-half the line reactance from the station to the capacitor, and the 720-cycle volts received at the capacitor is equal to the feeder 720-cycle input volts. The 720-cycle voltage distribution beyond the capacitor would then be the same as on a similar feeder without a capacitor with no drop in voltage to the load center. For a 4,000-volt three-phase feeder these conditions would exist for a three-phase capacitor of 400 kva at a distance from the station of approximately one mile, or 200 kva at a distance of approximately two miles.

Note that Figures 5, 8, and 10 do not apply if feeder capacitors present are not equipped with suitable reactors, unless the conditions outlined heretofore make negligible their effect on the 720-cycle characteristics of the feeder. If the 720-cycle application is made without reactors for the capacitors present, the 720-cycle feeder voltage, and power required, and the choice between series and shunt coupling should be based on a careful analysis of the 720-cycle impedance diagram of the 60-cycle system.

Secondary-Feeder Capacitors

Some distinction can be made between secondary-feeder capacitors depending on whether control is or is not required on the secondary feeder on which the capacitor is located. If control is not required, it is only necessary to consider the conditions under which the distribution-transformer reactance, capacitor reactance, and secondary load may combine to affect adversely the 720-cycle voltage at other points on the primary feeder. If the capacitor is large enough to short-circuit essentially the transformer at 720 cycles, the transformer reactance usually prevents appreciable adverse effect on the voltage on the primary feeder. Troubles with this setup are relatively infrequent, since the circuit constants usually must be near series resonance before the 720-cycle voltage is affected. If control is required over the secondary feeder on which the capacitor is located, the capacitor may require 720-cycle correction, if it causes either series resonance or low voltage because of short-circuiting effect.

720-Cycle Corrective Measures for Shunt-Feeder Capacitors

The preferred methods of eliminating adverse 720-cycle effects of shunt capacitors involve the use of reactors in series with the capacitors. One method is to use a reactor of approximately five per cent 60-cycle reactance on the capacitor base in every capacitor. The reactor increases the capacitor voltage five per cent, the capacitor kilovolt-amperes is increased ten per cent, and the net correction kilovolt-amperes is increased five per cent at 60 cycles, which may require that the capacitor be designed for a slightly higher 60-cycle voltage. The capacitor-reactor combination acts as a reactor at 720 cycles. The size of the reactor required and the fact that the 720-cycle transmitter has to furnish reactive vars absorbed in the reactor make this method relatively undesirable.

A series reactor having a 60-cycle reactance of 0.7 of one per cent on the capacitor base has a reactance equal and opposite to the capacitor at 720 cycles. If a reactor of twice this reactance (1.4 per cent at 60 cycles) is connected in series with the capacitor, the combination acts as a reactor whose reactance is equal to the reactance of the capacitor but opposite in sign. Hence the combination will neutralize or correct the 720-cycle power factor of another equal capacitor at the same location. Thus, if 1.4 per cent 60-cycle reactors are installed in one-half the capacitors, the sum of the 720-cycle current flowing through all capacitors is zero. Preferably, reactors should be furnished for one half of the capacitors at each capacitor bank. Where this is not feasible, capacitors with and without reactors need not be at the same location but the distance between locations must receive some consideration because of the circulating 720-cycle current which must flow in this path.

In some cases the preceding arrangement can be used to improve the 720-cycle feeder voltage distribution. By using tapped reactors the 720-cycle current through the reactor can be adjusted to obtain a resulting leading current through all capacitors and thus obtain a desirable 720-cycle voltage rise which will compensate for the voltage drop because of the feeder load and thus permit the use of smaller transmitter coupling and converter equipment.

In short, any capacitor arrangement that improves 60-cycle operation can be used to also improve 720-cycle operation.

Appendix

In order to illustrate the use of the equations and curves in typical 720-cycle control applications, the following examples are given.

Example 1

The 4,000-volt feeder circuit shown in Figure 16 is given. The three-phase 720-cycle voltage and power required at the substation end of the feeder to provide 720-cycle control at all points on the 115- or 230-volt secondary circuits is determined as follows:

The 60-cycle $\%IX$ may be computed for peak-load conditions for each section of line using equation 4. For example, use section 3 in Figure 16.

kva = 500
$d = 1$ mile
$X = 0.762$ ohm per mile
kv = 4.0

$$\text{60-cycle } \%IX = \frac{500\times1\times0.762}{10\times4^2} = 2.4 \text{ per cent}$$

Similar values for each section of the line are shown in Table I.

Note that voltage regulators have the small value of approximately one-half $\%IX$ in maximum boost position, but they provide enough boost in 720-cycle voltage so that they can be ignored conservatively.

The last transformer on this feeder is obviously at the far end of section 4. It is assumed that the last customer is fed from a distribution transformer having 2.6 $\%IX$ at the peak load through a secondary feeder having three $\%IX$. The latter value is often difficult to determine accurately, but a reasonable error in its determination usually will have little effect on the over-all application.

Table I

Section	Kva Load	X Ohms Per Mile	60-Cycle %IX
Regulator	1,000		0
1	1,000	0.712	5.6
2	200	0.762	1.0
3	500	0.762	2.4
4	200	0.762	1.0

The total 60-cycle $\%IX$ from the substation bus to the last customer is the sum of the $\%IX$ values for each part of the circuit traversed so that:

Total 60-cycle $\%IX = 5.6+2.4+1+2.6+3 = 14.6$ per cent

From Figure 3 it is seen that, for a value of 14.6 $\%IX$, the minimum 720-cycle feeder input volts necessary to provide complete 720-cycle control is:

Minimum $E_f = 3.5$ volts on 115-volt 60-cycle base

$$= 3.5\times\frac{4{,}000}{115} = 121 \text{ wye-volts}$$

on 4,000 wye-volt 60-cycle base

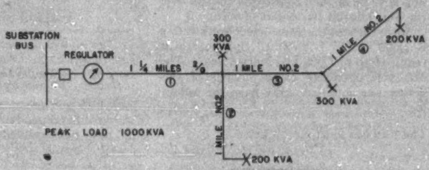


Figure 16. Circuit diagram of 4,000-volt three-phase 60-cycle overhead feeder showing approximate peak-load distribution

It is convenient to obtain the feeder 60-cycle reactance for an equivalent concentrated load circuit using values in Table I.

Equivalent primary-feeder reactance

$$= \frac{1{,}000\times5.6+200\times1+500\times2.4+200\times1}{1{,}000}$$

= 7.2 per cent on 1,000-kva base

Adding the average transformer reactance (estimated at two per cent) and average secondary-feeder reactance (estimated at one per cent) to this value, 10.2 per cent equivalent reactance to the load is obtained. Entering Figure 5 with this reactance and correcting for 3.5 volts required, rather than five volts, the minimum required feeder input is

720-cycle three-phase kilovolt-amperes

$$= 1.18\left(\frac{3.5}{5}\right)^2 = 0.58$$

720-cycle three-phase kilowatts

$$= 0.75\left(\frac{3.5}{5}\right)^2 = 0.37$$

Example 2

Assume that 720-cycle control is desired at all points on the 115- or 230-volt secondary circuits of a 1,200-kva radial feeder which has not been constructed and that no prints are available of the feeder layout or load distribution. Only the following data are known:

(*a*). The average 60-cycle load power factor is 70 per cent lagging.

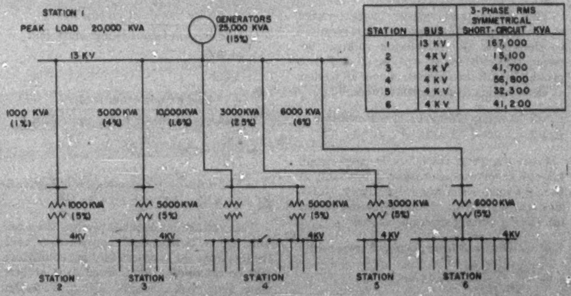
(*b*). 00 primary and 0 secondary conductors are to be used.

(*c*). To hold the customer's 60-cycle voltage within reasonable limits the peak-load 60-cycle voltage drop to the first customer is approximately ten per cent and cannot exceed 15 per cent to the last customer.

From wire-table values of X/R of 1.61 (for 00 conductor) and 0.942 (for 0 conductor) are obtained. Since most of the 60-cycle voltage drop occurs in the primary 00 conductor, it is estimated that an average value of X/R of 1.5 is applicable for the total voltage drop. For a value of X/R of 1.5 at 70 per cent power factor, 0.85 $\%IX$ is

Figure 17. Circuit diagram of three-phase 60-cycle power system

60-cycle per cent reactances shown in parentheses



STATION	BUS	3-PHASE RMS SYMMETRICAL SHORT-CIRCUIT KVA
1	13 KV	167,000
2	4 KV	15,100
3	4 KV	41,700
4	4 KV	56,800
5	4 KV	32,300
6	4 KV	41,200

obtained from Figure 4. Hence the total $\%IX$ to the last customer is

Total 60-cycle $\%IX = 0.85 \times 15 = 12.7$ per cent

The minimum required $E_f = 3.2$ volts (from Figure 3)

Equivalent reactance (equation 5)

$$= \%IX\,(^2/_3 V_{d1} + ^1/_3 V_{d2})$$
$$= 0.85\,(^2/_3 \times 10 + ^1/_3 \times 15)$$
$$= 9.9\% \text{ on 1,200-kva 60-cycle base}$$

Entering Figure 5 with this reactance and correcting for 3.2 volts instead of five volts and for 1,200 instead of 1,000-kva load, the minimum required feeder input is

720-cycle kilovolt-amperes

$$= 1.21\left(\frac{3.2}{5}\right)^2 \times 1.2 = 0.60$$

720-cycle kilowatts

$$= 0.78\left(\frac{3.2}{5}\right)^2 \times 1.2 = 0.38$$

Example 3

The system and system characteristics shown in Figure 17 are given. Series coupling is to be applied to provide control over the four-kilovolt feeder out of station 2.

Since the station-transformer-bank rating is 1,000 kva, it is assumed that control is required up to 1,000-kva station load. The 720-cycle operating characteristics of this system are obtained easily from the following:

The 4-kv bus short-circuit kva = 15,100 kva (from Figure 17)

$$P = \frac{\text{short-circuit kva}}{\text{load kva on feeder being controlled}}$$
$$= \frac{15{,}100}{1{,}000} = 15.1$$

$$N = \frac{\text{load kva on other feeders on same bus}}{\text{load kva on feeder being controlled}}$$
$$= \frac{0}{1{,}000} = 0$$

$E_f = 3.48$ volts (from Figure 8)

Since this voltage is less than the four volts usually considered necessary, the required voltage should be determined as in examples 1 or 2.

Example 4

The system and system characteristics shown in Figure 17 are given. It is required to determine the interfering voltages between transmitters at stations 2 and 3 to provide simultaneous control over all feeders at each station using series bus coupling.

$E_f = 3.48$ volts, 720 cycles (from example 3)

$E_b = 5 - 3.48 = 1.52$ volts on 4-kv bus

From Figure 17 the 60-cycle short-circuit kilovolt-amperes on station 2 bus and at the 13-kv generator are, respectively, 15,100 and 167,000 kva. From equation 8 the interfering voltage does not exceed

Volts at station 3 from station 2 transmitter

$$= \frac{1.52 \times 15{,}100}{167{,}000} = 0.14 \text{ volt}$$

Repeating the previous calculations for station 3 transmitter, it can be shown that

Volts at station 2 from station 3 transmitter = 0.55 volt

If the two transmitters are operated at different times the aforementioned interfering voltages are both less than one volt, and hence no objectionable interference is obtained.

If the two transmitters are operated simultaneously and happen to synchronize 180 degrees out of phase, it can be concluded that:

(*a*). The voltage on the four-kilovolt feeders at station 3 is decreased 0.14 volt by station 2 transmitter.

(*b*). The voltage on the four-kilovolt feeders at station 2 is decreased 0.55 volt by station 3 transmitter.

(*c*). Each transmitter must provide a somewhat higher 720-cycle voltage than would otherwise be required without interference.

Example 5

The system and system characteristics shown in Figure 17 are given. Series sequence coupling is to be applied to provide independent control over two or more four-kilovolt feeders out of station 3.

Table II

Station	Feeder Load Kilovolt-Amperes	N	4-Kv Short-Circuit Kilovolt-Amperes	720-Cycle Volts on 115-Volt 60-Cycle Base		
				4-Kv Feeder	4-Kv Bus	13-Kv Generator
2	1,000	0	15,400	3.51	1.49	0.115
3	1,000	4	43,500	4.60	0.40	0.087
4	1,250	3	60,200	4.51	0.49	0.13
6	1,000	5	42,800	4.62	0.38	0.082

It is first necessary to determine the limiting conditions for use in Figure 8. The most inefficient coupling will be obtained when the 720-cycle voltage is impressed on the most heavily loaded feeder at the peak load and when the least load is present on the other feeders. When the ultimate capacity of the station of 5,000 kva is reached, assuming control is required over any feeder up to its full-rated 1,380-kva load, the total load on the four other feeders is 3,620 kva. On this basis the limiting conditions for use in Figure 8 are:

$$P = \frac{\text{4-kv bus short-circuit kva}}{\text{load kva on feeder being controlled}}$$
$$= \frac{41{,}700}{1{,}380} = 30.2$$

$$N = \frac{\text{load kva on other feeders}}{\text{load kva on feeder being controlled}}$$
$$= \frac{3{,}620}{1{,}380} = 2.6$$

$E_f = 4.33$ volts on 4-kv feeder (from Figure 8)

$E_b = 5 - 4.33 = 0.67$ volt on 4-kv bus

This voltage is considerably less than one volt, hence independent control over each feeder under the assumed limiting conditions will be obtained. Also, since E_f is greater than four volts, ample 720-cycle feeder voltage is available assuming the feeders are of the usual type.

Example 6

The system and system characteristics shown in Figure 17 are given. It is required to determine the interfering voltages produced by transmitters at two or more stations using series sequence feeder coupling at each station.

Following the method used in example 5 it can be shown easily that station 5 transmitter is near the point of having interference between its own feeders. A 720-cycle shunt is required across the station 5 four-kilovolt bus, if additional transmitters are to be operated simultaneously. If this shunt is used, station 5 can be ignored as a transmitter or receiver of interfering voltage. Also this shunt passes some 720-cycle current from transmitters at other stations so as to decrease the current through the 13-kv generators and reduce the interfering voltage. The 720-cycle effect is the same as though the 167,000 short-circuit kilovolt-amperes of the 13-kv generators is increased directly by the 32,300 short-circuit kilovolt-amperes rating of station 5. Hence, for this study, the 13-kv generators can be replaced by a new generator having a 60-cycle short-circuit kilovolt-amperes rating of 199,300 kva and a reactance of 12.5 per cent on a 25,000-kva base.

The four-kilovolt bus short-circuit kilovolt-amperes at station 4 based on the new generator then is

$$\frac{25{,}000 \times 100}{12.5 + \frac{25{,}000}{5{,}000}\left(5 + \frac{1.6 \times 5{,}000}{10{,}000}\right)} = 60{,}200 \text{ kva}$$

If station 4 transmitter is operated under maximum-station-load conditions with equal loads on each feeder the limiting conditions are

$$P = \frac{\text{4-kv bus short-circuit kva}}{\text{load kva on feeder being controlled}}$$
$$= \frac{60{,}200}{1{,}250} = 48$$

$$N = \frac{\text{load kva on other feeders}}{\text{load kva on feeder being controlled}}$$
$$= \frac{3{,}750}{1{,}250} = 3$$

$E_f = 4.51$ volts on 4-kv feeder (from Figure 8)

$E_b = 5 - 4.51 = 0.49$ volt on 4-kv bus

$E_g = 0.49 \times \frac{53{,}800}{199{,}300} = 0.13$ volt on 13-kv generator

Values of E_f and E_b operating any one of the transmitters based on maximum load at each station and feeders equally loaded at each station are tabulated in Table II. Station 5 is omitted because a 720-cycle bus shunt is to be used at this station.

Disregarding station 2 because it has only one feeder, it is apparent that the maximum interfering voltage on any feeder at any station occurs at station 4 which has a four-kilovolt bus voltage of 0.49 volt when its transmitter is operated alone. If the transmitters at stations 2, 3, and 6 are simultaneously operated and synchronized in phase, the resultant voltage does not exceed

$$\text{720-cycle volts on 13-kv generator} = 0.115+0.087+0.082=0.28 \text{ volt}$$

If station 4 transmitter is operated at the same time, the interfering voltage at station 4 under this condition does not exceed

$$0.49+0.28=0.77 \text{ volt}$$

Since the total interfering voltage is less than one volt, no objectionable interference is obtained, even if all transmitters are operated simultaneously. In an actual application available station and feeder load data should be consulted and applied in the preceding calculations to ensure that the interfering 720-cycle voltage has been obtained under the most unfavorable operating conditions.

Example 7

The system and system characteristics shown in Figure 17 are given. A shunt-coupled transmitter is to be applied at station 1 to provide control at stations 2 to 6, inclusive.

In this example the 720-cycle voltage is impressed across station 1 13-kv bus. This arrangement provides operating flexibility in that the four-kilovolt and 13-kv feeders out of the different substations can be interconnected in a primary or low-voltage secondary network without increasing the 720-cycle voltage or power required. If each substation carries rated load, the lowest 720-cycle voltage is obtained at station 6, since the highest 60-cycle $\%IX$ is obtained at this station.

$$\text{60-cycle } \%IX \text{ to station } 6=6+5 =11 \text{ per cent}$$

If it is assumed that the feeder in example 1 is the feeder at station 6 requiring the highest feeder 720-cycle voltage, the maximum 60-cycle $\%IX$ from station 6 four-kilovolt bus to the last controller is 14.6 per cent. Adding this to the foregoing $\%IX$ to station 6 gives 25.6 $\%IX$ from station 6 to the last controller.

$$\text{720-cycle volts at station } 1=5.7 \text{ volts (from Figure 3)}$$

The maximum 720-cycle power will be obtained with 25,000 kva of generators in service at the system peak load of 20,000 kva. At this time the average load at each substation is approximately 75 per cent of the station transformer kilovolt-ampere rating. The 60-cycle per cent reactance to reach the substation four-kilovolt busses varies for each substation, but the maximum and minimum values are

$$\text{Per cent reactance for station 6} =0.75(6+5) =8 \text{ per cent on 4,500-kva base}$$

$$\text{Per cent reactance for station 2} =0.75(1+5) =4.5 \text{ per cent on 750-kva base}$$

For estimating purposes it can be assumed that the per cent reactance to the average substation falls midway between the aforementioned two values so that a value of six per cent is used. Representing each four-kilovolt feeder by a concentrated load with ten per cent series reactance, the total 720-cycle power input in the direction of the loads can be determined, based on 16 per cent reactance on a 20,000-kva base. The curves of Figure 10 are based on ten rather than 16 per cent reactance but may be used for estimating purposes since this assures results on the safe side. (If more accurate results are required, suitable corrections can be determined from Figure 5.) From Figure 17, 20,000 kva (the 13-kv 60-cycle peak load) and 167,000 kva (the station 1 13-kv 60-cycle short-circuit kilovolt-amperes) are the two values used in Figure 10 to obtain 48-kva 720-cycle input at 0.47 power factor, for five volts impressed.

But the peak load can be carried with one 5,000-kva generator out of service. This would decrease the 60-cycle short-circuit kilovolt-amperes 20 per cent to 134,000 kva and the 720-cycle kilovolt-amperes approximately ten per cent to 44 kva (from Figure 10). Thus a voltage multiplier of 1.05 from Figure 12 must be applied. The required three-phase 720-cycle input to the 13-kv bus at station 1 is:

$$\text{Volts on 115-volt 60-cycle base} =5.7\times1.05=6 \text{ volts}$$

$$\text{Volts on 13-kv 60-cycle base} =\frac{6\times13{,}000}{115}=678 \text{ volts}$$

$$\text{Kilovolt-amperes}=48\left(\frac{6}{5}\right)^2=69 \text{ kva}$$

$$\text{Kilowatts}=69\times0.47=32.4 \text{ kw}$$

$$\text{Current}=\frac{69\times1{,}000}{3\times678}=59 \text{ amperes}$$

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A New Control System for Automatic Parallel Operation of Load-Ratio-Control Transformers

Discussion and authors' closure of paper 43-64 by S. Minneci and S. B. Farnham, presented at the AIEE North Eastern District technical meeting, Pittsfield, Mass., April 8–9, 1943, and published in AIEE TRANSACTIONS, 1943, July section, volume 62, pages 488–93.

C. E. Winegartner (Cleveland Electric Illuminating Company, Cleveland, Ohio): This ingenous control system is designed specially to secure the seven characteristics, as stated in the summary of the paper, which are particularly applicable to medium-voltage networks. It might be appropriate to consider some of its limitations and to compare it with electrically interlocked units with duplicate master-control equipment.

It is not practical to set contact-making voltmeters closer than about $\pm 1^1/_4$ volts. Although the circulating current is self-reducing, there will still be, at times, this band of voltage producing it. Also, the meter has the dual function of regulating voltage and the relative tap position of the transformers. This does not provide as positive an interlock as does an odd and even auxiliary switch on the tap-changer mechanism operating through a relay to prevent further automatic operation when units get out of step. In fact, a jammed or welded contact on the meter will cause a transformer to run to its tap limit instead of being blocked as soon as it gets one step out.

Even though the odd- and even-step electrical interlock does not provide for reducing circulating current automatically, it is possible to operate the transformers any multiple of two steps, continuously out of step, to afford some control, as long as the transformers do not run to their limits. In view of the aforementioned limitations on the automatic control, a fixed relation between steps through the electrical interlock will, in some cases, keep circulating currents down as effectively as automatic control.

Duplicate master control has its greatest advantage when two or more transformers are operated in parallel and are located relatively close together. This always affords a spare automatic-control equipment to use when one has to be taken out of service for tests or maintenance. If the current from all the transformers in parallel is totalized, and if this current is used through the duplicate compensators, it is not necessary to change compensator settings when a transformer is taken out of service, and compensation is not affected by circulating current.

To summarize, duplicate master control, at a saving of compensator and auxiliary current transformers for each of two load-ratio-control transformers, and without requiring additional control equipment for additional units, affords spare control equipment for any emergency. While it does not afford automatic control of circulating current, yet where it is practical to have a central control point for all transformers in parallel and where circulating currents do not become excessive, it does have definite merit.

S. Minneci and S. B. Farnham: Mr. Winegartner's discussion is based on his familiarity with an arrangement for parallel operation of regulating equipment in which the units are electrically interlocked so as to operate substantially as one unit; and at any given time all are under the control of one master-control equipment, with another control equipment available as a spare. Although this type of arrangement has been used successfully in a number of applications, it requires the attention of an operator each time a unit is brought into parallel operation with the others, after having been out of service for any reason. The reason for this is that all of the regulating mechanisms must be brought by manual control to the same relative position before the master control is placed in operation. There is no way in which they can be brought into agreement automatically afterwards, and if they fall out of step the effective regulating range of the entire group is reduced by an amount equal to the greatest difference in position between any two units. In addition, there may be, of course, excessive circulating current, and the flexibility of operation is not so great as it is with some other arrangements. In several recent applications for which the electrically interlocked arrangement was being considered, these were among its principal limitations which lead to the development of the new fully automatic-control system described in the paper.

The authors agree with Mr. Winegartner, however, that the arrangement which he describes has certain merits, and may be operated successfully, provided:

(*a*). The transformers are connected directly in parallel on both their high-voltage and low-voltage sides, so as to avoid any differences in the applied voltages such as might be caused by different impedances in the supply circuits.

(*b*). Individual operation is not required.

(*c*). The number of units operated in parallel is not so great as to complicate unduly the control interconnections.

(*d*). There is no objection to having an idle transformer brought by manual control to the same operating position as the active units.

Mr. Winegartner makes the statement that it is not practical to set contact-making voltmeters closer than about $\pm 1^1/_4$ volts, which corresponds to a band width of $2^1/_2$ volts. This is probably a reasonable figure, if the regulator steps are $1^1/_4$ per cent. This corresponds to the rule of thumb to the effect that the voltmeter band width should equal the voltage difference between successive steps, plus one volt. On a 120-volt base, $1^1/_4$ per cent $= 1^1/_2$ volts, which, added to 1 volt, gives $2^1/_2$ volts as the band width, when Mr. Winegartner's figure is checked. However, there are many regulators having steps smaller than $1^1/_4$ per cent, and, consequently, narrower band widths are very frequently encountered. This is particularly true in the case of induction-type regulators, where band-width adjustment need not be influenced by consideration of frequency of operation in order to conserve the life of moving contacts.

It appears that Mr. Winegartner has overlooked the provision that is made in the new control system for using an overcurrent lockout relay, connected so as to respond to circulating current only, for insuring that no regulating mechanism can get more than a predetermined amount out of agreement with the others. His statement, that a jammed or welded contact on the contact-making voltmeter will cause a mechanism to run to its limit, therefore, does not apply to this new control system.

In the comparative evaluation of the two control arrangements, it will be found that the new system not only fulfills all of the functions of the electrically interlocked arrangement described by Mr. Winegartner, but also includes other features for which the electrically interlocked arrangement makes no provision. In addition, it is fully automatic, rather than requiring the services of an operator. These advantages, together with the high degree of flexibility for meeting varied system-operating conditions, result from the inclusion of a complete set of automatic-control equipment in each unit, in preference to placing dependence upon equipment that is located elsewhere. The additional auxiliary current transformer and compensator, required by the new control system, are operated in the secondary circuit of the main current transformer and are very small physically. Their added cost is usually insignificant.

It may be concluded that the new control system will supersede the electrically interlocked arrangement in the majority of applications.

Use of Equivalent Annual Ambient Temperature in Overloading Transformers and Voltage Regulators

Discussion and author's closure of paper 43-65 by M. S. Oldacre, presented at the AIEE North Eastern District technical meeting, Pittsfield, Mass., April 8–9, 1943, and published in AIEE TRANSACTIONS, 1943, July section, pages 477–80.

W. C. Sealey (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The described method for determining the equivalent ambient temperatures has a sound theoretical as well as empirical basis. The values of equivalent annual ambient temperatures listed in the paper are average equivalent ambient temperatures based on an average year.

The author is strictly correct in following the American Standards and making no correction for daily variations in temperature. Such a correction can be made easily if desired. The purpose of making such a correction is to assure the same life base for all localities instead of a varying life base. In practice, it is not feasible to take full advantage of the present recommendations which allow increased loads for a departure from the base of 30 degrees daily average temperature and 40 degrees maximum temperature, since to do so would require daily adjustment of the load carried.

In most cases, where the daily ambient temperature is used for loading, the resultant aging will be less than for full load carried continuously with a constant ambient of 30 degrees. Consequently, it would not be inconsistent to use a constant 30-degree ambient as a reference base for calculating equivalent annual ambient temperatures and a 30-degree average with a maximum of 40 degrees for the daily ambient temperature.

With reference to Figure 3, the difference between the curves K'_{s} and K_{s} represents the temperature correction for range in daily temperatures during the month. This correction is equal to 2.2 degrees centigrade and corresponds to a range in average daily temperature during the month of 27 degrees centigrade, which is a typical value (from Figure 3 of reference 9 of the paper).

This paper and references 8 and 9 of the paper agree in the fundamental principles to be used in calculating equivalent ambient. These three papers differ in their application of the fundamental method, but there is no real inconsistency among them.

For convenience in calculation and analysis, as outlined in these papers, a satisfactory means of determining the equivalent ambient consists of the sum of three factors:

1. A mean temperature for the given period of time.

2. A correction factor based on the range during the given period of mean temperatures for the next smaller unit of time.

3. A correction factor based on the range of temperatures during the next smaller interval of time and so forth.

The base mean temperature is usually selected either for a year or for a month. This base mean temperature may be the average of several years or months, the maximum mean temperature over a long period of time, or some intermediate value.

The typical manner of variation of the temperature during the period of time considered can be determined theoretically, checked by empirical data. The range selected for determining the correction may be an average range for the whole country, an average range for that locality, the maximum range for that locality for the time considered, or some other value which would be typical. For convenience, any value so selected should be determinable readily from the records of the United States Weather Bureau.

The factors mentioned in the preceding paragraph apply, of course, for each succeeding smaller unit of time, until the hourly variation of temperatures during a day is reached. For the variation of temperatures during a day the thermal capacity of the transformer comes into play. For most transformers thermal capacity produces the same effect as does a reduction of the temperature range during one day to 80 per cent of its actual value.

These three papers have used the same fundamental principles to calculate values of equivalent ambient temperature which establish the upper and the lower limits of such equivalent temperatures. Equivalent temperatures based on average temperatures over a long period of time represent the lower limit of equivalent ambient temperature, since an examination of weather reports will show that there can be a succession of several years with monthly average temperatures during the hot months in excess of the long-time average temperature for these months. A succession of as many as ten years, in which the average monthly temperatures during the summer months exceed the long-time average temperature for that month, is not uncommon.

Temperatures derived in reference 9 of the paper are definitely on the safe side and represent the higher limit of equivalent ambient temperatures.

The spread which exists between the higher and lower limits is not unduly great. For the cities compared, the variation is between four and nine degrees centigrade.

Methods readily suggest themselves by which the values obtained in reference 9 can be reduced by taking a less pessimistic view of the probable temperatures which may occur. Similarly, the values given in the present paper could be increased to allow for the difference between the average mean temperatures and the maximum mean temperatures to be expected and, perhaps, in addition, for the daily range in temperature.

There is some advantage in having available equivalent monthly temperatures. If it were decided to use equivalent monthly ambient temperatures, the equivalent monthly ambient temperature could be calculated as the sum of the maximum monthly mean temperature, plus a correction for monthly range, in which the monthly range is assumed to be twice the difference between the maximum temperature ever recorded for that month, and the maximum monthly mean temperature. An alternative suggestion, which will give values somewhat more optimistic, would be to apply a monthly correction equal to the correction for a range equal to the difference between the maximum temperature recorded for the month and the maximum monthly mean temperature. This correction would be applied twice, once as the monthly correction, and once as the daily correction. It would be based on the assumption that one half of the variation was due to variation in hourly temperatures during the day. This correction would tend to be slightly optimistic, because the correction for range would be based on the most favorable conditions which are equal division of the temperature range, but this would be compensated for to some extent by the use of the maximum mean temperature ever recorded for the month.

The establishment of maximum and minimum limits for equivalent annual ambient temperatures with a small spread between them indicates the progress which has been made in the determination of equivalent ambient temperatures for overloading transformers. When the nature of the problem is considered, the agreement obtained between optimistic and pessimistic values should be reassuring to those who plan to use equivalent ambient temperatures for transformer overloading.

R. Rüdenberg (Harvard University, Cambridge, Mass.): From the standpoint of the transformer, it is very important to determine the maximum permissible temperature, or temperature rise, with corresponding limit to which the load may increase without undue deterioration of the insulation. However, if I were a transformer operator during this wartime, I probably would approach the problem from a quite different and much less conservative viewpoint.

Since most of the transformers in our networks have a life expectancy of the order of ten years or more, whereas the war and, thus, the demand for high industrial loads will last probably for a period of five years or less, I would sacrifice quite a number of years of the future life of the transformer by overloading it heavily, if this should be required by the conditions of war industry. I then would sell the power exceeding the well-established rated load of the transformer at a higher price per kilowatt-hour, derived from the more rapid deterioration of the insulation and the corresponding higher rate of depreciation. The increase in cost per kilowatt-hour would be very moderate, and I believe that the war industry would be perfectly willing to pay such extra cost as a return for getting the excess power at all. The additional money that would be received I would put aside until the end of the war, and then with it I would buy a new transformer, thereby solving another problem, namely, giving our transformer factories a good number of orders in the first period after the end of the war, when the returning labor needs it most.

I see no reason to prevent the application of a similar scheme for turbine generators, if their overloading should be seriously required, although the cost balance may not be so favorable here.

C. F. Wagner (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The ultimate purpose of determining the annual equivalent ambient temperature is to permit loading beyond the rating of the transformer when this quantity is below 30 degrees centigrade. It is highly desirable that any rule for this purpose should be as simple as possible. The rules proposed to date, in an effort to obtain accuracy, have suggested procedures that are too complicated, and the accuracy implied is not warranted by fundamental considerations of the problem. In the first place, the eight-degree rule for insulation aging is accepted without question, whereas the data do not justify this

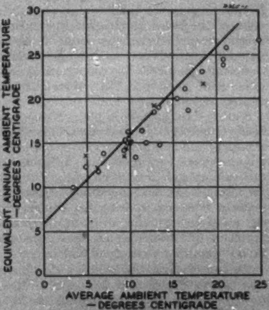

Figure 1. Comparison of Oldacre's equivalent ambient temperatures with proposed rule

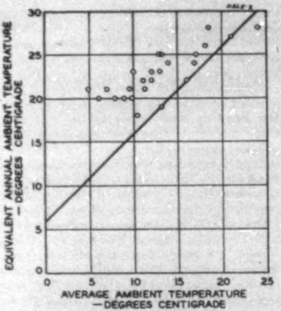

Figure 2. Comparison of Sealey's equivalent ambient temperatures with proposed rule (straight line)

faith. Insulation experts are far from agreement in their views regarding the factors affecting aging as to whether insulation aging follows an eight-degree rule, or a five-degree rule, or does not age appreciably until a certain critical temperature is reached. It is likely that the laws change with the character of the transformer, whether or not oxygen is permitted to come in contact with the oil. Also, the effects of other factors, such as the location of the transformer with respect to reception of direct sunlight, whether it be exposed or protected, are not considered. Further, it is assumed that the loading of the transformer is constant.

In Figure 1 of this discussion the equivalent annual ambient temperatures obtained by Mr. Oldacre are plotted as circles against the average ambient temperatures. The points represented by the crosses were obtained from the data contained in the Hellmund and McAuley[1] paper corrected to normal as indicated in Table III of Oldacre's paper. The writer is proposing that the values given by the straight line be used. This is a simple relation that is obtained merely by adding six degrees to the average ambient temperature. By this proposal the equivalent annual ambient temperature would be equal to the average annual ambient temperature plus six degrees. In view of the indefinite character of the assumptions underlying this work, it is the writer's opinion that the proposed rule is sufficiently accurate for all practical purposes and possesses the important advantage of simplicity.

Similar data have been plotted from Mr. Sealey's paper.[2] These points lie above the suggested curve, because they are based on maximum variations and are probably too conservative. The disparity between the Oldacre and Sealey values of equivalent ambient temperature is further evidence of the unsettled thinking with regard to this problem. It constitutes a further argument for not requiring accuracy of high order.

This proposal is equivalent to assuming that K_s' of Figure 3 of the paper is equal to 6.0. While this may appear to be a rather rough approximation, the proposal should be judged in terms of the total correction to be applied to the rating of the transformer; that is, the accuracy of the equivalent ambient temperature should be judged in terms of the amount by which it lies below 30 degrees centigrade.

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2. EQUIVALENT AMBIENT TEMPERATURES FOR LOADING TRANSFORMERS, **W. C. Sealey.** AIEE TRANSACTIONS, volume 62, 1943, February section, pages 87–93.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): I wish to discuss this paper first from the standpoint of comparing M. S. Oldacre's derived equivalent annual ambient temperatures with those obtained by W. C. Sealey in his paper (reference 9 of M. S. Oldacre's paper).

When the ambient temperatures derived from six cities, Bismarck, Helena, Kansas City, Denver, Dallas, and Chicago, are compared, it will be found that Mr. Oldacre's data permit an average of 7.0 per cent greater overloads than Mr. Sealey's data permit. The difference, of course, is due to the fact that Mr. Sealey was very conservative and used the greatest mean temperature recorded over several years, whereas Mr. Oldacre used annual average values.

We, therefore, have two papers whose authors differ in their ideas of how recorded ambient temperatures should be used in deriving equivalent annual ambient temperatures. Both are right, depending on the degree of conservatism one wishes to use.

As I see the situation, we are supposed to take full advantage of any and all factors during the war either to conserve critical materials or to get more capacity out of existing transformers, and for this reason I think that Mr. Oldacre was justified in using the recorded ambient data as he did. Perhaps after the war we may wish to be more conservative and use Mr. Sealey's method of deriving the equivalent ambient temperatures.

The question of whether we should use daily average or daily equivalent ambient temperatures in applying the one per cent rule for overloading in cool ambients naturally arises. Both Mr. Oldacre's and Mr. Sealey's curves show that for a change of 20 degrees centigrade daily temperature (which represents a good average value) the effective ambient is in the order of one to two degrees centigrade higher than the average ambient. The one per cent rule has a small margin of safety for most transformers, that is, if a self-cooled transformer has 45 degrees centigrade top-oil rise, 65 degrees centigrade hot-spot rise, and a 2:1 loss ratio at rated load, for 20 degrees centigrade ambient it could carry 111.5 per cent load for a 95-degree hot-spot temperature instead of 110 per cent load permitted by the 1 per cent rule, 121.5 per cent load instead of 120 per cent load for 10 degrees centigrade ambient temperature, and 132 per cent instead of 130 per cent load for zero degrees centigrade ambient temperature. For this reason it is not really necessary to use equivalent daily ambient, but rather average daily ambient temperatures, when applying the one per cent rule.

Under most conditions a greater benefit can be derived probably from using the annual equivalent ambients rather than daily average ambient temperatures. Where overloads are required during the winter months only, the one per cent rule used with daily ambient temperatures would be better than the one per cent rule used with the equivalent annual ambient temperature. It is obvious that both rules cannot be used. The user, however, should be able to decide which rule, that is, the one per cent rule used with daily average ambient temperatures or the one per cent rule used with the equivalent annual ambient rule, works out best for his particular loading condition.

M. S. Oldacre: Mr. Sealey points to the method used in the paper as obtaining "average equivalent ambient temperatures based on an average year." This statement, which he develops in his discussion, is not correct. The equivalent temperatures obtained by methods in the paper are based on all the recorded temperatures over a long period of time and are not based on either average temperatures or extreme temperatures. The approximate method outlined in the paper uses the average temperatures, since they are readily available, but makes correction which takes into account the variations from averages that occur over a long period of time. This seems to be a better procedure than to use only averages or only extremes.

Mr. Wagner's suggestion to add six degrees centigrade to the average annual temperature is a very rough approximation which is good enough for some localities but not very adequate when there is either a great or small range of temperature during the year. As shown in Figure 3 of my paper, the correction factor is not a constant but a function of the temperature range. The use of the K_s' curve of Figure 3 gives a reasonably accurate value of equivalent annual ambient temperature from readily available United States Weather Bureau data.

As pointed out by Mr. Montsinger, we believe that the use of the equivalent ambient temperature, as derived in this paper, has a sufficient degree of conservatism for present-day conditions.

Thermal Rating of Overhead Line Wire

Discussion of paper 43-73 by Myron Zucker, presented at the AIEE North Eastern District meeting, Pittsfield, Mass., April 8–9, 1943, and published in AIEE TRANSACTIONS, volume 62, 1943, July section, pages 501–07.

Jerome J. Taylor (Detroit Edison Company, Detroit, Mich.): It is readily apparent that emergency overload ratings are valuable in application to overhead lines, as they are to transformers, circuit breakers, and other equipment. It is not so apparent that running line wire up to temperatures of 90 degrees centigrade or more, continuously, as inferred by the author's Figure 13, is either good engineering or a net service to the war effort.

If 3/0 copper conductor operated at 500 amperes is taken, as shown in the mid-range of Figure 13, the I^2R loss per mile of three-phase line is about 325 kw, four or five times that which occurs at ordinary loadings. At 500 amperes, one quarter of the total energy is thrown away in 17 miles of 24-kv line, or in 85 miles of 120-kv construction. Stated another way, the wastage of coal is about 300 tons per day per 100 miles of line thus operated. Losses of this magnitude seem to call for considerable justifying, even though the capacity of the lines has thereby been approximately doubled. Another drawback is that under such conditions there is little margin left for emergencies.

From the other side of the picture, if we assume that high loadings are advantageous in particular cases, reinforced conductors such as steel-reinforced aluminum cable enjoy large advantages over hard-drawn copper in regard both to strength and to sag considerations, since steel is little affected by relatively high temperatures. There is a good deal of such construction in service. The paper in limiting its consideration to copper only is more specialized than is indicated by its title.

In regard to a minor point, the writer questions the validity of the author's statement that ". . . . (self-) circulation was more effective, per unit speed in carrying away heat than equal real wind speeds." There seems no basis for such a conclusion either experimentally or theoretically. The difference in slope between the no-wind and the wind curves, of which Figure 6 is an example, is adequately explained by the fact that the air velocity of self-circulation increases with temperature rise of the wire, whereas external wind velocity does not.

The Sorocabana Railway Electrification

Discussion of paper 43-67 by Durval Muylaert, presented at the AIEE North Eastern District meeting, Pittsfield, Mass., April 8–9, 1943, and published in AIEE TRANSACTIONS, volume 62, 1943, pages 804–18.

Sidney Withington (New York, New Haven, and Hartford Railroad Company, New Haven, Conn.): This paper is exceedingly interesting in the data it presents, especially in indicating the status of railroading in a part of the world remote from the United States. The high cost of fuel and the low cost of labor in that section of Brazil are a surprise to us in the United States. A locomotive maintenance bill of 3.1 cents per mile and a fuel cost of $1.48 would mystify some of our master mechanics mightily, who report maintenance expense of six times, and fuel cost of one sixth of those figures. Skilled labor at 12 cents per hour would indeed make them envious.

The figure of more than 50 watt-hours per trailing ton-mile seems high compared with our figures, even considering the fact that the locomotive weights are relatively high compared with trailing tonnage. It would be of interest to know just why. In a Paulista report of some years ago 21.3 for freight and 35 for passenger were given.

The power costs are of interest: 90 cents per kilowatt per month demand, and energy cost of just over one mil—total cost being less than half a cent, with a load factor of less than 40 per cent. It is easy to see why the feeding back of surplus regenerated power was not thought necessary, even in the small net amounts it is contemplated will exist.

In this connection it is intimated that regeneration is not easily obtained in single-phase systems. It may be pointed out that there is considerable dynamic braking on some railroads abroad with single-phase motors where, as in Brazil, energy from water power is cheap.

The railroad is to be congratulated in resisting the temptation to cover modestly the locomotive running gear with petticoats. In spite of the alleged popularity of such efforts in this country, especially with steam locomotives, streamlining equipment thus does not seem justified.

The concrete hollow poles for catenary are of interest. It would be interesting to know just how the core or mandrel is designed in casting. Also it would be interesting to learn how the poles are climbed in maintenance.

It is a little harsh to charge engineers with prejudice in the design of catenary structures. There is no doubt that most of our designers would conscientiously adopt the most economic design, regardless of personal preference; the difficult line to draw is that between cheapness of construction and expense of maintenance and reliability of operation. That is a most vexing problem.

It is understood that the power in the vicinity of Rio de Janeiro is 50 cycles. It would be interesting to know just the division between 50 and 60 cycles in Brazilian territory.

Overvoltage Protection of Current-Transformer Secondary Windings and Associated Circuits

Discussion and author's closure of paper 43-70 by R. H. Kaufmann and G. Camilli, presented at the AIEE North Eastern District technical meeting, Pittsfield, Mass., April 8–9, 1943, and published in AIEE TRANSACTIONS, 1943, July section, pages 467–72.

C. A. Woods, Jr. (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The subject of the paper is very timely in focusing the attention of engineers upon a source of overvoltages in the secondary circuits of current transformers due to the rapid increase in installations involving the switching of lumped capacitance on power systems. The test data and calculations show that, under certain conditions, excessive voltages may be developed in the secondary circuits of current transformers used in such installations. The data given show very clearly the factors, such as source voltage, intervening line impedance, and current-transformer ratio, which determine the magnitude of the overvoltages induced in the current-transformer secondary circuits. This information should be of considerable benefit in determining whether any additional precautions regarding the protection or installation of transformer secondary circuits are necessary in such instances.

As stated by the authors of the paper, there are other sources of overvoltage on the current-transformer secondary circuits which have been recognized for many years. Likewise, in the past there has been considerable use made of some form of protective device, such as protective gaps, protective tubes, or other similar devices, for short-circuiting the secondary circuits of the current transformers when overvoltages occur, where these transformers were used for operation of instruments. On the other hand, there has been considerable hesitancy on the part of the industry to utilize any such device on current-transformer secondary circuits where the transformers are used to energize protective relays. The principal reason for this is that any such device has rendered the relays inoperative at the very time when they are most likely to be called upon to operate.

The new protector described herein seems to overcome the principal objection to the use of overvoltage protectors on the secondary circuits of current transformers used to energize relays. The curves of Figure 6 of the paper show the device performance under a specific set of conditions, but it would be interesting to know the volt-ampere characteristics of the protector, thus providing an indication of the limitations under varying conditions of currents and burdens.

We have developed recently a protective device, in combination with multicircuit arrangements, which will restrict the overvoltages in the secondary circuit of current transformers and still permit operation of protective relays. This device consists of protective gaps, tubes, or similar devices in series with an impedance, both in shunt with the secondary burden, and located as close to the secondary terminals of the current transformer as possible. The series impedance is co-ordinated with the burden and relay application so as to permit a proportional amount of the secondary current to flow through the burden because of the voltage drop across the impedance, even though the protective tube or gap has operated at the predetermined voltage. With this device in certain multiphase arrangements, particularly three-phase arrangements of current-transformer secondary circuits, it is even possible to obtain functioning of the protective relays, in certain instances restricted functioning, with one of the secondary lead wires or burdens open-circuited.

Over the years, operating experience has shown on average installations, capacitor installations for line power-factor correction excepted, that the ordinary overvoltages developed on current-transformer secondary circuits do not warrant the use of such protective devices in circuits involving protective relays, except under unusual conditions. Such unusual conditions involve very high ratio transformers, very long and possibly exposed secondary leads, and current transformers having unusually large cross-sectional area of core. Where the unusual conditions justify the application of such a device, it seems desirable that it should permit, to as great a degree as possible, proper functioning of protective relays.

The paper under discussion clearly indicates another special condition, wherein current transformers that are installed in, or adjacent to, circuits containing lumped capacitance require an investigation of the possible overvoltages on the secondary circuit. If it is found necessary, suitable protective measures should be taken.

M. C. Westrate (Commonwealth and Southern Corporation, Jackson, Mich.): After reading this paper, one is impressed with the need for overvoltage protection on current-transformer secondary circuits. However, the question immediately arises as to why there has not been more trouble experienced without such protective devices. The examples and tables in the paper show conditions where capacitors are involved, and it may be that high voltages will be experienced with increased capacitor installations. In the paragraph headed "Need for Overvoltage Protection," the authors do not limit the application of the protective devices to capacitor installations.

In the discussion of open-circuited secondary circuits, the authors mentioned that the voltage magnitude is a function of the current-transformer magnetic design. It would seem as though the saturation of the iron would be a very definite factor limiting the maximum voltage. Bushing-type current transformers, in particular, would be subject to the effect of saturation. It would be of interest if the authors would supply additional data or suitable references on the effect of saturation.

G. Camilli: Mr. Woods has amplified on the scope of the paper and has stressed the fact that the new protector does not interfere with the operation of overcurrent relays.

While the curves of Figure 6 of the paper show the performance of the device with a *Z* burden connected across the secondary circuit of the current transformer, the flexibility of the device is such that any predetermined characteristic can be obtained easily.

Mr. Westrate's comments are well founded. There are many cases in which the circuit insulation has been punctured by excessive transient voltage which may not be evident at normal rated current.

Automatic protection of every current transformer against open secondary circuits is desirable if the cost and size of the device are relatively small. There are, however, some applications where protection certainly should be considered, for example, high primary-current ratings, low secondary-current ratings, applications to test circuit where connections may be changed frequently.

New Developments in Potential-Transformer Design

Discussion of paper 43-71 by G. Camilli, presented at the AIEE North Eastern District meeting, Pittsfield, Mass., April 8–9, 1943, and published in AIEE TRANSACTIONS, volume 62, 1943, July section, pages 483–7.

J. J. Samson (Commonwealth Edison Company, Chicago, Ill.): The new type of potential transformers described by Mr. Camilli is quite an innovation, as it includes in one unit several new developments of the transformer industry. Most developments are rather piecemeal in using new methods and processes, and it is quite refreshing to see so many changes at one time.

In addition to the saving in materials, there is an improved insulation structure, practical elimination of gaskets by welding of cover to case and by sealed construction between porcelain and metal parts of the bushings, and also the use of noninflammable insulating liquid. All these points are included, and at the same time the accuracy is said to be as good or better than the older designs.

From the application angle, the new design of potential transformer has made long strides toward the 100 per cent we are looking for. Obviously, the smaller and lighter the transformer can be made, particularly if the size will allow mounting on the bus structure, the greater the saving in labor, material, and over-all area otherwise required. The material saving above 46 kv should be appreciable.

The new design also promises to mitigate some of the other potential-transformer problems. The one always present is that of protection—not of the potential transformer itself but of the bus, its associated equipment, and the service it supplies—from the effects of faults in the potential transformers.

A "stewing" fault in the secondary or the ground end of the primary winding of large oil-filled potential transformers is a major hazard. Fusing the primary winding is the usual remedy, with its well-known "headaches." If the fuse current rating is high enough to withstand the inrush current due to switching surges and other transients, it will not protect for faults in the secondary and in the ground end of the primary winding (because of the necessary high impedance of the primary winding). If the current rating is low enough to protect for these faults, it becomes a hazard to the relay protection. For example, a 138-kv potential transformer would require a 0.25-ampere fuse to protect for secondary short-circuits. The fuse will blow on surges or momentary secondary short-circuits, removing the potential from the reverse-power or impedance relays. It is therefore a step in the right direction to design the transformer so that the probabilities of fault are reduced to a point where the protection is not considered necessary.

It would have been helpful if more detailed description had been given of the joint between the porcelain and metal supports. This is an important development that is quite new, and, although it might be the subject of a separate paper, more information would be valuable with Mr. Camilli's paper.

The complete sealing of the transformer by welding and solder-sealed bushings will eliminate gaskets and greatly reduce the chances of trouble with the insulating liquid and insulation since moisture and oxygen are excluded. The improvements in shielding and impulse strength, together with proper lightning-arrester protection, should practically eliminate the chances of faults due to lightning. The small amount of liquid or the use of Askerel as liquid will make the fire and explosion hazard negligible.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): Mr. Camilli has done an excellent job in designing and building potential transformers of a construction so nearly the ideal. The paper states in general terms how the great reduction in size and weights was accomplished but gives no data on the relative strengths of the conventional method of arrangement of insulations and the arrangement which was used.

Two factors that appear to contribute to the improvement made in these potential transformers are:

1. Eliminating as far as possible nonuniform dielectric fields.
2. Eliminating oil in series with solid insulation between the high-voltage and low-voltage windings.

With reference to the first point: it can be stated that for nonuniform dielectric fields the spacing varies approximately as the three halves power of the voltage, that is $kv^{1.5}$, whereas for uniform fields the spacing varies approximately as the first power of the voltage. For low-voltage designs this factor is not very important, but for high-voltage designs it becomes very important. For example: if we assume that it requires the same insulation distance for both uniform and nonuniform fields for a test of 70 kv, then for a test of 460 kv the distance for the nonuniform field would be $(460/70)^{1.5}=16.8$ times that for the 70-kv test, whereas for the uniform field the distance would be $460/70=6.57$ times that for the 70-kv test or approximately four tenths of the space required for the nonuniform field.

On the second point: the benefits to be gained by elimination of the oil ducts in series with the insulation barrier between windings is best illustrated by an example, based on the fact that the division of voltage across solid and oil in series is inversely proportional to their dielectric constants. The dielectric constant of solid insulation is approximately twice that for oil.

The following assumptions are made:

1. The dielectric strength of oil is 200 volts per mil (uniform field).
2. The dielectric strength of solid is 250 volts per mil.
3. Test = 300 kv

If all solid is used, the thickness of barrier will be $300/250=1.2$ inches.

Half of barrier is now oil and half is solid. The stress on the oil is of course twice the stress on the solid, because of the ratio of their dielectric constants. Therefore, only half of the solid can be added to the oil to get the equivalent oil spacing required.

The space required will be $300/200=1.5$ inches of equivalent oil space made up of one inch of oil and one inch of solid, or a total thickness of two inches which is 1.67 times the thickness of the 1.2-inch solid barrier.

In other words, by the elimination of free oil ducts the barrier need be only 0.6 of that for a barrier with free oil ducts. Also, it was shown that with a uniform field the space was reduced to approximately 0.40 of that for a nonuniform field. The product of the two reductions is $0.6\times0.4=0.24$ per cent, or one quarter of the space between high-voltage and low-voltage windings for the conventional design.

A third factor which resulted in the saving of space was the ingenious method used in folding the paper over the ends of the layers in the high-voltage winding, which thereby eliminated large creepage distances ordinarily required in high-voltage layer-wound coils.

Automatic Voltage Compensator for Resistance Welding Control

Discussion and authors' closure of paper 43-121 by E. M. Callender and R. S. Phair, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 701–05.

Benjamin Cooper (General Electric Company, Schenectady, N. Y.): The work presented in this paper represents a step in the advancement of high-quality resistance welding as applied on a mass-production basis. The problem of either providing a heavy electric-power distribution system or providing electrical apparatus to compensate for the voltage variations which are unavoidable on less-substantial power systems, is a problem for users of resistance-welding equipment if they are to maintain consistently high-quality welding, particularly on power systems that become more and more overtaxed.

While the first alternative of building a power-supply system with low-voltage drop represents a very sound way of improving the voltage conditions, it may not be selected for at least two reasons: First, it may not represent the most economical solution; and second, in view of our wartime emergency, the new aspect of a maximum saving in critical materials becomes an important item for consideration.

If the building of a heavier power system were selected as the way of improving the voltage conditions, anticipated growth would be the deciding factor as to how much more substantial the power system should be made. Obviously, an adequate system today may soon be overtaxed by an unanticipated growth. This, then, means unused copper and idle fixed assets today in anticipation of tomorrow's growth. On the other hand, the voltage-compensating apparatus may be added as an integral part of the new installations, or, in many cases, used to supplement the older installations.

One form of electrical apparatus which is now commercially available and which may alleviate the poor-voltage conditions at the welder is the capacitor for power-factor improvement. This type of apparatus reduces the voltage variations by reducing the high reactive demands such as are required by a welding transformer. However, in some cases it may be either impractical or uneconomical to add capacitors for purposes of improving the voltage conditions. The best answer in these latter cases would certainly be the voltage-compensating or current-regulating control. Then too, there is the possibility, even after capacitors have been added, that the electrical demand may be great enough to require the voltage-compensating control anyway. The voltage-compensating control is an electronic device which does not attempt to correct the voltage variations, but rather changes the current by phase-control methods so as to provide a constant welding current irrespective of these voltage variations. While in the authors' case, their control is designed to compensate for voltage variations, it should be pointed out that electronic type of control is not necessarily limited to compensating for voltage variations alone. Electronic control may be made to regulate the welding current and to compensate for changes in the welding-transformer power factor, impedance, or any combination of voltage and impedance effects that might change the welding current. This is an advantage which cannot be gained in any way except by means of proper control. There are in operation numerous current-regulating compensators which permit a constant welding current to be delivered to the weld, irrespective of the size of the welder throat or of the change in projected metal into the throat of the welder. One example is an arrangement where identical welds are made alternately, using first the main electrodes of the welding machine and then an auxiliary parallel-connected welding gun without making any changes in transformer taps or current adjustments on the welding control.

In the authors' case, because they were welding nonmagnetic materials they did not experience any appreciable impedance changes; hence they were interested only in voltage compensation. For their work, a special voltage compensator was used advantageously in conjunction with the current squared–time or I^2T recorder, the I^2T recorder being a standard part of each welding control operated by them. Using the voltage compensator in these systems, they have demonstrated conclusively, by actual operations, that high-quality welding can be maintained in production lines, independent of the line-voltage variations even though, at times, these variations may be in excess of 20 per cent.

To produce equivalent-quality welding by addition of extra feeders would have required that the capacity of the power system be more than doubled. This is a striking enough comparison.

In conclusion, when the abilities of electronic control, as a device for maintaining constant welding current, become more common knowledge through industrial experiences and papers such as this, there is little doubt but that the future may find an electronic regulating unit an integral part of most welding installations.

E. M. Callender and R. S. Phair: Mr. B. Cooper's discussion brought out a consideration that was not previously stressed. Certain types of welding, notably seam welding, may require special control when welding material having permeability greater than unity. For instance, the configuration, or otherwise the varying portions of the work, passing through the throat of the welder loop may have a marked effect on the secondary-circuit reactance. The authors did not meet particularly with this problem, since the welding material is mostly stainless steel with permeability approaching unity.

Constant-current regulators which regulate the current for many of the causes, such as line-voltage drop or reactance change, are understood to be available. These devices are somewhat more elaborate and probably require more attention than the automatic voltage-compensator described.

We wish to emphasize that the voltage compensator in no way affects the regulation of line voltage, but regulates the welding current during the multiplicity of machine operation on the same line. Therefore, full consideration still is required to determine that the application contemplated will not reflect undue voltage drops back to the power company high-voltage line, with consequent light-flicker problems. Multiple-welding installations require as "stiff" a source of power as possible and complete divorcement of lighting and welding loads within the plant.

The authors appreciate Mr. Cooper's clear interpretation of plant savings in copper bus feeders through use of the voltage compensator.

A Study of Voltage Transients in Arc-Furnace Circuits

Discussion and author's closure of paper 43-97 by J. B. Hodtum and J. B. Rice, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 556–62.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The authors have found in their study that the highest switching surge was of the order of 5.2 times normal. This corresponds to approximately 73 per cent of the full-wave impulse test level of the rated transformer voltage. From tests made on barriers representing transformer insulation, such barriers can withstand switching surges of approximately the same amplitude as the full wave. Furthermore, even though switching occurs rather frequently, from the data presented it appears that the highest surge could occur only once in approximately a month's operation. These results do not differ greatly from those obtained at other installations.

The authors also have found that the surges originating on the low-voltage side, due to arcing, are not transmitted to the high-voltage terminals. This also has been confirmed by others. However, tests made by the General Electric Company show that such transients are transmitted to the high-voltage side of the transformer. In Figure 1 of this discussion, upper trace, is shown the voltage to ground at the junction of the high-voltage winding and the internal reactor. The violent changes in voltage and the high-frequency character are of similar nature as the surges on the low-voltage side. The lower oscillogram trace shows the simultaneous voltage to ground from the other end of the transformer winding which is connected to the supply line and shows no disturbance whatever.

The installation on which these oscillograms were obtained is similar to the ones described in the paper, except that the high-

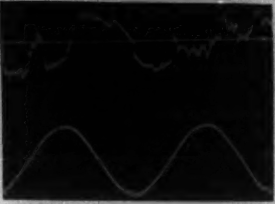

Figure 1. Line voltage (H_1 to ground) and voltage from reactor–transformer connection (B-10 to ground)

Rapid rate of voltage change inside of transformer–reactor windings is not associated with disturbance on the transformer line terminals

voltage winding was rated 13,800 volts, delta–wye. The network feeding this transformer is a 13.8-kv cable network of 52 miles total cable length. Power to this network is supplied principally from a 115-kv system and partly from generators through step-up transformers.

Measurements made with surge-voltage recorders from line to ground showed overvoltages of less than four times normal line-to-ground voltage crest. These overvoltages occurred only during switching.

Figure 2 of this discussion, upper curve, shows the voltage from transformer reactor junction to ground with peak voltages of about two times normal line-to-ground voltage crest and a frequency of approximately 10,000 cycles. In the lower curve the associated arc voltage and current show that the violent changes on the transformer high-voltage side are associated with rapid changes in arc current.

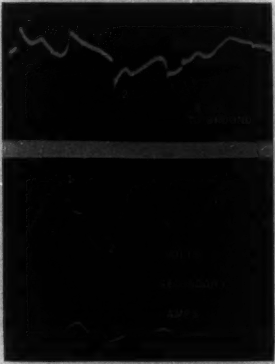

Figure 2. Transients within high-voltage windings are caused by arc phenomena in the furnace

Top. Voltage to ground from transformer–reactor junction

Bottom. Arc voltage and arc current. Points 1 and 2 relate voltages at high-voltage and low-voltage terminals

In this study it was found that only in the melt-down period such high-frequency transients occur in the high-voltage winding of the transformer. The frequencies of the observed transients ranged between 10,000 and a few-hundred cycles. The maximum voltages measured across the reactor and across the high-voltage winding of the transformer with surge-voltage recorder were only of the order of 2.5 times to 3.5 times normal line-to-ground voltage crest However, in cases of resonance between natural periods and applied frequencies, it appears possible that voltages of much higher relative amplitude may appear across certain portions of the windings. These high-frequency surges are very numerous. Thirty-three of the steep-front 10,000-cycle surges of Figure 2 occurred within the observed period of 48 cycles. Therefore, it would appear that the internal transient voltages caused by the low-voltage arc transients are much more important from the point of view of transformer insulation than the relatively infrequent switching surges.

C. C. Levy (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It seems to me that in some cases the conclusions drawn are too positive, and not wholly justified by the evidence; for example, I do not believe that the statement made by the authors to the effect that characteristics of the electrode control have an important bearing upon voltage surges caused by instability of the arc is justified. In the body of the paper evidence is submitted that voltage surges do not occur when the secondary circuit is opened, and this of course is what would happen if the arc were unstable.

Again, I do not think that sufficient evidence is produced to reach the conclusion that tripping under load in the early part of the heat will result in higher-voltage transients than load tripping during refining. This is merely an academic consideration, because in the refining period load is quite stable, and such short circuits as occur are rapidly cleared by the regulator so that the breaker seldom, if ever, opens during this period. My theory is that if there is a difference, as the authors claim, it will probably lie in the fact that simultaneous extinction of the arc on the secondary side, caused by regulator operation, and the opening of the breaker on the primary side is much more likely to happen during the melt-down period than during refining. This condition would give rise to higher transients during meltdown.

I should like to ask Mr. Hodtum if the tests at location *A* when the change was made from cable to overhead line showed greater number as well as greater magnitude of the surges for the cable as against the overhead line. It is also possible to assume that the change in overvoltages when the cable was replaced by overhead line might have been due to arcing grounds in the cable system. I mention this because some recent tests on a furnace circuit supplied by a cable showed a large number of overvoltages which were proved definitely to be caused by an arcing ground in the cable system.

[illegible] should also like to ask whether the possi[illegible]ility may not exist that closing surges are [illegible]ibutable in some cases to the mechanical condition of the breaker causing restrikes in closing.

G. E. Shaad (nonmember, General Electric Company, Schenectady, N. Y.): There are some unknown factors in regard to the circuit characteristics used in these tests when it comes to explaining the high voltages obtained, and a great deal of study may have to be given before all of the phenomena definitely can be explained. For instance, an "ideal" breaker might be considered as one which closes all three phases at exactly the same instant; there is no restriking phenomenon on either closing or opening, and the current is interrupted at normal current zero. It is well-known that for such breaker operation it is not possible to get over twice-normal voltage on the closing or opening of a circuit irrespective of the circuit constants.

When we necessarily discard the desirable characteristics of the "ideal" breaker, however, it is theoretically possible to get transient voltages many times normal, during both opening and closing of transformer circuits. So little is known, however, in regard to the circuit-breaker phenomena present in these tests that we should not be too hasty to condemn any one item, for example, the use of cables on such installations. For instance, it is interesting to note the following facts in regard to the tests just completed and presented.

It may be noted that in Figure 5 of the paper, curve *B*, a maximum of four times normal voltage was obtained on a grounded system with *no* underground cable, while in Figure 6, curve *B*, exactly the same value of four times normal voltage is given as a maximum for a grounded system with cable. Thus, there would seem to be little to choose between the two.

The capacitance of cable is some 15 to 20 times that of overhead transmission lines. In the particular case used in this paper the cable run was 650 feet. Thus, it was equivalent in capacitance to about $2^1/_3$ miles of overhead transmission line. It is interesting to note that there are some 350 circuit miles of overhead transmission in this system. Thus, the capacitance of the cable involved adds very little to that of the system.

Switching surges of four or five times normal, caused by restriking of the arc in the interrupter, have been recognized for many years, and it is believed that adequate protection against contingencies of such overvoltages should be provided, as may be obtained by the use of protective equipment, instead of changing the form of one of the circuit elements, which may be inconvenient and is of doubtful value.

It would appear, therefore, that the excellent data presented in this paper should serve as the groundwork for future study. The fact still remains that there are hundreds of successful installations using cable.

Sherwin H. Wright (Ebasco Services, Inc., New York, N. Y.): In the paper it is stated that it appears unlikely that overvoltages caused by breaking magnetizing current should be a source of serious concern. One's attitude toward the possibility of overvoltages from breaking magnetizing current depends on whether the overvoltages have actually been experienced. It is believed that breaking magnetizing current on power transformers not only can but does occasionally produce serious overvoltages on parts of systems, and that it should not be assumed that this is unlikely to be of serious concern. It may occur only rarely, but when it does occur it may create results which are quite serious.

Regarding overvoltages which are produced by switching, such as those mentioned in the paper as often occurring on the high-voltage terminals, it seems well worth-while to consider the possibility of their control by a resistor in the circuit-breaker interrupting element. This has already been accomplished on 230-kv circuit breakers used for extensive high-voltage transmission systems. Have the authors examined this possibility of control of transients?

H. G. Heath (Pittsburgh Lectromelt Furnace Corporation, Pittsburgh, Pa.): From tests and analysis of switching operations on arc-furnace installations, it has been known generally that voltage transients of considerable magnitude resulted. In fact, the first data obtained in connection with voltage transients on an arc-furnace circuit occasioned the observation that the magnitude was no greater than would be expected from previous knowledge of switching operations.

The present studies, I believe, should take into consideration the large number of furnaces in operation on which the insulation levels have been ample, at least to the extent that the few failures should be considered abnormal or exaggerated cases.

On 5-kv transformers the test voltage, high voltage to low voltage and ground, is 3.8 times the rated voltage, while I find it to be only 2.23 and 2.05 times, respectively, on 23 and 34.5-kv transformers, respectively. It has been the practice of my company to specify 34.5 kv installation test on 23 kv and 46-kv installation test on 34.5-kv transformers, which brings test values to 3 times and 2.7 times rated voltage, respectively. It is to be admitted that conditions are more severe on 23 and 34.5-kv installations and need to be given every consideration. There have been no failures on 13.8 kv where the ratio is 2.46 times the rated voltage, except in one or two abnormal cases.

On installation *A*, I was surprised at the mention of restrikes on the closing of the switching equipment. The switching equipment consists of an oil circuit breaker equipped with Ruptors where the current-carrying members consist of a bayonet and a segmented stationary contact equipped with springs to give sufficient pressure. The bayonet enters this stationary segmented contact to a degree that I can see no possibility of sufficient bounce to open or break contact. Possible explanation for this restrike indication is that it may be a reflected wave.

The use of surge capacitors has been recommended and tests made where they have been installed. The magnitude of the voltage transients has been reduced to approximately two times normal. Economically, I have found that in most cases it is less expensive to install surge capacitors than to insulate the transformer for the next higher-voltage class.

J. B. Hodtum: During the meltdown operation there is usually a reactor connected in series with the primary winding of the transformer. Between the secondary terminals of the transformer and the arc there is considerable reactance because of the secondary lead connections. For the refining period the primary reactor is usually removed from the circuit. If the primary switch is opened during the meltdown operation, with the electrodes short-circuited, the primary winding receives an inductive kick from the primary reactor and the secondary winding an inductive kick from the lead reactance. During operation of the furnace under this condition, oscillations appear at the junction of the primary reactor and the primary winding as indicated in Mr. Hagenguth's Figure 1, upper trace.

If the furnace arc interrupts with the primary breaker closed, similar disturbances occur at this junction.

Surge magnitudes of the order of 100 times normal have been recorded at the secondary terminals of the transformer by other observers. These probably resulted from the inductive kick produced by the collapse of the field surrounding the secondary leads when the primary breaker is opened and with the electrodes short-circuited.

During the refining period the primary reactance usually is removed from the circuit, and investigations conducted within the transformer winding indicate similar surges as shown by Mr. Hagenguth's figures.

Mr. Shaad raised the point regarding Figures 5 and 6 of the paper. Curve *A* of Figure 6 represents a system having an impedance ground and a lump capacitor on the line side of the breaker. Curve *B* represents a system solidly grounded, but with capacitance on the line side of the breaker. Curve *C* represents an impedance-grounded system without the additional capacitance on the line side of the breaker.

Curve *B* of Figure 5 represents a solidly grounded system without the additional capacitance. The magnitude and frequency for this connection is almost identical with that of curve *B* of Figure 6. The surges for curve *B* of Figure 6 are all on closing, while those of curve *B* of Figure 5 are equally on opening and closing.

The authors may be in error in assigning the principal cause of the overvoltages to the extra capacitance; however, when this capacitance was removed and with the system impedance grounded, the magnitude of surging was reduced from 4 times normal to $2^1/_2$ times normal maximum.

Mr. Hagenguth's point regarding the impulse strength of the winding is more or less confirmed, in that there have been few winding failures caused by switching surges. Practically all of the difficulties have been caused by the flashover of associated equipment.

Regarding Mr. Levy's question as to whether when the change was made from cable to overhead line showed a greater number as well as a greater magnitude of surges for the cable against overhead line, I regret that at the present time I cannot give him a definite answer. I am not, however, willing to admit that cable failures at other locations have been due to arcing grounds within the cable. It is quite possible that the arcing grounds were the result rather than the cause.

Mr. Wright raises the question regarding a resistor in the breaker interrupting element. This was considered, of course, and some testing was accomplished, the results of which indicate a reduction in magnitude of the overvoltage.

It is difficult to visualize how the breaker described in Mr. Heath's comment can restrike on closing. The magnitude of the overvoltages, however, indicates that some such phenomena must be taking place. It is possible that a similar result is obtained when all of the phases do not close simultaneously.

Practical Calculation of Electrical Transients on Power Systems

Discussion and authors' closure of paper 43-98 by R. D. Evans and R. L. Witzke, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 690–6.

T. Y. Shen (Nonmember; American Gas and Electric Service Corporation, New York, N. Y.): Evans and Witzke have made an important contribution to the method of calculating transients on power systems. On frequent occasions we encounter generator-neutral-grounding or lightning-arrester-application problems in which we have to determine the magnitude of transient over voltage due to ground fault. The magnitude of these transient voltages can be amplified considerably if the fault is of the arcing type or if it consists of successive clearings and restrikings during circuit interruption. Calculations for these types of faults are usually lengthy and cumbersome if conventional mathematics are used. By using the method suggested in Evans and Witzke's paper, many mathematical manipulations are by-passed, and hence much time is saved. Furthermore, this method is well adapted to the needs of many electrical engineers who would prefer to attack the problem without resorting to differential equations or Heaviside operational calculus.

In applying the present method to practical computations, our experience from various calculations indicates that in many cases the zeros and slopes can be determined quite easily from the analytical expressions of the reactance or admittance function and its derivative. This results from the fact that many ordinary power circuits can be reduced and represented by a relatively simple network. For this reason the angular velocity, ω, corresponding to zero reactance (or infinite admittance) between the driving-point terminals can be determined by equating the impedance function to zero and solving for the respective ω's. Similarly, the angular velocity corresponding to zero admittance (or infinite reactance) between the driving-point terminals can be determined by equating the admittance function to zero and solving for the respective ω's. This method of finding ω requires the determination of the roots of an algebraic equation whose degree is equal to the number of internal zeros of the reactance function or admittance function. In the case of a complicated network, the equation will be of high degree and will not be practical to solve. Under such conditions, the cut-and-try or graphical method developed in the paper will undoubtedly be more suitable.

If a high-degree accuracy is desired in the calculation of the final network response, the slopes as well as the zeros should be determined with high accuracy. The slopes of the angular velocity-reactance (or angular velocity-admittance) curve can be determined more accurately from the first derivative of the reactance (or admittance) function. If the reactance (or admittance) between the driving-point terminals be expressed in the following form:

$$X=\frac{1}{B}=\frac{\lambda_1\omega^n+\lambda_2\omega^{n-2}+\lambda_3\omega^{n-4}\ldots.}{\beta_1\omega^m+\beta_2\omega^{m-2}+\beta_3\omega^{m-4}\ldots.}$$

where $\lambda_1, \lambda_2, \ldots.$ and $\beta_1, \beta_2, \ldots.$ are functions of the circuit parameters, the procedure for determining its derivative at zero impedance (or admittance) can further be simplified, because all terms involving the product of the numerator of the impedance (or admittance) function are equal to zero. Therefore

$$\left.\frac{dX}{d\omega}\right|_{\text{at } X=0}=\frac{1}{(\beta_1\omega^m+\beta_2\omega^{m-2}+\beta_3\omega^{m-4}\ldots.)^2}\times$$

$$(\beta_1\omega^m+\beta_2\omega^{m-2}\ldots.)\frac{d}{d\omega}[\lambda_1\omega^n+\lambda_2\omega^{n-2}\ldots.]-$$

$$(\lambda_1\omega^n+\lambda_4\omega^{n-2}\ldots.)\frac{d}{d\omega}[\beta_1\omega^m+\beta_2\omega^{m-2}\ldots.]$$

$$=\frac{1}{(\beta_1\omega^m+\beta_2\omega^{m-2}+\beta_3\omega^{m-4}\ldots.)}\times$$

$$\frac{d}{d\omega}[\lambda_1\omega^n+\lambda_2\omega^{n-2}+\lambda_3\omega^{n-4}\ldots.]$$

Similarly

$$\left.\frac{dB}{d\omega}\right|_{\text{at } B=0}=\frac{1}{(\lambda_1\omega^n+\lambda_2\omega^{n-2}+\lambda_3\omega^{n-4}\ldots.)}\times$$

$$\frac{d}{d\omega}[\beta_1\omega^m+\beta_2\omega^{m-2}+\beta_3\omega^{m-4}\ldots.]$$

In most of the practical cases, very little numerical computation is involved by using this method. One further advantage is that the computation may be done on a slide rule, whereas the calculations for the incremental method as suggested by the authors in their illustrative example must be carried out to five or six figures or more in order to obtain the same degree of accuracy. This is true, because in the authors' method of evaluating $\Delta X/\Delta\omega$ or $\Delta B/\Delta\omega$, the result depends upon finding the difference of two numbers which are approximately equal. The truth of this statement is clearly shown by the numerical example in the paper.

It might be of interest to show a numerical example illustrating our method of computing the transients due to a circuit change. Taking the same problem as in Table VI in the paper, the admittance function can be expressed by the succeeding equation.

$$B=\omega C_1-\frac{1}{\omega L_1}+\frac{1}{\dfrac{1}{\omega C_2}-\omega L_2}$$

$$=\frac{L_1L_2C_1C_2\omega^4-(L_1C_1+L_2C_2+L_1C_2)\omega^2+1}{L_1L_2C_2\omega^3-L_1\omega}$$

By substituting numerical values, thus

$$B=\frac{10^{-14}\omega^4-2.1\times10^{-7}\omega^2+1}{10^{-8}\omega^3-0.1\omega}$$

Set $B=0$ and solve for ω's

$\omega_a=2{,}700$ or $\omega_b=3{,}700$

$$\left.\frac{dB}{d\omega}\right|_{\text{at } B=0}=\frac{1}{10^{-8}\omega^3-0.1\omega}\times$$

$$\frac{d}{d\omega}(10^{-14}\omega^4-2.1\times10^{-7}\omega^2+1)$$

$$=\frac{4\times10^{-14}\omega^2-4.2\times10^{-7}}{10^{-8}\omega^2-0.1}$$

At $\omega_a=2{,}700$

$$\frac{dB}{d\omega_a}=0.474\times10^{-5}$$

At $\omega_b=3{,}700$

$$\frac{dB}{d\omega_b}=0.344\times10^{-5}$$

$$A_a=\frac{2}{2{,}700\times0.474\times10^{-5}}=156$$

$$A_b=\frac{2}{3{,}700\times0.344\times10^{-5}}=157$$

$$I(t)=2.65\cos\left(377t-\frac{\pi}{2}\right)$$

at $\omega=377$

$B=-0.0261$

Therefore

$$I(t)=\frac{2.65}{-j\,0.0261}\cos\left(377t-\frac{\pi}{2}\right)-$$

$$\frac{2{,}700\times377\times156\times2.65}{2{,}700^2-377^2}\cos 2{,}700\,t-$$

$$\frac{3{,}700\times377\times157\times2.65}{3{,}700^2-377^2}\cos 3{,}700\,t$$

$$=102\cos 377t-59\cos 2{,}700\,t-43\cos 3{,}700\,t$$

R. D. Evans and **R. L. Witzke:** Mr. Shen's discussion is a noteworthy supplement to our paper, as it illustrates an alternative method of obtaining the zeros and slopes. As pointed out in our paper, under "Extensions of the Method," ". . . it is possible and sometimes convenient to form the analytical expression for the impedance (or admittance) function and its derivative and to use the first or both of these to determine the zeros and slopes." Mr. Shen has mentioned that it may be more convenient to obtain the zeros by graphical or cut-and-try methods when the equations involved are of a high degree. This procedure will also be useful, if not necessary, in those cases where hyperbolic or trigonometric functions are used to represent distributed circuit constants. In many cases we have found it desirable to calculate the zeros by graphical methods, and then to obtain the slopes by substituting the zeros in the general analytical expression for the derivative of the reactance (or susceptance) function. The procedure to follow in any particular case will depend upon the complexity of the network as well as the experience of the operator.

Mr. Shen's simplified expressions for the slopes of the reactance and susceptance functions are very useful when the complete analytical solution is made. Similar expressions can be obtained directly from the Heaviside expansion theorem as follows:

$$i(t)=i_0(t)+\sum_{k=1}^{k=2n}\frac{e^{p_kt}}{p_kZ'(p_k)} \tag{1}$$

Let

$$Z(p_k)=\frac{D(p_k)}{M(p_k)}$$

Then

$$Z'(p_k)=\frac{M(p_k)D'(p_k)-D(p_k)M'(p_k)}{[M(p_k)]^2}$$

$$=\frac{D'(p_k)}{M(p_k)}\text{ because } D(p_k)=0$$

if we substitute in equation 1

$$i(t)=i_0(t)+\sum_{K=1}^{K=2n}\frac{M(p_k)e^{p_kt}}{p_kD'(p_k)}$$

which is another common form of the expansion theorem.

The Calculation of Unbalanced Magnetic Pull in Synchronous and Induction Motors

Discussion and author's closure of paper 43-99 by R. C. Robinson, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 620–4.

C. J. Fechheimer (Louis Allis Company, Milwaukee, Wis.): Mr. Robinson has given consideration to the decrease in unbalanced magnetic pull caused by circulating currents in parallel paths of the stator windings. As the vast majority of induction motors have squirrel-cage rotors in which there is nearly the equivalent of an infinite number of phases, consideration should also be given to the decrease in pull occasioned by circulating currents in the squirrel-cage winding. If the rotor is of the slip-ring type with parallel paths, there are circulating currents between such paths, although they are not so helpful as a squirrel cage. A damper winding on the rotor of a synchronous motor helps in the same way as a squirrel cage on an induction motor, although not to the same degree.

I do not believe that a two-circuit stator winding is necessarily in the same class as a single-circuit winding; for example, if turned through 90 degrees from the axis of symmetry.

I would inquire of the author whether $^1/_{32}$-inch displacement is still considered to be the arbitrary value for synchronous machines. Certainly that value is entirely too large to be used as a basis for calculation in induction motors.

R. C. Robinson: Mr. Fechheimer points out that the presence of either parallel circuits in the rotor winding or a two-parallel stator winding may further reduce the unbalanced magnetic pull. While this is true for certain conditions of air-gap deflection, there are other conditions where there will be no reduction in pull. For instance, a machine operating at synchronous speed with a rotating deflection will have no reduction in pull caused by rotor parallels and at a speed close to synchronous will have only slight reduction. A two-parallel stator winding will produce no reduction in pull when the deflection is along the line between parallels.

Since we do not know in advance what type of deflection the machine may have in operation, it is desirable to make the most pessimistic assumptions. Consequently, the machine should be calculated without regard to any reduction in pull produced by the rotor winding or the two-parallel stator winding.

A displacement of $^1/_{32}$ of an inch is the generally accepted standard for synchronous machines. The use of an arbitrary figure such as this merely serves the purpose of giving a basis of comparison between machines and between the unbalanced pull and the shaft spring constant.

Pole-Face Loss in Solid-Rotor Turbine Generators

Discussion and author's closure of paper 43-76, by W. W. Kuyper, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, volume 62, 1943, pages 827–34.

C. J. Fechheimer (Louis Allis Company, Milwaukee, Wis.): Mr. Kuyper has analyzed the pole-face loss of turbine generators arising from the armature-magnetomotive force. Has he made analysis of the loss due to the tufting of the flux from the stator teeth, which occurs at no load and is generally included in the core loss? It would be interesting also for Mr. Kuyper to tell his views on the beneficial effects secured by grooving the rotor surface, as has been the practice of manufacturers of turbine generators.

C. Concordia (General Electric Company, Schenectady, N. Y.): In regard to the value of rotor-iron permeability to be used in the loss formulas developed by Mr. Kuyper, it should be remembered that the flux components with which we are concerned are usually relatively small pulsating components superposed on a large direct component. For this reason it might seem logical to use an incremental permeability in the relations between flux density and magnetizing force.

Laboratory measurements of the incremental permeability of typical rotor steels for small oscillations (of practically zero frequency) superposed on a direct flux have indicated that the incremental permeability is small and relatively constant. Plotted as a function of the magnitude of the direct component, it starts at the value of initial permeability and decreases at a slow and rather uniform rate with increasing average flux density. In nearly all cases investigated the curves ranged from slightly over to slightly under $\mu = 100$, so it would seem that $\mu = 100$ is a good round figure to use in these loss calculations, as well as in some of the transient short-circuit calculations discussed in reference 2 of Mr. Kuyper's paper.

Since the behavior of the incremental permeability is entirely different from that of the garden variety of permeability, one must use caution in the interpretation of test results in which the direct component of rotor flux is absent.

C. B. Fontaine (General Electric Company, West Lynn, Mass.): Mr. Kuyper's paper is of real value to the turbine-generator designer, inasmuch as it offers a rational means for evaluating a major component of the complex generator load loss. These methods have been used to advantage, not only in determining losses for usual generator stator windings, but also in determining the proper location for omitted coils. An idea of the importance of proper location may be gained by observing that a coil grouping of (5-) (5-) for an 11/18 pitch winding results in three times the pole-face loss calculated for a (3-2) (3-2) winding arrangement.

The calculations, particularly those using Figure 3 of Mr. Kuyper's paper, are not complicated. Curves of loss versus pitch, similar to those of Figure 1 of this discussion, may readily be made up to give loss directly for frequently used winding combinations. Figure 1 represents the calculated loss for two-pole 60-cycle three-phase generators with no coils omitted, and with 21 slots per pole, but applies with slight error to similar generators having 18 or more slots per pole.

The calculated pole-face loss is critically dependent on armature-winding pitch whether or not coils are omitted. Figure 1 shows this relation clearly when all coils are in. An attempt has been made to check this calculated relation between loss and winding pitch by studying results of load-loss tests taken previously on about 100 generators ranging in size from 1,250 kva to 12,500 kva. These generators all had magnetic-rotor retaining rings. The losses were measured calorimetrically during short-circuit heat runs and are not reliable individually. However, when the test load loss minus the calculated extra copper loss is plotted against pitch, the curve representing the average of the test points for a given frame size shows a maximum and a minimum near the same values of pitch as the calculated pole-face-loss curve, but the effect of the pitch on the magnitude of the test-loss curve is not so pronounced as the effect on the calculated pole-face loss.

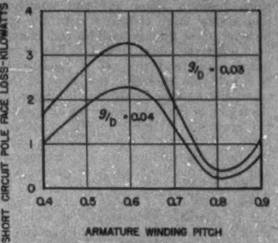

Figure 1. Short-circuit pole-face loss for solid-rotor generators. Calculated for a two-pole 60-cycle three-phase turbine generator, with no coils omitted

A = 10,000 ampere turns per pole and *L* = ten inches. Loss is proportional to A^2L

Losses in the stator end structures of turbine generators form an appreciable portion of the total load loss. If the pole-face-loss formulas and the extra copper-loss formulas were correct, the difference between the test load loss and the sum of the two calculated losses would represent mainly the stator end losses. This residue component of the loss was found from the tests above to increase with pitch between about 0.5 pitch and 0.8 pitch, and to dip downward slightly between 0.8 and 0.85 pitch. It is probable that the true end loss contains one component which is relatively independent of winding pitch for a given value of fundamental armature reaction, plus another component which increases with pitch. If this is true, the trend of the test residue loss with pitch appears reasonable, excepting for the downward dip after 0.8 pitch, which may reflect an inaccuracy in the pole-face-loss formulas.

For small two-pole 60-cycle generators we have used the expression, end loss in kilowatts $= (A/10{,}000)^2(D_s/10)\,(K)$, where A is the square wave value of fundamental armature reaction in ampere turns per pole, and D_s is the stator-bore diameter in inches. K is an arbitrary loss factor, the value of which is determined by test and is plotted against pitch for generators of a given frame diameter and given end construction. Use of the formula and curves have allowed reasonably close predetermination of the short-circuit load loss for those individual generators where the load loss has been measured reliably by dynamometer drive. An average value of K for generators of usual design is roughly 4.

Such a simple expression for end loss, while useful to the designer, does not separate many of the fundamental factors controlling the loss. Consider, for example, a two-pole generator with magnetic-rotor retaining rings operating under short-circuit conditions. The path of the major portion of the fundamental stator-end leakage flux is from the stator end through air to the retaining ring, peripherally around the retaining ring to the opposite pole, through air to the stator end, and peripherally back through the stator end structure to the starting point. The reluctance of the air paths between stator end and retaining ring appear large compared with the remainder of the leakage path through metal, even when the added reluctance due to induced stator end currents is considered. It is probable, therefore, that most of the stator-end leakage flux leaves the stator in the region of the slots, where the air gap to the magnetic retaining ring is relatively short. Since most of this leakage flux links the stator-end windings near the stator core, and before the windings have turned appreciably toward the connection point, it appears that most of the stator-end leakage flux is relatively independent of winding pitch for a given value of armature reaction. This applies as well to the end losses in the region of the stator slots and clamping fingers. The large unlaminated mass of the clamping flange lies above the slot region, however, so that the magnetomotive force, flux density, and losses in this region must be dependent to some extent on pitch. Obviously, the stator end structure configuration and materials, as well as other factors, must also affect appreciably the end losses.

Past papers have presented means for evaluating and controlling extra copper losses. Mr. Kuyper's paper accomplishes the same result for harmonic rotor-surface losses. His work paves the way for a thorough study of losses in the stator-end structure. This additional step must be carried out before evaluation of all major components of load loss may be placed on a rational basis.

W. W. Kuyper: Mr. Fontaine's method of simplifying the calculations is most welcome to designers. It permits a reasonably accurate determination of pole-face loss in a shorter time than is usually required for copper or core losses.

The end loss, as Mr. Fontaine points out, is affected by many factors. A determination of this loss component is difficult, not only because of the geometry of the region, but also because of the wide variety of materials employed. The retaining rings, the stator clamping flanges and fingers, the binding rings, the shape of the stator-coil ends, the type of connections, and the end covers of the machine all affect the loss through their shapes, locations, and the materials of which they are made. It is encouraging that an approximate calculation can be made by a simple formula for a group of machines which is of similar construction.

Because tests give us the sum of pole-face and end losses together with the more accurately determinable extra copper loss, it appears that further progress in the accuracy of predetermining one component is conditioned upon advancement in the quantitative understanding of the other component. The use of accurate testing methods employing dynamometer drive will be a powerful tool in these studies.

The facts which Mr. Concordia presents concerning incremental permeability probably are applicable directly to the pole-face-loss problem and should be considered by those who use the subject paper.

As suggested by Mr. Fechheimer, attempts were made to apply the method to the flux-tufting problem. The calculations showed negligible values of loss for turbine generators of usual design, and the study was abandoned.

That grooving the rotor reduces the pole-face losses has been determined by test. The usual grooving employed is a square thread of about one-fourth-inch pitch. Such a coarse groove is evidently not the ideal, and it is to be expected that calculations for the ideal case of a perfectly laminated layer on the surface of the rotor will show a greater reduction of loss caused by grooving than actually exists.

Such calculations have been made, and the following results are shown to indicate what can be done with perfect grooving:

Let region 1 be the main rotor body
region 2 be the grooved region
region 3 be the air gap
region 4 be the stator

The conductivity of region 2 is assumed to be zero, which, in the ideal case, is true for axial currents. The region is assumed to be homogeneous to the passage of flux. The thickness of region 2 is d.

The loss is given by equation 19 where

$$R=\frac{q_t(1-\tanh^2 ad)(1-\tanh^2 ag)}{\left[\tanh ag+\mu_2 q_r \tanh ad \tanh ag+\dfrac{1}{\mu_2}\times \tanh ad+q_r\right]^2+[\mu_2 q_t \tanh ad \tanh ag+q_t]^2}$$

This equation for R may be plotted as in Figure 2 of the paper, provided the ratio d/g is fixed and μ_2 is fixed. For $a/g = 0.1$, $\mu_2 = 50$ the $g/l = 0.01$ contour (about the order of the fundamental) shows practically no change; the peak of the 0.05 contour is reduced to two thirds of the value shown in Figure 2; and the peak of the 0.20 contour is reduced to one fifth.

Because tests show that the reduction of loss from the relatively coarse grooving is definitely less than is indicated by calculations for the ideal case, the calculations for the grooved rotor were omitted from the paper. All the test values reported in the paper apply to machines with rotors grooved with square screw threads. Though the benefits of grooving with a coarse thread are appreciable, they do not now appear to warrant any additional complication in the method of calculation.

The Quadrature Synchronous Reactance of Salient-Pole Synchronous Machines

Discussion and authors' closure of paper 43-89 by R. V. Shepherd and C. E. Kilbourne, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 684–9.

C. J. Fechheimer (Louis Allis Company, Milwaukee, Wis.): The maximum-lagging-current test, being dependent upon the reluctance torque, would usually imply that the field member has salient poles. With the round-rotor construction, as used in turbine generators, there are relatively large central poles with slots and teeth adjacent to them. There is still some reluctance torque with this construction, and I would inquire of the authors whether their maximum lagging-current-test method is applicable to turbine-generator construction.

R. V. Shepherd and C. E. Kilbourne: As Mr. Fechheimer points out, the maximum-lagging-current test depends upon the machine being able to develop reluctance torque, but it should also be remembered that the success of this method demands that the load angle at pull-out must be small. It is for this condition that $\sin^2\delta$ is negligibly small and equation 28 reduces to equation 29 from which the simple relation $X_q = E/I_1$ obtains.

If we limit the pull-out angle to 15 degrees or less, the curves of Figure 8 of the paper show what friction and windage power (P_1) are allowable for a machine with given reactances. For values of X_q/X_d approaching unity the value of P_1 must become vanishingly small to limit the pull-out angle to 15 degrees. Typical values for turbine-generator construction indicate that this condition is not usually met; hence, the method would not generally be satisfactory.

It may be interesting to point out that when an ideal salient-pole machine is operating at no load and zero field current the axis of stator magnetomotive force is in line with the direct axis, and the stator current is, neglecting saturation, e/X_d. As negative excitation is applied, the effect is to decrease the reactance seen by the stator in the direct axis. When the negative excitation is increased to the point where the apparent reactance in the direct axis is decreased to a value equal to X_q, the machine becomes essentially a round-rotor machine as seen by the stator, and becomes unstable. This point of view is modified easily to account for the small load angle necessary to overcome friction and windage losses and gives a good physical picture of the phenomenon. Of course, after the machine has slipped a pole the stator again "sees" the saliency of the rotor, since it is operating with positive excitation in its new position.

Distribution Factors and Pitch Factors of the Harmonics of a Fractional-Slot Winding

Discussion and author's closure of paper 43-100 by M. M. Liwschitz, presented at the AIEE national technical meeting, Cleveland Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 664–6.

C. G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Professor Liwschitz has presented an interesting paper on a subject which seems to intrigue the mathematically minded. Other papers on this same subject have been presented by Malti and Herzog[1] and by Calvert. Incidentally, Professor Liwschitz demonstrates the truth of the more or less well-known but frequently forgotten truth that the distribution factor for the principal harmonic of a fractional-slot winding having N/B slots per pole per phase is the same as the distribution factor of a machine having N slots per pole per phase.

Since Professor Calvert published a rather extensive prior treatise on this subject, I believe it would be helpful to point out here some of the similarities and some of the differences between the two papers which might not be at once obvious to one studying both papers.

One difference is the definition of "fundamental wave." Professor Calvert defines the fundamental wave as the one having a length equal to one full repeatable group, whereas Professor Liwschitz always makes the fundamental a two-pole wave. For example, consider a machine having 20 poles and 96 slots. If the procedure of Professor Liwschitz is followed, the principal flux wave (20 poles) or synchronous harmonic is referred to as the tenth harmonic. The magnetomotive-force waves in this machine are similar to those in a ten-pole 48-slot machine, but, in the latter case, the synchronous harmonic is of the fifth order. Professor Calvert would consider the synchronous harmonic of both machines as of the fifth order; the fundamental wave would have four poles in the 20-pole machine and two poles in the 10-pole machine. Both systems of definition have their own respective merits, but it probably would be helpful if all would define "fundamental" the same way. Professor Liwschitz, however, does assign the number ν to each harmonic, and the value of this number appears to be what Professor Calvert calls "order of the harmonic." My personal preference would be to call ν the "order of the harmonic," for the only harmonics that can exist are those for which ν is a whole number.

Professor Liwschitz finally arrives at the three equations, 15, 16, and 17, for computing the distribution factors of the various harmonics. (Of interest is the fact that these equations are in terms of ν, not n'.) The author helpfully illustrates the use of these formulas by giving numerical examples. Attempts to use these formulas when results were compared with those given by Professor Calvert in his Iowa State College *Bulletin* 142 brought to light some interesting facts:

To compare distribution factors, one must remember that Liwschitz' ν corresponds to Calvert's n. In the author's example of $N=11$, $\beta=4$, Liwschitz' "second harmonic" is Calvert's first, and Liwschitz' "eighth harmonic" is Calvert's fourth.

When β (Calvert's N) is an even number, the Liwschitz distribution factor is twice the Calvert distribution factor. This difference is again a matter of definition, because Calvert uses his distribution factor differently. My personal preference would be for the Liwschitz definition in this case, because his distribution factor is approximately 0.96 for the synchronous harmonic in all cases, whereas Calvert's distribution is approximately 0.48.

Apparently, equations 15, 16, and 17 give the correct magnitudes of distribution factors for most practical cases, where triple harmonics (harmonics of order that ν is a multiple of three) do not occur. However, triple harmonics do have an effect on the zero-sequence reactance of synchronous machines. For such cases the simplified equations yield distribution factors half of what they apparently should be. In other words, if ν is a multiple of 3, the numerator of equations 15, 16, and 17 should be 1.0 instead of 0.5.

Algebraic signs of distribution factors are probably of more academic than practical interest, but it might be observed that the algebraic signs obtained from equations 15, 16, and 17 do not always check those given by Calvert.

The fact that the three final equations given by Professor Liwschitz are not applicable to triple harmonics, unless the numerator is taken as 1.0, and the fact that they do not always yield correct algebraic signs, is not caused by any weakness in Professor Liwschitz' slot-star method, but it is apparently a natural result of the simplifying assumptions which made the numerators of all three equations equal to 0.5.

Professor Liwschitz' equations 20 and 21 for the pitch factors prompt a question. For integral-slot windings there are two common expressions for the pitch factor of a coil having a throw of ρ electrical degrees

$$K_{pn}=\sin\frac{n\rho}{2} \tag{1}$$

$$K_{pn}=\cos\frac{n(180-\rho)}{2} \tag{2}$$

The first equation yields correct magnitudes for the pitch factors for all harmonics but not the correct algebraic signs. Equation 2 gives the correct sign and magnitude of the pitch factor for all odd harmonics. Professor Liwschitz' pitch-factor equations have the form of the first equation (particularly, see his equation 18), which is known and acknowledged not to be correct for all algebraic signs. Would not the following form of equation for pitch factor yield both correct sign and magnitude for all odd values of ν?

$$Kpn=\cos\nu\left(90-\frac{\rho}{2\nu_s}\right)$$

where

ν_s = value of ν for synchronous harmonic
ρ = coil throw expressed in electrical degrees of the synchronous harmonic

REFERENCE

1. FRACTIONAL-SLOT AND DEAD-COIL WINDINGS, Michel G. Malti, Fritz Herzog. AIEE TRANSACTIONS, volume 59, 1940, pages 782–94.

M. M. Liwschitz: The use of a single reference wave for all machines in connection with the slot diagram makes it possible to derive simple formulas for the distribution factors of fractional-slot windings by simple reasoning, whereas the use of different reference waves in connection with charts leads to rather extensive mathematical derivations. Through the use of a single reference wave and of the slot diagram, the problem becomes a simple vector problem rather than a complicated mathematical problem.

The remarks made by Mr. Veinott concerning the signs of the distribution factors and the magnitudes of the triple harmonics are based on a misunderstanding. Mr. Veinott assumes that equations 15, 16, and 17 of the paper are the final equations for the distribution factors. The final equations for the distribution factors are the equations 10 and 11. These equations give the correct magnitudes and correct signs for all harmonics. Equations 15, 16, and 17 are simplified equations determined to meet the problems which the engineer usually encounters in practice; in these problems neither the signs of the harmonics nor the triple harmonics are of importance (in order to avoid that the same error be made by other readers, a corresponding remark has been made in the proofs).

With regard to the question by Mr. Veinott of whether it would be advisable to modify formula 2 of his discussion for the fractional-slot windings in order to get the right signs, I would advise not doing this for two reasons:

1. This formula is limited to odd harmonics only and can be applied neither to the fractional-slot windings with even β nor to the chorded 120-degree phase-belt windings.
2. There is no necessity of using this equation (even for the 60-degree phase-belt winding, where it gives correct signs).

The Fourier series, when consistently applied, yields the right signs for all cases. So, for example, for the odd harmonics of the electromotive force it yields the factor

$$k_n=\sin\frac{n\zeta}{2}\sin n\frac{\pi}{2}$$

which gives the same signs as formula 2. The first multiplicand of the factor k_n is the expression given by formula 1 of Mr. Veinott's discussion. It is clear that without the factor, $\sin n(\pi/2)$, this formula can not give the right signs.

It is customary to call the factor which contains the coil pitch "pitch factor." The magnitudes as well as the signs of the pitch factor, as yielded by the Fourier series, are treated in my paper. The signs of the electromotive-force and magnetomotive-force harmonics which depend on different factors are not in the scope of my paper.

Critical-Material Conservation in Induction-Motor Manufacture

Discussion and author's closure of paper 43-82 by H. M. Hobart, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 549–52.

P. M. Lincoln (Therm-Electric Company, Inc., Ithaca, N. Y.): Mr. Hobart points out that the heat-resisting abilities of the usual insulations used in electric equipment—papers, silks, cottons, linens, and so forth—have increased during the last 40 or 50 years by something of the order of 15 to 20 per cent. This estimate agrees with my own experience which began some time in the early 1890's. Mr. Hobart further points out that this increase in the heat-resisting abilities of insulations has resulted in the ability to increase the ratings of motors and other electric equipment by some 30 to 35 per cent. Or, to put the matter in another way, the increase in the heat resistance of insulations that has taken place during the last 40 or 50 years has enabled us to decrease the weight of electric motors and generators by some 30 to 35 per cent. Mr. Hobart's proof of this is entirely satisfactory to me.

But why stop with a 15 or 20 per cent increase in the heat-resisting abilities of our insulations? There are at least three types of insulation that are practically heat proof. These are mica, asbestos, and glass. There may be other types with which I am not familiar. Mica, asbestos, and fiber glass are not particularly good as insulators, but they are heat proof. When I say "heat proof" I mean this in a relative sense. When these heat-resistance materials are used in electric equipment, it is necessary to use a certain amount of tape, varnish, or other materials which are non-heat-resisting. The heat-resisting portions of such a compound resistance material are unaffected by temperatures vastly higher than the maximum temperature to which modern resistance may be subjected, but the non-heat-resisting portions are eventually reduced to carbon by temperatures only slightly above those to which our modern insulations may be subjected. The reduction to carbon, of course, destroys the effectiveness of our insulation. If we could develop insulations which could withstand temperatures 50, 100, 200, 300, 400 per cent, or perhaps even a higher percentage above the best of our modern insulations, think of the tremendous decreases in weight of electric equipment that might be accomplished. Please understand that I am not advocating this increase in the heat-resisting qualities of our insulations but am simply pointing out the possibilities. Any such modification in design must be taken gradually and no forward step made in the heat resistance of our insulations until it has been fully demonstrated that a further increase is safe.

If we attempt increases in the ratings of electric equipment in the manner suggested, there are many other problems that are sure to arise. Mr. Hobart's paper discusses one of these problems. When we decrease the weight of our motors by 30 or 35 per cent by using a better insulation, the problem of starting becomes important. Mr. Hobart proves—at least to my satisfaction—that with a decrease in motor weight of 30 to 35 per cent, the starting problem can easily be taken care of. This may not be the case when the decrease in weight is of the order of 300 or 400 per cent instead of 30 to 35 per cent. In this case, it simply becomes another problem for the electrical engineer to solve. And there are other problems that are sure to arise if we attempt to decrease weights by the amounts suggested. If we attempt to operate electric equipment at 300 or 400 degrees centigrade, each motor becomes a fire hazard. However, it is my belief that if the electrical engineer were confronted with that problem, he could produce a design that would be reasonably safe from a fire-hazard standpoint.

In my opinion a research on the means by which our modern electric equipment may be made safe for temperatures not limited to 115 or 120 degrees centigrade, but which may safely go to 200, 300, 400 degrees centigrade—or perhaps even higher—is one of the problems of research which should be undertaken at once.

H. Weichsel (Wagner Electric Corporation, St. Louis, Mo.): It is very timely to study means of reducing the requirements for critical materials. For induction motors, this is similar to a study of means of increasing the horsepower rating for a given frame size. One author quoted by Mr. Hobart suggests giving the motor a higher normal rating, that is overloading the motor. One of the disadvantages of this is that the starting torque expressed in per cent on the basis of the new full-load rating is decreased. Below it is proved that, contrary to this, the arrangement proposed by Mr. Hobart has the advantage that the starting torque expressed in per cent on the basis of the new full-load rating is increased.

The characteristics of a 15-horsepower four-pole 440-volt three-phase motor connected as proposed by Mr. Hobart will be analyzed. Figure 1 represents its circle diagram. The locus for the primary current when motor is operating without capacitors is the circle 0′–6′–10′. The starting torque is 63 foot-pounds = 145 per cent; the maximum horsepower equals 29.5 horsepower = 197 per cent. The full load corresponds to point 2′.

Parallel capacitors with 7.2 amperes leading current draw shift the full-load point to 2 corresponding to 100 per cent power factor. The locus for the line-current vector is the circle 0–6–10. A capacitor is in series with each line. Its voltage for load point 5 is represented by the vector $f-g$ and is a certain fraction of the vector $z-5$. The required line voltage is $z-g$. With varying load, the point g travels on a circle having a diameter

$d=D\cdot m$

$D=$ diameter of main circle

$$m=\frac{\text{length of voltage vector}}{\text{length of current vector}}$$

The top of the circle is a distance $a\cdot m$($a=$ distance $0'-z$) from the end of the motor voltage vector $z-f$.

Increasing the line voltage $z-g$ to the value of $z-f$ increases all vectors in the relation $(f-z)/(f-g)$, but the angle $\alpha=5-z-f$ remains fixed. The new line current in the direction of $z-5$ has a magnitude $z-5\times(f-z)/(f-g)$ (see Figure 2). This process repeated leads to points 1 to 10 (Figure 2) located on a circle.

The locus of the motor current vector is circle 0′ to 10′. The series-capacitor voltage vector has one end fixed at the point 254. Its other end travels on a small circle 1 to 10. Equal numbers on the small and big circle belong to the same load condition.

For the locked condition, the stator copper loss is line 11′–12′ (Figure 2), and the rotor copper loss is the line 10′–12′. The introduction of series capacitors increases the maximum horsepower from 29.5 to 39.5 (Figures 1 and 2). Thus the machine originally rated 15 can now be rated 20 horsepower. The starting torque is increased from 63 to 172 foot-pounds. The line starting current is increased from 86 amperes (475 per cent) to 125 amperes (520 per cent). Thus the addition of capacitors not only raised the maximum horsepower but also improved the starting conditions materially.

The full-load current at the original 15-horsepower rating is 18.1 amperes. When operating at 20 horsepower with series and parallel capacitors, the motor current is 23.6 amperes. Thus the stator copper loss is 1.7 times the loss of the original 15-horsepower machine. Figure 2 shows that for the 20-horsepower load the motor voltage is equal to that of the original machine at 15 horsepower. Consequently, the iron loss in the machine in Figures 1 and 2 is identical. If the machine was originally a 15-horsepower 40-degree machine, it can be expected, when equipped with capacitors, to have at 20 horsepower 65 to 70 degrees rise. This rise is satisfactory if the motor is wound with asbestos wire or its equivalent.

The diagrams discussed assume that each parallel capacitance has a current draw of 7.2 amperes with 254 volts corresponding to 75 microfarads, corresponding to a total of 5.49 kva per machine.

Each series capacitance is calculated to give the motor about 40 maximum horsepower. The line current is 21.9 amperes at 20-horsepower load. The series-capacitor voltage is 33, corresponding to 1,750 microfarads. The series capacitors handle 2.16 kva per machine at 20-horsepower load.

Thus a total rated capacitor capacitance of 7.65 kva per machine is required to increase the motor rating from 15 to 20 horsepower, provided starting conditions are disregarded.

Figure 2 shows that under starting conditions the parallel-capacitor voltage (vector z-10 on the small circle) is 410/254 = 1.6 times the voltage rating of the capacitor, a quite satisfactory ratio for commercial capacitors.

The series-capacitor voltage at starting is 570 per cent of normal, because the starting current is 570 per cent of normal. Commercial capacitors are tested for one minute with two times rated voltage. To withstand the starting condition, a capacitor with a normal rating of at least 285 per cent of its full operating voltage is required.

Series capacitors rated for normal operating voltage can be used, provided they are short-circuited or shunted during starting. When current transformers supply the series capacitors, the transformation ratio can be altered during starting when it is desired to obtain decreased series capacitor starting voltage.

It was mentioned that the locus for the line as well as the motor current is a circle. This follows from the equivalent diagram of an induction motor. By adding to this diagram series- as well as parallel-capacitive reactance values, the series and parallel reactances of the diagrams are changed. Only the numerical values of reactances are altered without altering the principle of the diagram. It is known that the locus of current drawn by said equivalent circuit is a circle.

From diagrams Figures 1 and 2, it follows that the voltage conditions on the series capacitors become more favorable when the machine is inherently a poor power-factor machine as a slow-speed induction motor has inherently a lower per cent starting current and starting torque.

Let it be assumed that the no-load saturation curve of the 15-horsepower motor is

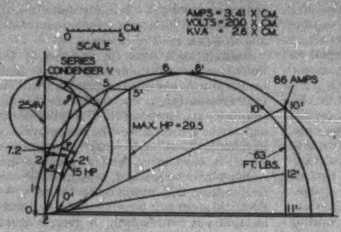

Figure 1 (left). 15-horsepower 60-cycle 440-volt three-phase motor with series and parallel capacitors

Motor voltage held constant

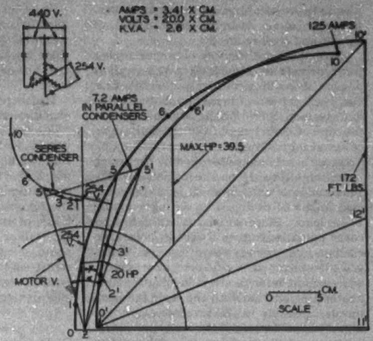

Figure 2 (right). Arrangement same as in Figure 1 but line volts held constant

Full load of motor is now 20 horsepower

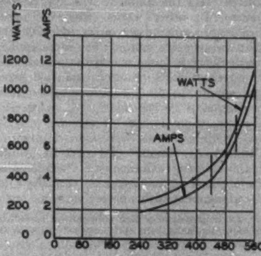

Figure 3. Idle amperes and "constant" losses as function of impressed voltage

given by Figure 3 and that the 15-horsepower machine is rewound with 86.5 per cent of the original turns, corresponding to a maximum horsepower output of 39.5 provided the copper section per slot remains unchanged. The starting current increases from 86 to 115 amperes while the per cent starting torque remains the same. Because of saturation, the power factor at 20 horsepower is materially lower than for the original machine at 15 horsepower. When the original machine has a highly saturated iron circuit, its rewinding for higher horsepower may force the induction to excessive values, making the machine a commercial impossibility. On the other hand, if the original machine is operated with capacitors, the saturation for normal operating conditions does not change materially; consequently an increase of its rating is possible.

Table I

	Original 15-Horsepower Motor		Modified to Give 20-Horsepower Full-Load Output by: Rewinding With 86.5 Per Cent of the Original Number of Turns		Using Series and Parallel Capacitors	
Full-load amperes	18.1		25.6		21.9	
Full-load power factor	92	per cent	88	per cent	99.9	per cent leading
Locked current amperes	86		125		125	
Locked current, percent of full load	475	per cent	450	per cent	570	per cent
Locked torque (foot-pounds)	63		86		172	
In per cent of synchronous horsepower torque	145	per cent	145	per cent	290	per cent
Maximum horsepower (full load)	195	per cent	195	per cent	197	per cent
Stator copper loss (watts) full load	434		670		730	
Rotor copper losses (watts) full load	640		850		1,140	
Constant losses (watts)	508		780		508	
Total losses (watts)	1,582		2,300		2,388	
Output (watts)	11,200		14,900		14,900	
Input (watts)	12,782		17,200		17,278	
Efficiency	88	per cent	86.5	per cent	86.4	per cent
Total parallel-capacitor kilovolt-ampere rating					5.49	
Total series-capacitor kilovolt-ampere rating						
(a). Capacitor in circuit during starting					6.2	
(b). Capacitor cut out during starting					2.16	

The numerical values shown in this table hold true only for the particular machine investigated.

If the original machine is a poor power-factor machine (high number of poles), all conditions become more favorable for the machine using series and parallel capacitors, and the conditions become materially poorer or even impossible for the rewound motor. In every case an analysis should be made to determine if rewinding the motor with decreased number of turns or the addition of series and parallel capacitors gives the more desirable results.

The outstanding results are listed in Table I.

Walter L. Upson (Torrington Manufacturing Company, Torrington, Conn.): Mr. Hobart's very interesting paper is aimed at meeting a temporary need. We are inclined to question the substantial gains to be effected in savings of materials and would ask the author to compare savings of copper with correspondingly increased demands of metal for the six capacitors required for each motor.

On the other hand, this paper suggests a peacetime improvement which may be particularly pertinent to postwar progress.

Induction-motor design has changed measurably since 1924, the date of the original proposal mentioned by Mr. Hobart, and, during the intervening period capacitors also have undergone considerable development. The application of condensers to induction motors has resulted in the so-called capacitor-type motor which has made a distinctive place for itself. Nevertheless, in spite of the 20-odd years of capacitor development for motor applications, and the more than 50 years development of the induction motor itself with its resulting high degree of commercial perfection, one may well question whether we have not reached a period favorable to another major advance in induction-motor design and practice.

Transformer practice, in spite of its apparently even greater perfection, seems to have made greater progress in development than the induction motor during this period.

Mr. Hobart mentions improved performance with the application of capacitors by which terminal voltage is variable with load. This variation has the effect of varying the points of maximum efficiency and power factor, keeping them closer to the load position, which, in itself, benefits performance.

In future developments this relationship of efficiency to load deserves careful study along with such obvious considerations as the use of improved core materials and insulations, better ventilation, and more compact spacing.

Going beyond these is the possibility of applications which may profit by use of a frequency greater than 60 cycles, in which capacitors would become even more commercially practicable, high-power-factor performance more desirable, and weight of copper and steel reduced.

Since there are ways of obtaining higher frequencies from 60-cycle circuits, as well as *de novo*, it would seem highly desirable that the whole subject of induction-motor design and system operation be studied with a measure of detachment from the limitations of standard practice.

One other thought provoked by the paper bears more directly on the subject of wartime saving of materials. It is that greater care might be exercised in suiting a motor to its load. Since standards are meant to cover operations up to a certain point, for example, continuous operation in 104 degrees ambient, if a 75-horsepower motor were required for a given installation; in another installation with the same full load torque, possibly a 50-horsepower motor would be satisfactory if the load were intermittent and the ambient was appreciably lower. In other words, wherever practicable, recommend use of an under- rather than an over-size motor. The same applies particularly to cases where the actual load requirement falls between standard sizes and where the danger of excess load is a minimum.

Comfort A. Adams (Edward G. Budd Manufacturing Company, Philadelphia, Pa.): The author suggests two steps in the attainment of his objective:

1. The employment of high-temperature insulation in order to make safe the proposed increase in rating.
2. The employment of both series and parallel capacitors to increase the stalling torque and to improve the over-all power factor.

The first step has been and is being increasingly employed to the proposed end.

The employment of parallel capacitors for power-factor correction is also a familiar device, the use of which is usually determined by economic considerations, although there are doubtless many cases where they are not now used, but where such considerations would dictate their use, particularly with the present price and quality of the capacitors.

However, the additional use of series capacitors for automatic voltage control, although fully explained in the author's patent of 17 years' standing, has not been employed to any appreciable extent, so far as the writer is aware.

Since there can be no question as to the author's general claims concerning the advantages, assuming the proper choice of the capacitors for both the series and parallel groups, the question naturally arises as to why the series capacitors have not been employed. It is thus unfortunate that the author did not present more complete data as to the capacitance of the series capacitors and as to their cost, rather than stating their kilovolt-amperes for the one example cited. The cost per kilovolt-ampere at the relatively low voltage usually required for series capacitors, would certainly be considerably greater than for those operating at the higher and more normal voltages." This would be particularly true in this case, since it seems likely that the series capacitors would have to be designed to stand transient voltages at least equal to the rated voltage of the motor.

In other words, is it not likely that the kilovolt-ampere rating of the series capacitors would have to be seven or eight times their working load, or practically equal to the kilovolt-ampere rating of the motor itself?

If this reasoning is sound, it may be the explanation of the lack of use of this device.

If, however, the assumptions are not correct, and if the cost of these series capacitors per kilovolt-ampere of the actual load which they carry is no greater than that of capacitors for the normal voltage range, there would be no question as to the desirability of employing this device for raising the stalling torque of induction motors to a point commensurate with the increased rating of the motors themselves, made possible by higher-temperature insulation and high operating temperatures.

S. D. Sprong (consulting engineer, New York, N. Y.): It occurred to me that many would be interested in knowing approximately the prorata cost per horsepower of the additional capacity brought about by your application of series and shunt capacitors. Obviously this horsepower increment would be relatively low as compared with the original installation, and figures covering this might give added emphasis to the commercial side of the problem.

The second thought that occurred to me is whether many of the present commercial motors would have sufficient leeway in the stator laminations to utilize advantageously the increase in applied voltage from 480 to 555 without approaching too closely the knee of saturation curve. Probably your calculations have already taken this factor into account, but I was not able to read into the paper anything covering this point.

Speaking in general terms the overmotoring has been largely due to the purchasers for not predetermining more exactly the power requirements and duty cycle required of the motors. It was probably easier to allow a generous margin of safety for this undetermined factor, contingencies, and so forth, and possibly increase of tool speed as the result of the use of special tool steels, and further the addition of added tools where they were gang-driven. Referring particularly to gang drive there are usually some units not essential to continuous operation and can therefore be dropped off automatically for a time if the coincident load exceeds the safe motor capacity. This has been done successfully in several installations that I happen to know about. It permitted a close measure of motor capacity to essential demand and also kept down the demand charge where this entered into the power-rate contract. It occurs to me that this general idea might still be applied in some installations where motor capacities have about reached the limit.

C. J. Fechheimer (The Louis Allis Company, Milwaukee, Wis.): We have heard recently recommendations for operating electrical machinery at higher temperatures. It is generally assumed that, if the insulation will not fail, it is safe to operate the apparatus at high temperatures. It is also assumed that the higher the temperature the smaller the machine, with a corresponding reduction in weight. It is my desire to call attention to a fallacy in these assumptions, and to add a word of caution.

Entirely aside from the question of the insulation being suitable to withstand high temperatures, we must remember that, as the temperature is increased, the copper resistance and therefore the I^2R is increased, and as the I^2R increases the temperature is increased. Thus, with constant current, the whole is cumulative, and a point of instability may be reached, beyond which the temperature will never cease to rise. As electrical apparatus is usually operated, such instability is not reached. A general figure as to the point of instability can not be given, as it involves the constants and proportions associated with the thermal drop through the insulating wall. If all heat due to I^2R flows transversely through the insulating wall for the part of an armature coil embedded in the iron, then the drop through the wall is given by equation 13 in my paper, "Longitudinal and Transverse Heat Flow in Slot-Wound Armature Coils," in the 1921 AIEE TRANSACTIONS,[1] as follows:

$$\theta = \frac{K\theta_i + \epsilon I^2R}{K - \epsilon I^2R\alpha} = \text{copper temperature above zero degrees centigrade}$$

where

$$K = \frac{\text{transverse heat conductivity of insulation} \times \text{mean coil perimeter}}{\text{insulation thickness}}$$

θ_i = iron temperature above zero degrees centigrade

ϵ = eddy factor in copper conductors

I^2R = loss in copper per coil per unit length axially at zero degrees

α = temperature coefficient of resistance of copper at zero degrees (0.00427)

Evidently, the conditions of instability are secured when the denominator equals zero. Then

$$\text{Critical } I^2R = \frac{K}{\epsilon\alpha}$$

Assume, as a typical case, in an induction-motor slot

$$\text{Transverse heat conductivity of insulation} = 0.0025 \frac{\text{watt}}{\text{inch} \times \text{degrees centigrade}}$$

Mean coil perimeter = 2 inches

Insulation thickness = 0.04 inch

ϵ = eddy factor = 1

Then

$$\text{Critical } I^2R = \frac{0.0025 \times 2}{0.04 \times 1 \times 0.00427} = 29.3 \text{ watts}$$

The drop through the insulating wall is then

$$\frac{29.3 \times 0.04}{2 \times 0.0025} = 234 \text{ degrees for the } I^2R \text{ loss at zero degrees}$$

As the temperature rises, the drop increases so that the drop would be considerably in excess of this value. An experiment on a d-c shunt-field coil some years ago indicated that at around 350 degrees centigrade gases were generated sufficiently rapidly to cause the taping around the coils to be blown off. The temperature was then mounting at a rapid rate.

But the engineer should so proportion his apparatus that the temperatures attained should be considerably below the point of instability. For, as temperatures mount, so do the I^2R losses, and there is little gained by running at high temperatures, from the standpoint of weight and size. Also, in the case of the armature for a motor, the higher temperatures imply reduced efficiency, and therefore more current and greater I^2R.

Would it not be well to reconsider this question, before we allow the pendulum to swing too far in the direction of very high temperatures?

REFERENCE

1. LONGITUDINAL AND TRANSVERSE HEAT FLOW IN SLOT-WOUND ARMATURE COILS. Carl J. Fechheimer. AIEE TRANSACTIONS, volume 40, 1921, pages 589–645.

H. M. Hobart: Professor Lincoln's plea for research on the means by which our modern electrical equipment may be built for satisfactory operation at temperatures much above any to which users are at present accustomed, should be heeded. It is over 30 years ago that Professor Lincoln, Doctor C. A. Adams, and a good many other AIEE members (most of whom are no longer with us in this world), undertook the root and branch revision of this country's electrical-engineering standards. This effort at thorough revision initially was undertaken as an AIEE project, it being felt that a professional body best could guide to success a task of this kind; all sections of the industry being called upon for advice and assistance as occasions arose. We strove to encourage the greater use of mica and other inorganic materials suitable for withstanding high temperatures, and we urged the assignment of considerably increased values for the permitted service temperatures. But the state of the art, and the non-availability, in those days, of suitable heat-withstanding materials for impregnants, adhesives, gums, and varnishes provided the opposition with too many arguments which we could not refute successfully with the result that, even with such stuff as mica, asbestos, lava, and other inorganic substances, the temperature limits to which we could obtain agreement, were decidedly moderate. But the enormous progress which since has resulted from researches on these subjects, now—after 30 years—demands prompt reconsideration. However, instead of attempting the formidable and bewildering task of straightening out the limiting-temperature situation, does it not seem preferable completely and sweepingly to rescind all temperature limitations and simply require of the manufacturers the provision of products with satisfactory life expectancies when employed for their intended purposes? If left to their own responsibilities, manufacturers will proceed to employ materials and constructions of their own choosing, and progress and improvement will be rapid. A manufacturer will employ for each part the material which his experience approves and will so design his product that each part shall withstand in service, for a satisfactory life period, the temperatures to which it will be subjected. In the present standards (which, absurdly enough, show but trifling advances over, or departures from, those established 30 years ago), a fundamental principle of helpful standardization was transgressed in that the manufacturer's freedom was restricted with respect to the means by which he should obtain his object. Fortunately, the will to research resists all discouragements. Each succeeding year, research has put us in possession of more complete mastery of the art of providing insulating materials which successfully withstand subjection to the high temperatures the use of which is often the best means of securing important characteristics desired in various electrical products, such, for example, as lightness, compactness and low cost.

With respect to the kind of insulation employed and the temperature attained, it is reasonable and desirable that the manufacturer shall utilize in his product the advantages accruing from the availability of any superior insulating material or insulating process which he has developed which permits of a higher temperature for a given degree of reliability. To restrict this freedom has the same retarding effect on progress toward better products as would any restrictions to his use of higher permeability castings or lower core-loss laminations or skillfully devised transpositions of conductors. Thus each manufacturer should employ his own best judgment in applying to his product the rating at which he arrives by his special knowledge of its features and characteristics. The manufacturer will supply information as to the continuous or short-time output of his machine at reference conditions and as to the overloads and underloads for which he considers it suitable with service conditions less and more favorable than the reference conditions.

Far less interest, however, should be attached by the user, to the corresponding temperatures of the insulating materials, since responsible manufacturers will not resort to any temperatures not compatible with the properties of the insulations they employ. A manufacturer will be required to produce (with the aid of properly accredited testing laboratories and licensed proving establishments) satisfactory evidence that the materials and constructions which he employs in a given product will not be associated, in the service to which it will be applied, with an annual deterioration of the insulation, greater than corresponds with the agreed life expectancy. The object in view is to limit not the temperature rise but the rate of deterioration of the insulation and other parts. Standardized temperature limitations may have been desirable 30 years ago, at the then state of the art. But it has now led to an undesirable discouragement of initiative, to penalizing the use of the best products, and to placing unjustified confidence in inferior products. In other words, a product intelligently built by an experienced manufacturer employing insulation defined as of a certain class may be more reliable at a temperature rise much above the standardized limit for that class, than another less excellently built product would be when operated at a temperature rise much below the standardized limit.

In his very interesting discussion, Doctor Fechheimer quite rightly emphasized that we should not employ temperatures so great that the rate of increase in the I^2R loss will lead to danger. However, Professor Lincoln was not contemplating any approach to such temperatures in the windings. But by the employment of suitable thermal barriers, we can, in certain designs, advantageously so shield the windings from the laminated magnetic-core material, that the latter's temperature may exceed the winding's temperature by 100 degrees centigrade or more. By such constructions it should be practicable to obtain the advantage of considerable decreases in the hysteresis loss which occur at high temperatures, and to which attention was drawn by the researches carried through by MacLaren in 1911 and 1912.

Doctor Weichsel's contribution to the discussion is so extremely interesting and valuable that it is regrettable that it should be consigned to the rarely read records of Institute discussions. While the author is very proud and encouraged with respect to his own efforts, in realizing that the presentation of his paper should, in part, have been the reason that Doctor Weichsel undertook this fine piece of research in induction-motor design along new lines, it is also his very earnest wish and hope that Doctor Weichsel will make his important findings more promptly accessible to his professional associates and to the entire electrical industry, by embodying the material (suitably and considerably amplified to conform with its importance), in an independent paper of his exclusive authorship, which shall be presented at an early date, on the occasion of an Institute meeting.

Professor Upson puts forward several interesting suggestions, particularly that concerning the increased commercial practicability of capacitor hookups when associated with higher periodicities. It may be interesting to point out that, with the use of higher circuit periodicities, the trend will be toward lower densities in the magnet cores. This bears on a point raised by Mister Sprong who rightly indicates that the approach to saturation in the stator cores imposes a limit to the amount by which (in the proposed hookup), the full-load voltage may be increased above the voltage at light loads. Consequently, with higher periodicities, we may go further with that feature of the project than with the present usual frequency of 60 cycles per second.

The fine quantitative treatment of the problem, for which we are indebted to Doctor Weichsel, provides the kind of information which Doctor Adams justly reproaches the author for not including in his paper. That it was not undertaken by the author was not due to laziness but to his reprehensible ignorance of some aspects of the subject. This applies also to ferroresonance, about which phenomenon the author is likewise completely ignorant. He is informed by a capacitor specialist that, "before the scheme could be exploited," both ferroresonance and the influence of "regions of self-excitation" will require special study. Since this specialist may be right, it is the author's hope that the relation of these phenomena to his induction-motor project, will be given the suggested further study, by someone. Indeed, the specialist was earnestly urged by the author to contribute his knowledge and advice to be embodied in the present discussion. But a letter from him states: "I do not feel that I have anything of sufficient interest to warrant a written discussion for the TRANS-

ACTIONS." It is the author's hope to learn, before long, of some actual trial, somewhere, of this induction-motor project, and of the attainment of good results. It is, however, only reasonable to expect that some troubles will be encountered and overcome, as with all excursions into new fields. A complete exception would be a very agreeable surprise.

In conclusion, let us return for a moment to Professor Lincoln's very timely recommendation to turn our backs on present outmoded temperature practices and examine, in the light of up-to-date knowledge and the industrial needs of the present and the immediate future, plans for a general resort to very much higher temperatures in the service operation of electrical machinery. Attention especially should be drawn to the enormous present and future demand (running literally into many millions of electric motors per year) for motors with ratings of fractions of a horsepower and a few horsepower each, as well as for an unprecedently great number of larger motors. On page 88 of the July 1943 issue of *Electrical Manufacturing*, in a particularly valuable article by Lieutenant Colonel T. B. Holliday, we learn that "in the large airplanes which are to come in the future" the number of electric motors required will approach 100 per plane. To keep down the weight high periodicities of the order of 400 cycles per second, and speeds of many thousands of revolutions per minute usually will be resorted to. All the old conceptions of permissible temperatures will be inapplicable, and all insulations will be inorganic or the equivalent as regards their ability to withstand far higher temperatures than heretofore have been deemed practicable. These aircraft motors will receive their current from generators of extremely light weight, often through interposed lightweight high-temperature transformers.

It is perfectly evident that the consequent shake-up to established conceptions and accepted standards will be utter. The result of demonstrating the possibilities of radical improvement in electric equipment for aircraft inevitably will be that comparable high periodicities and speeds and temperatures promptly will become usual in other fields, instead of being exceptional. This will apply not only to motive power for embodiment in machine tools in workshops, but also in the motors and transformers which will be required by the millions in domestic appliances of all kinds, and in agriculture. In other words, the electrical age definitely has arrived on a great big scale, and much of the equipment, which, heretofore, has been regarded as reasonably adequate, now must be promptly superseded by equipment embodying up-to-date conceptions and constructions, and in which notable advances will be employed as regards high periodicities, much higher service temperatures, very high speeds, lighter weights, and lower costs. These changes will be effected without involving any sacrifices in the efficiencies or life expectancies.

A New Type of Adjustable-Speed Drive for A-C Systems—II

Discussion and authors' closure of paper 43-83 by A. G. Conrad, S. T. Smith, and P. F. Ordung, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 522–5.

C. G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): The authors have recognized the practical difficulty of operating a shunt motor on alternating-current because of the approximate quadrature displacement of the armature current from the field flux. They solve this difficulty by the use of a polyphase supply circuit. This paper reminds the writer of his own intensive efforts to solve this problem 13 years ago using only a single-phase source of power. I attempted to get around the phase-displacement difficulty by using a capacitor in series with the field, making a series-resonant circuit in which the field current would be in phase with the impressed voltage. This arrangement did not work very well, because the short-circuit currents in the coils undergoing commutation, being inductively coupled with the main field circuit, interfered with the resonant circuit and gave rather erratic results. It is probable that the use of a polyphase power source would eliminate this difficulty.

C. W. Drake (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): I have been very much interested in both parts I and II of these papers, but more so in part I presented at the AIEE national technical meeting, New York, N. Y., January 25–29, 1943 in which the diagram of connections and speed-torque curves was given. Adjustable-speed characteristics as shown, together with regenerative torques, are required by a large number of industrial applications, and such characteristics on a-c motors have been sought ever since the induction motor was first developed.

From year to year, various designs of a-c commutator motors have appeared in this country, but so far no one type has obtained what we would call general acceptance by the trade. This may be due in part to the relative price, performance, or electrical complications of the motor and control equipment; but, since many motors of similar design are used in Europe, we think that the lack of acceptance here is attributable chiefly to the difference in the temperament of the American and European electricians or maintenance men. There is no question about the satisfactory operation of certain types of these motors abroad and in some plants in this country, but, in general, maintenance men here are not trained to, or in most cases are not agreeable to, the frequent and careful inspection required by this class of apparatus in order to insure satisfactory operation. The tendency here seems to be toward equipment which will require attention only at very infrequent intervals, and if the design described in this paper will measure up to these requirements it will undoubtedly overcome many of the limitations of previous types.

In Mr. Veinott's discussion he mentions work he did along these lines 13 years ago, but we also find that B. G. Lamme in 1911 disclosed before the AIEE a design quite similar to the one described by the present authors and mentioned that two motors of 20 and 30 horsepower were built.[1] The 20-horsepower motor was installed in an industrial plant and operated satisfactorily for 18 months, after which it was taken out because of a change in manufacturing conditions. These particular motors operated on a 25-cycle system, so that commutation was much less of a problem than it is now for 60-cycle service.

REFERENCE

1. Discussion by B. G. Lamme of METHODS OF VARYING THE SPEED OF ALTERNATING-CURRENT MOTORS. AIEE TRANSACTIONS, volume 30, 1911, pages 2487–90.

A. G. Conrad, S. T. Smith, and P. F. Ordung: Mr. Veinott's explanation of the shunt-a-c motor, with its field excited in series with a capacitor to establish resonance, checks precisely results obtained by other investigators using such circuits. He mentions that the effect of the change in currents in the short-circuited coils with different values of load interferes with the desired resonant condition. Another factor that causes such inteference is the shift of the neutral axis of commutation with change in load. These two factors cause a phase shift of the field flux with respect to the armature voltage as load is changed. This results in large changes in power factor and poor commutation. With the polyphase supply described in the paper, these factors while present do not change the magnitude or direction of the field flux, and, consequently, better commutation is obtained.

Mr. Drake's discussion of reluctance on the part of American engineers and maintenance men to accept commutator types of a-c motors is accurate. Judging from the number and the nature of the inquiries received on the subject, the authors assume that the commutator-type motor is still quite a mystery to many engineers. It is not accepted because it is not understood. The reference to Mr. Lamme's work in 1911 has been covered in the closing discussion of part I of this series of papers.

The Cause and Control of Some Types of Switching Surges

Discussion and author's closure of paper 43-102 by T. W. Schroeder, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 696–700.

R. A. Hentz (Philadelphia Electric Company, Philadelphia, Pa.): It may be of interest to outline the part of the Philadelphia Electric Company system which is similar to the system outlined in the author's paper and to describe methods of operation followed in order to avoid high switching surges.

Two generating stations, *A* and *C*, are tied together by means of 66-kv lines. One of these lines recently installed (see Figure 1 of this discussion) passes through substation *B* and forms one of the two 66-kv "backbones" which extend practically throughout the system. It is separated from the other 66-kv "backbone," though the two are paralleled through reactors on 13.2-kv busses at various points. The new line consists of underground cable from station *A* to station *B* and aerial open wire from station *B* to station *C*.

The addition of this underground-cable section, which has a charging kilovolt-amperes of about 20,000, focused attention on methods of switching to insure minimum of switching surge. The problem was considered:

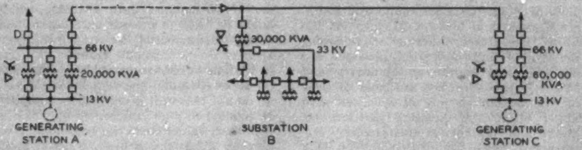

Figure 1

1. As to what would be done in the case of automatic switching for a failure on the 66-kv aerial line or cable constituting this tie.
2. What would be done in the case of manual switching where the time selected for this could be chosen.

In the case of automatic operation, 66-kv circuit breakers at stations *A* and *C* and the 33-kv circuit breakers on the low-voltage side of the transformer bank at station *B* are tripped by pilot-wire relay scheme, thus keeping the station *B* transformer bank, which is delta-connected on the 66-kv side, in circuit to absorb the charge of the cable in the manner indicated in Figure 3A of the author's paper. The only disadvantage of such a procedure is that it is slightly preferable to trip the 66-kv breaker on the station-*B* transformer bank, since the 33-kv installation is a ring bus, and tripping the two 33-kv breakers opens this ring. If this is the only tripping, this in itself is of no great importance, as all of the remaining lines and transformer banks remain connected together and energized from the 33-kv system; however, it would be of importance were there an operation on some other part of this 33-kv installation, as the ring bus would then be opened at a second point, thus creating two 33-kv parts. It is planned, however, that as soon as these 33-kv breakers on the transformer bank trip, the operator will open the 66-kv breaker and a disconnecting switch in the 33-kv leads from the transformer bank and then again close the 33-kv ring by reclosure of these breakers.

Where the switching may be done at the load dispatcher's convenience, the cable is de-energized by first opening the 66-kv circuit breakers at stations *B* and *C*, and circuit breaker *D* at station *A*. The low-voltage (13.2-kv) breakers on the three transformer banks at station *A* are successively opened, the supply to the 66-kv cable being interrupted with the opening of the third low-voltage breaker, leaving the three 20,000-kva transformer banks, which are wye-connected and neutral, solidly grounded on the 66-kv side, to absorb the surge as indicated in Figure 3B of the author's paper. The characteristics of the transformer banks at station *A* and station *B* and of the 66-kv underground cable between these two points are:

Station *A* transformers X_m = 11,300 ohms
Station *B* transformer X_m = 9,920 ohms
Cable X_c = 246 ohms

It will be noted that with the preceding characteristics the ratio of X_c/X_m is about 0.025, which is an unfavorable location for it to be, as indicated in Figure 5 of the author's paper. As an added precaution, line-type lightning arresters were installed at each end of the cable.

It is to be noted that all of the circuit breakers involved in either the automatic or manual operation of the 66-kv cable are of the oil type, though an air-blast breaker is to be installed to replace the oil breaker on the 66-kv side of the station-*B* transformer bank, which, as previously shown, does not enter into any operation while the cable is energized. Some thought was given to the effect of the air-blast breakers; but, if there was any advantage, which was doubtful, it did not seem enough to justify the expense of their installation. In fact, it may be that the rapid extinction of the arc, that is characteristic of the air-blast breaker, may create higher surges than those experienced in oil-circuit breakers where the extinction of the arc is slower.

Since this 66-kv line has gone in service, there have been two automatic operations. One of these was due to a fault on the aerial wire section between stations *B* and *C*; the second was caused by an operating error whereby the live cable was accidentally grounded at substation *B* by closing an air-break switch connecting the cable to the aerial wire section which had been blocked out for work and purposely grounded by tying the three phases together and to ground. Obviously, this ground connection offered a drain for the static charge of cable. Both of these faults were cleared satisfactorily, and there was no apparent disturbance.

E. L. Michelson (Commonwealth Edison Company, Chicago, Ill.): This paper is a valuable addition to the literature on switching transients. These papers take the mystery out of switching transients and reduce their analysis to a well-defined basis.

In Chicago our experience has shown the value of switching transformers with lines. A few severe cases of overvoltage have occurred when 66-kv underground cables were de-energized by opening the line breaker. The most severe cases occurred when the system supplying the line was ungrounded. As a result of this experience, present practice requires that whenever possible these lines should be de-energized by connecting the line in series with a transformer and opening the low-voltage switch on the transformer.

In connection with Figure 3A of Mr. Schroeder's paper, I wonder whether his studies on this setup would enable him to express an opinion on the following problem: In Chicago we have a few situations where a 12-kv three-conductor cable, approximately two miles in length, supplies directly a transformer bank of 7,500-kva in the manner shown by Figure 3A. A single-phase-to-ground fault on the cable will result in the line breaker being opened at the supply end of the system, but there is no provision for immediately disconnecting the transformer from the low-voltage bus for such a fault. This situation leads to a theoretical possibility of an arcing ground with resultant overvoltages on the cable and transformer, and I wonder whether Mr. Schroeder's studies would shed any light on the possibility of such overvoltages being obtained.

L. R. Gaty (Philadelphia Electric Company, Philadelphia, Pa.): The author has prepared an excellent paper giving us another tool to determine the magnitude of switching transients. These become particularly important in switching having high-capacitance kilovolt-amperes. The information in this paper was very helpful in studying the transient voltages which would result from switching a 66-kv cable, installed by Philadelphia Electric Company in 1942, having a capacitance charging kilovolt-amperes of 20,000. A delta-connected transformer bank was connected to one end of this cable and was found to be helpful in minimizing the possibility of high transients. Fortunately, switching could be done on either the high-voltage or the low-voltage side of this bank, and it is now arranged for low-voltage switching. Referring to Figure 5 it is interesting that in this case the ratio of X_c to X_m is about 0.025 giving close to the worst condition.

T. W. Schroeder: I wish to thank Michelson, Hentz, and Gaty for their discussions covering the practical application of some of the thoughts advanced in my paper.

The Philadelphia Electric Company's problem of switching some 20,000 kva of cable capacitance furthered the desirability of more definitely determining the effect of transformer windings on the capacitance-switching problem. Mr. Hentz indicates that the X_c/X_m ratio of 0.025, applying to this system, is in an unfavorable region, referring to Figure 5. While this is true from the standpoint of the ultimate maximum volts which would obtain across the switch, which would be about 2.5 times normal (against twice normal with no restriking in the absence of a transformer), it should be noted that the time required for the switch voltage to reach twice normal is about 1.3 cycles as against 0.5 cycle in the absence of a transformer. This latter is probably the more important consideration for an interrupter capable of interrupting the three phases reasonably simultaneously.

Mr. Michelson's experience, that "high-side" switching in an ungrounded circuit can result in more severe switching overvoltages than in a solidly grounded one, agrees with analyses and other experience.

In connection with the question raised by Mr. Michelson, the circuit he describes is probably not uncommon where transmission is largely at generator voltage. It is now generally believed that an arcing ground in a dielectric of free air is not capable of building up very high overvoltages, because the dielectric strength of the medium in the vicinity of the arc is decreasing rather than building up with time.

Interim Report on Application and Operation of Automatic Reclosing Equipment on Stub Feeders

Discussion of paper 43-84 by the subcommittee on a-c automatic reclosing equipment of the AIEE committee on automatic stations, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 536–40.

G. S. Lunge (General Electric Company, Schenectady, N. Y.): It is appreciated that to keep the length of the questionnaire reasonably short, it was not considered feasible to ask for various additional useful items of information concerning these circuits.

Perhaps one of the most important of such missing items of data is what percentage of the poles were equipped with protective gaps or arresters, and of what type? If these gaps were plain rod gaps, as distinct from expulsion-protector tubes, each time such a gap arcs over from lightning overvoltage, a circuit breaker, oil circuit recloser, or fuse operation will result, after which a successful initial reclosure should always be possible.

It will be obvious that these circuits will necessarily show a higher percentage of successful reclosures than they would if not equipped with plain gaps. Therefore, if it had been known which circuits had such protective gaps, it would have been logical to tabulate the data from such circuits separately, as the number of outages for such circuits will naturally be larger than, and bear no definite relationship to, the number of true line faults.

It would be interesting to know whether enough of the circuit mileage, for which data were received, had protective gaps (other than expulsion protector tubes) to have contributed materially to raising the percentages of successful reclosures to the values reported in Figures 2 and 3 of the report.

While advocating that consideration be given to wider application of immediate reclosing to motor circuits, and pointing out that to avoid losing the induction-motor load, reclosure must be completed within 0.5 second to 2 seconds, it will be noted that the report does not state whether immediate reclosure of motor circuits, without any material time delay, is advisable. Although none of the proposed American Standard *C*37.7 circuit-breaker operating duties for reclosing service mention intentional time delays less than 15 seconds, reclosing relays are available that will permit total de-energization times within the 0.5- to 2-second range advocated. Nevertheless, in the interest of permitting the use of still simpler reclosing relays, it would be of interest to know whether the use of reclosing relays with no intentional time delay, that is, immediate reclosing, has met with widespread success in the case of circuits carrying an appreciable percentage of motor load.

J. A. Elzi (The Commonwealth and Southern Corporation, Jackson, Mich.): As Mr. Lunge has stated, information regarding the type of surge protection provided was not requested in the questionnaire, and data are, therefore, not available to show to what extent the use of open rod gaps would affect the reclosing performance. Moore and Watkins in their paper, "Experience With Oil Circuit Reclosers on REA Systems," did present such data, and the results of their survey would seem to indicate very little difference in the percentage of lockouts for the two types of protection.

Mr. Lunge has pointed out that a Rural Electrification Administration system, though nation-wide in scope, would have transformers on a small percentage of the poles as compared with other distribution circuits. This could very well account for the fact that Moore and Watkins found very little difference in the performance of the two types of surge protection. A review of the REA data as given in Tables IV and VII shows that trees are the greatest single cause of lockouts on REA systems. This would further account for failure to show any decided difference caused by the protective equipment. As Mr. Lunge has pointed out, it would seem reasonable to expect that on a distribution circuit on which plain rod gaps are used for surge protection a higher percentage of successful initial reclosure than the average values given in Figures 2 and 3 could be expected. The number of trip-outs per year probably also would be higher on these circuits than on circuits having transformers protected by lightning arresters or expulsion gaps.

With reference to the question as to whether or not immediate reclosure of motor circuits without material time delay is advisable, it might be stated that the 0.5-second interval given by some of the users was based on the length of time that a circuit would be de-energized using immediate reclosure, the time interval being simply the time required for a normal-speed breaker to open and close. Some users very definitely apply immediate initial-reclosing equipment on circuits carrying an appreciable percentage of motor load, and this practice has been very successful where used.

Reference

1. Experience With Oil Circuit Reclosers on REA Systems, Lee M. Moore, Bruce O. Watkins. AIEE Transactions, volume 62, 1943, August section, pages 531–5.

Experience With Oil Circuit Reclosers on REA Systems

Discussion and author's closure of paper 43-86 by Lee M. Moore and Bruce O. Watkins, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 531–5.

Edward Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Moore and Mr. Watkins have gathered a large number of data on the operation of their rural systems. Undoubtedly, there are a considerable number of background data available which might be used to present an accurate and detailed discussion of the relative performances of simple gaps and lightning arresters in relation to service continuity. Although the paper contains some data on the subject, we do not believe it is sufficiently complete or detailed to draw conclusions one way or the other. Since there has been some controversy about this subject, a report from the same source as this paper, covering so many miles of line in different parts of the country, would be of considerable interest.

Tables II, III, and VI of the paper indicate that there are less breaker operations per mile of line with arrester-protected transformers than with gap-protected transformers. We understand that the outage following the initial breaker operation is several seconds before the breaker recloses, and that on this type of system such a long outage is not objectionable. It is thus possible that ample service reliability is produced by gaps, particularly since, according to Tables IV and VII, the lockouts that the transformer protection can do something about (items 4 and 5) are comparatively few. Where the outages produced at the transformers are a large factor in the causes of lockouts or where temporary interruptions of several seconds are not permissible, the character of the transformer protection will be of considerable importance. Satisfactory system operation apparently will be obtained on arrester-equipped systems with less breakers per mile than for gaps.

It is apparent that arresters or protector tubes reduce the breaker operations and that, therefore, more reliable service can be given with self-clearing protection, such as arresters or protector tubes. Probably one of the factors that leads some operators to lean toward the simple open gaps is the fact that arresters or protector tubes of suitable quality are more expensive than gaps. On rural circuits, the requirements for successful arrester operation are somewhat modified from those on high-capacity urban systems. The lightning duty is severe, and therefore the 65,000-ampere discharge capacity requirement considered standard should be maintained. However, the fault currents on rural systems are limited; therefore, the current-clearing ability of a protector tube may be less. Advantage can be taken of this in the design. A maximum current rating of 500 amperes should be ample for the majority of applications. Such a device can be made for lower costs than the standard tube with unlimited current rating. The company with which the discusser is associated has manufactured such protectors for use on 7,200-volt circuits. They are identified as the type *B* deion protectors. Some are in service. I would like to ask the authors of this paper whether it is likely that future service requirements will demand greater continuity of service, and whether the economics of such requirements would be satisfied by a combination of reclosing breaker and lightning protectors costing perhaps 75 per cent of those now standard?

H. L. Rawlins (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper marks a real contribution to our knowledge of the distribution field, and the authors are to be thanked for obtaining and presenting the information in such a complete and yet concise form. It covers the operating experience over a wide area, and such a comprehensive survey of reclosing breakers and rural systems would have been difficult to obtain from any other source.

To those who are interested in the design of rural reclosing breakers, the story is complete. The average operating duty on the breakers is revealed, and the detailed analysis of breaker troubles certainly would be of value to the designer. No exceptions can be taken to the proposed specifications covering an ideal small distribution circuit breaker, for the requirements seem both desirable and feasible.

There is a wealth of information in the tables which, upon close inspection, reveal many interesting and puzzling features. All but one of the tables concerning system performance show comparisons between systems with gap-protected transformers and those with arrester-protected transformers. Was this to compare the performance of the different systems, or was it done merely because different operating duties were imposed on the breakers? It is noted that no comparison is drawn favoring one type of system over the other. In classifying the systems, were tube-protected or CSP (completely self-protected) transformers included with the gap-protected transformers or with the arrester-protected transformers?

A study of Table II reveals a marked decrease in the number of breaker operations per mile for arrester-protected systems during September and October; yet there is only a very slight decrease in the number of operations per mile for gap-protected-transformer systems. There were over three times as many breaker operations on the systems with gap-protected transformers. To what do the authors attribute the large number of gap breakdowns during these comparatively lightning-free months?

D. D. Clarke (Kansas City Power and Light Company, Kansas City, Mo.): The authors have presented a splendid résumé of experience in the maintenance of service to an extensive territory of operation. In some 20 years of experience with oil circuit reclosers of various types, it was found that as the art progresses the customer more frequently is not inconvenienced by a successful reclosure. There is evidently some short time of opened circuit for each customer's usage that may be considered as no outage for that customer. In line with this, it would be of great interest to know whether the study of the system experience described by the authors has indicated that the immediate oil circuit recloser has caused the customer inconvenience or noticeable outage, where such reclosure is successfully accomplished.

B. W. Wyman (General Electric Company, Philadelphia, Pa.): Mr. Moore and Mr. Watkins have presented a wealth of operating data which should prove of inestimable value to system planning engineers the country over. This is the first time that actual operating experience with oil circuit reclosers has been made available on a scale large enough to overbalance any influencing factors such as peculiarity of terrain, weather conditions, tree, or bird difficulties.

Although the authors have pointed out that the improvement in service continuity obtained by these reclosing breakers is of far greater importance than the mere operating savings, it is interesting to note that, on over one half of the systems reporting, the maintenance and operating savings alone amount to six dollars per month per breaker. This means that only about 16 months are required to write off the investment costs per recloser.

Inasmuch as the recloser of design 2 has shown such an outstanding performance record, and also because the percentage difficulties have been reduced to such a low value as compared with the older design 1, it may be of interest to note the design features of design 2 which are responsible for this marked improvement.

To increase greatly the impulse insulation strength of the recloser and still to maintain moderate dimensions, the steel-tank construction was eliminated entirely and replaced by a porcelain cylinder of very high insulation strength with the two terminals at opposite ends of the device. Figure 1 of the paper shows this device mounted on a crossarm.

To prevent damage to the series coil from lightning impulses of steep wave front, a Thyrite disk is used to shunt the coil. This disk is small and mounts directly at the coil inside the recloser. This Thyrite disk enables the series trip coil to withstand impulse voltages far in excess of those which would flash over the outside of the porcelain housing. This construction makes possible a device with an impulse insulation strength many times higher than the original steel-tank design 1. The considerable improvement in operating performance obtained is evidenced by the small total percentage of difficulties reported since the second design has been introduced.

The Rural Electrification Administration is to be congratulated on its farsightedness in installing these devices in such large numbers and on the accuracy of their application, as any device owes its success or failure to the way in which it is applied.

F. E. Andrews (Public Service Company of Northern Illinois, Chicago): There are included in the paper suggestions for a specification for a small distribution circuit breaker. These suggestions do not cover the question of time–current characteristics for fault clearing.

On some systems it has been found impractical to apply oil circuit reclosers because of the need for co-ordination with branch-circuit fuses beyond the recloser. If reclosers are applied to a portion of the system sufficiently large in extent to load the recloser to a value approaching its rating, the amount of line beyond the recloser usually will be sufficient to require branch sectionalizing fuses. It has been found impossible to co-ordinate these branch fuses with the circuit reclosers now available, and for that reason they have not been used in many situations for which such devices generally seem needed. Therefore, it would seem that an item to cover circuit-opening time–current characteristics suitable for co-ordination with branch-circuit fuses beyond the recloser should be added to those items listed in the paper as desirable requirements for a small distribution circuit breaker.

Carl E. Mosley (W. N. Matthews Corporation, St. Louis, Mo.): Moore and Watkins are to be commended highly on the fine paper that they have prepared on their "Experience With Oil Circuit Reclosers on REA Systems."

The comments that I have are on parts (*c*) and (*d*) of the possible specification for a small distribution circuit breaker which respectively suggest that the breaker "be entirely automatic," and that the breaker "be equipped with two instantaneous openings and two subsequent time-delay openings to lockout. . . ."

Unquestionably, a breaker with an entirely automatic feature should reduce the number of service outages to a minimum and will require less attention from the operator. However, it is possible to get into trouble if breakers are too automatic and are not periodically inspected. A circuit breaker is like a transformer, with respect to the load current that it is designed to carry. A breaker should be replaced when the load current exceeds its safe current-carrying capacity; otherwise, the breaker will operate unnecessarily without lockout, while a higher-rated breaker will continue in service under the same load conditions. Breakers are likely to remain in service for months, subject to these overload conditions, without the operator knowing it.

If the breaker is oil-filled, these unnecessary operations will carbonize and lower the dielectric strength of the oil which would cause the breaker eventually to fail.

If accurate records of increasing loads are kept by the REA co-operatives, such overload conditions probably will not occur. It seems to me that if a breaker will operate a predetermined number of times and then lockout, either it will give the operator a clue that there is something wrong that should be corrected, or it will remind him to inspect the breaker.

I am of the opinion that to include two instantaneous openings and two subsequent time-delay openings in a circuit breaker would complicate the mechanism and increase the cost of the breaker. This would defeat the requirement (*h*) of the possible specification which states that the breaker should "be inexpensive."

Hamilton Treadway (Rural Electrification Administration, St. Louis, Mo.): The extensive construction of rural electric-distribution systems, which began in the middle of the last decade, raised many new problems for the distribution engineer. Not the least of these was the lack of suitable protective and sectionalizing equipment. The adoption of 7.2-kv multigrounded neutral systems made possible the use of single-pole oil circuit reclosers as one approach to the solution of this problem. The authors' paper discusses the operating experience of REA systems using this type of protective equipment. This experience has been exceptionally good.

The authors have indicated the direction which further development of this method of protection dictates. The advantages of combination instantaneous and time-delay openings have been apparent for a long time. It is obvious that continuity of service will be improved for consumers along the main feeders. Whether sectionalizing in this manner will improve service to all consumers is a problem which must be determined on each individual system. If time-delay opening is to be provided in these breakers, it is desirable that it be made adjustable to provide great flexibility in co-ordination.

The development of a single-pole breaker with time-delay opening will require more accurate determination of fault currents and a more thorough study of co-ordination problems. Also, it will require the manufacturers of fuses to develop characteristics which will bring the time–current curves of the fuses and breakers closer together. While much has been done in this direction already, there is need for further improvement.

One interesting feature of the data presented is the relation of lockouts to the number of operations. Since lockouts are equivalent to permanent faults, the data indicate the percentage of permanent faults to be much lower than was thought to be the case a few years ago. This low percentage of permanent faults is explained partly by the time–current characteristics of these reclosers which increase the number of operations over those experienced with fuse sectionalizing. However, the earlier data may require some revision. This is a point demanding much study, because continuity of service is of first importance in the rural area. It would be interesting to determine what portion of this reduction in permanent faults is due to tree trimming, improved line construction, and so forth. It would prove interesting to break these data down according to the recognized thunderstorm-frequency areas throughout the country to determine the effect of lightning on this factor.

Much could be gained if more factors could be analyzed in the economic data presented. The assembling of future data should be done in a manner to permit an evaluation of the effect of the miles of line and number of consumers per breaker, the distance which must be traveled to restore service to the area controlled by each breaker, number and length of outages per breaker, ratio of permanent to temporary faults, type of roads, weather conditions prevailing in the area, type of transformer protection, and many other factors. It is realized that considerable difficulty is experienced in assembling these data in a form whereby they can be analyzed readily. This, however, is essential to permit the distribution engineer to select sectionalizing points with greater certainty as to over-all economy of operation.

A further comparison between the relative economy of line sectionalizing and branch sectionalizing would be desirable. This again requires data not now available.

Another approach to the problem of protecting rural electric-distribution systems has been made in the area of the Tennessee Valley Authority. Here station-type breakers have been used with a single high-speed opening and reclosure, followed by a time-delay opening. This method has been employed in conjunction with branch fusing. There are many advantages to this system of fault protection, and there are many limitations. It would be interesting to compare the economy of operation of this method of fault protection with the single-pole method described by the authors.

As pointed out by the authors there are many problems yet to be solved in this field. It is hoped that the authors will continue their search for basic data, particularly in the direction of the economics of breaker design and operation.

W. R. Bullard (Ebasco Services, Inc., New York, N. Y.): This paper provides a valuable contribution to the available data concerning the performance of rural distribution circuits equipped with reclosing breakers. Of especial value is the comparison of performance of circuits with gap-protected transformers and that of circuits with arrester-protected transformers. Of course, the results of such a comparison must be used with caution, since there can be no assurance that factors other than the method of lightning protection, which might influence the performance, were sufficiently similar for the two groups of circuits to make the comparison valid. Nevertheless, the large amount of circuit mileage represented and the fact that the systems represented by both groups apparently were distributed among a number of different states encourage the use of the results for drawing tentative conclusions.

An outstanding point of interest is the very small difference between numbers of operations per month per mile for the two groups of circuits. But a more important point is that there is practically no difference between the numbers of lockouts per month per mile for the two groups. In Table III of the paper the comparison is 0.0062 lockout per month per mile for the gap-protected circuits and 0.00598 for the arrester group. In Table VI it is 0.00548 for gaps and 0.00562 for arresters.

It is the lockouts that determine the quality of service, not the momentary interruptions occasioned by opening operations followed by immediate successful reclosure. Therefore, insofar as this particular comparison is indicative of relative performance of arresters and gaps, it is difficult to see how the use of arresters, with their higher first cost and added maintenance problems, can be justified on circuits equipped with reclosing breakers. Furthermore, referring to the data on miles of line per breaker in the various tables which show appreciably longer individual circuits or sections of circuits for the arrester-protected systems, and also the data on lockouts per breaker which show appreciably lower numbers of lockouts per breaker for gap-protected systems, it seems that, as presently constituted, the gap-protected circuits are giving considerably better service than the other group.

If service equivalent to that provided by the arrester-protected group is satisfactory, service quality cannot be considered as justification for the larger number of breakers for a given amount of mileage actually used in the gap-protected group. In other words, the design can be the same for the two groups insofar as length of individual circuits, or sections of circuits, is concerned. Of course, there may have been some other reason for the difference in average mileage per breaker shown in the tables.

Near the end of the paper mention is made of the desirability of a particular sequence of operations, namely, fast opening on the first one or two openings and moderate time delay on succeeding openings. This is a sequence that has been in use for many years and has proved very successful, not only in improving quality of service, but also in eliminating the need for large numbers of reclosers, such as those of which the performance is covered in the paper. Briefly described, the practice concerned consists of controlling a large amount of circuit mileage by a single reclosing breaker located at the substation. This large circuit is sectionalized by simple single-shot fuses. The first opening of the breaker is instantaneous, and, if there is no persistent fault, service is immediately restored to the entire circuit by reclosure. If the fault persists, a time delay is introduced in the second or third opening, causing a fuse to blow on the section in trouble, after which service is restored to the remainder of the circuit by another reclosure. Insofar as quality of service is concerned, this arrangement very nearly performs the duty of a number of circuit reclosers at the cost of only an equivalent number of simple fuses plus that of one breaker at the source. The latter, of course, has a somewhat more elaborate control arrangement and may need to be heavier. However, in some situations there may be appreciable net savings in first cost, and the substitution of a single piece of automatic equipment with moving parts for a number of such devices may reduce greatly the problems of maintenance. The scheme has been applied successfully to circuits consisting individually of 100 miles of line or more, gap-protected. It is in use both in urban and in rural areas. Its possibilities are well worth investigating for application in specific situations.

Lee M. Moore and Max W. Rothpletz:[*] The authors are highly gratified to acknowledge the interest shown in this paper by rural power engineers and electrical-equipment design engineers, as evidenced by the interesting, competent, and constructive discussions.

These discussions indicate that there are a number of side roads in the design and application of oil circuit reclosers which should prove interesting and valuable on being investigated further. However, such investigation must wait until personnel again is available for that purpose.

In order to indicate avenues which further research might follow, the authors are happy to present their opinions respecting certain portions of the presented discussions.

Inasmuch as the survey covers a relatively limited period of time, no definite conclusions should be reached regarding the estimated maintenance and operating savings. These savings, however, as Mr. Wyman indicates, appear to be very substantial, and it is hoped that future maintenance costs on the oil circuit reclosers will increase these savings.

Regarding continuity of service on rural power-distribution circuits, it is believed that the quality of service to rural users ought to be equal to or better than the quality of service now rendered city and urban consumers. The reason is that lengthy interruption of service to some rural loads, for example, incubators and brooders, results in direct financial loss rather than mere inconvenience.

In answer to Mr. Clarke's question, the momentary interruption in service when a recloser operates has not to our knowledge created any serious problems for rural consumers. In this connection, however, it is reasonable to assume that momentary interruptions to rural consumers are not of as much importance as they are to large power consumers commonly served by utility systems. The length of outage which would result in definite rural-consumer inconvenience has not been determined but would depend, of course, on the type of appliances or equipment being served.

Mr. Rawlins points out that the suggested specifications for small oil circuit reclosers "seem both desirable and feasible." This statement is significant and of considerable importance in that it was our thought to present some definite objective for the designers to shoot at.

Breaker operations on systems with arrester- and gap-protected transformers were tabulated in the paper separately to show the operation of these reclosers with different imposed operating duty. In these tabulations, CSP transformers were classified as arrester-protected transformers. At present there are not enough data available to indicate the definite superiority of one type of system protection over the other. Since there is no "typical" or "average" REA-financed system because of the effects of such factors as terrain, weather conditions, geographic locations, and others, any study aimed toward proving the superiority of gaps or arresters for rural line protection should include systems which operate under every conceivable condition.

The large number of breaker operations shown in Table II of our paper for the months of September and October should not be considered unusual in view of the fact that about 75 per cent of the REA-financed systems are located in Midwestern and eastern-seaboard states. By referring to the isokeraunic maps contained in the *Monthly Weather Review*, published in May 1935, by the United States Department of Agriculture, Weather Bureau, Washington, D. C., it may be noted that lightning storms are quite prevalent in these areas during the months of September and October. This, of course, explains only the total number of recloser operations and not the unequal division of operations between gap- and arrester-protected systems.

The subject of Mr. Beck's question, though mentioned before, bears further emphasis, for it is reasonable to expect that the quality of service to rural consumers must be improved and approach uninterrupted service insofar as is practicable. Rural power engineers should consider this specific item as the first order of business. We think that the suggested reduction of 25 per cent in the cost of reclosers and lightning protection is a step in the right direction. However, we must not permit ourselves to become overly satisfied with available equipment, but should continue to strive for improved and more economical ways of doing the job.

Mr. Andrews touches on possibly one of the most difficult and important problems involved in the design and application of these small reclosers; that is, their co-ordination with transformer-primary and short-tap sectionalizing devices. Although considerable thought has been given to this problem, and progress is being made toward its solution, there is much work left to be done in this direction.

Bullard, Mosley, and Treadway have presented valuable comments on the proposed instantaneous- and time-delay sequence of operations of oil circuit reclosers. While we have not had actual experience with small reclosers embodying this operating sequence, we believe that it would provide definite advantages where co-ordination with fuses on the load side of the reclosers is required.

A New High-Interrupting-Capacity Fuse for Voltages Through 138 Kv

Discussion and author's closure of paper 43-103 by H. H. Fahnoe, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 630–5.

Jerome J. Taylor (Detroit Edison Company, Detroit, Mich.): This paper contains much valuable material, of both general and specific nature, in regard to fuse designs and applications. If information is available for publication it would be of additional interest to know whether actual fuse-tube pressures and hinge-support reactions were measured, what the stroke of the arcing rod is for the voltage ratings mentioned, what clear space is required at the muzzle end of the device, and what actual fault currents are capable of causing destruction of these fuses.

Competition between designers of fuses and designers of circuit breakers recently has taken the turn, recurrently true of engineering problems generally, that each group has been making use of design features previously assumed to be characteristic of the other. Thus, the present trend in circuit-breaker design is toward the use of blasts of gases or liquids, while, at the same time, fuse designers have been employing shunted springs as auxiliary separating devices, and now, as described in this paper, solid-retractable arcing rods.

As compared to circuit breakers, fuses have been traditionally inadequate in regard to circuit reclosure and require careful manipulation in order to take part in co-ordinated relaying. On the other hand, their capital cost is relatively small; their interrupting range and reliability have been increased greatly; speed of interruption is unmatched; and peak fault current is often limited to values substantially less than those corresponding to circuit impedance. These tendencies have resulted recently in British proposals to use combinations of breakers and fuses in series, the breakers handling switching and medium faults with the fuses timed to clear heavy short circuits in advance of the breaker. There seems some basis for thinking that a fusible conductor has inherent advantages over separable contacts in clearing heavy faults, and it would be interesting in this connection to know whether the author thinks his design is adaptable to duty in circuits of short-circuit capacity in excess of 1,000,000 kva. It may be that in the future a hybrid design of interrupter will be developed, embodying the special abilities of several present types.

Regarding fuse construction, there always has been a fairly definite grouping into enclosed and expulsion types. Another differentiation seems to be becoming more pronounced—one style employs fillers or liners containing hydrates; the other uses relatively inert materials, such as sand or granulated quartz. Fundamental research has shown that arc drop in steam is higher than in any other gas except hydrogen.[1] It is this fact that appears mainly responsible for the ability of fuses of the gypsum- and boric-acid type to interrupt relatively high voltages per inch of length, assuming a given wire size of fusible element. This ability is extended by pressure and turbulence, giving rise to a new difficulty encountered and solved by the author of the paper, that the effective dielectric strength of an arcing chamber may exceed that of the external air.

For heavy power applications, it seems possible that in special cases, liquid cooling of the main conductors by forced convection would be advantageous, both in fuses and in some types of breakers. It would entail additional costs and complications but offers potential profits in that load ratings up to ten times present figures might be obtained in an interrupter of the same physical size and short-circuit capacity.

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[*] Max W. Rothpletz is associate engineer with technical standards division, Rural Electrification Administration. In view of the fact that coauthor of the paper, B. O. Watkins, is now a lieutenant, junior grade, United States Naval Reserve, Mr. Rothpletz kindly consented to act as an alternate in preparation of this discussion.

REFERENCE

1. C. G. Suits. *Journal of Applied Physics*, volume 10, March, September, 1939; *General Electric Review*, volume 39, 1936, page 194, and volume 42, 1939, page 432.

C. L. Schuck (General Electric Company, Philadelphia, Pa.): Mr. Fahnoe's paper gives a very rational development of the economic and engineering aspects leading to the desired features in fuses for the higher voltages. The new fuse described necessarily must be classed as a vented fuse, since an external expulsion of arc products accompanies interruption. It should be noted that, in addition to the water vapor resulting from the breakdown of boric acid, fuses of this type also expel quantities of incandescent gas resulting from the explosion of the metal forming the current responsive element together with air which has been heated to incandescence by the arc. The prevention of the expulsion of solid matter, such as the fuse cable in conventional expulsion fuses, is praiseworthy and may reduce the degree of violence of the disturbance accompanying interruption.

The problem of installing and removing high-voltage fuse units by the operator is recognized in the paper, and, in this connection, it should be noted that the average man is capable of handling about 150 foot-pounds of the kind of effort represented by a weight, such as a fuse, at the end of an arm, such as a switch hook. It is estimated, from the construction of the 138-kv fuse unit described, that such a fuse unit would weigh in the neighborhood of 15 pounds. This would indicate a ten-foot switch hook as the maximum practical for handling a 138-kv fuse unit. Allowing four additional feet for the effective height of the operator, this places the maximum height of the upper insulator stack of the fuse disconnecting switch at 14 feet above the ground. The lower end of the fuse unit is, therefore, eight feet above ground, a height which is considered inadequate for this voltage, and, obviously, an operating platform is required. Even with the operating platform, is it considered good operating practice to allow personnel to come within eight feet of a live 132-kv device? If not, a disconnecting switch is required to isolate the fuse before it can be handled. It also would be of interest to know whether the reduction in violence of interruption by the use of the fuse described permits mounting this close to ground.

H. H. Fahnoe: In the new high-voltage fuse the interruption of heavy fault currents is initiated by the fusion of an extremely short calibrated-fusible element, and the resultant arc is drawn out by separable contacts, as indicated by Mr. Taylor. On high short-circuit currents, where extremely fast clearing is desirable, the fuse element usually melts during the first half-cycle, and interruption is completed quickly by the lightweight retractable arcing rod being drawn through the interrupting chamber. The length of this boric-acid-lined interrupting chamber is shown in Table I. The use of separable contacts, instead of a long fusible conductor, greatly reduces the arc energy in the fuse.

Table I

Fuse Unit (Kv)	Interrupting Chamber (Inches)
138	36
115	31
92	26
69	17

It is probable that fuses of this type can be made having an interrupting capacity in excess of 1,000,000 kva. However, the field for such fuses is not yet clear. It is desirable to explore the possibilities of applying the present fuse before developing a fuse of increased interrupting capacity.

The interrupting tests made on these fuses included tests to destruction. The fault currents required for failure vary with circuit conditions. Actual fuse-tube pressures and reactions were not measured because of the difficulties of making such measurements at the potentials involved.

The problems of installing and operating high-voltage fuses, as outlined by Mr. Schuck in his comments, deserve the consideration of all fuse designers. However, Mr. Schuck apparently has overlooked the fact, that, in placing the fuse unit in the mounting, one end of the fuse unit must engage the lower contact. This, according to Mr. Schuck's figures, would place the lower end of the mounting at 14 feet above ground. It is relatively easy to rotate the fuse about the hinge point to the closed position.

The cone of incandescent vapor ejected from the fuses when interrupting maximum fault current is approximately 72 inches long. Since these higher-voltage fuses often are mounted on greater pole spacing in an open structure, the expulsion of incandescent vapor is not so important as in the case of lower-voltage fuses.

It would not be considered good operating practice to allow personnel to operate these high-voltage fuses without isolation from live lines, particularly as hook-stick-operated switches do not have the ability to interrupt any magnitude of load or magnetizing current.

Safe Ratings for Overhead Line Conductors

Discussion and author's closure of paper 43-85 by Leonard M. Olmsted, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, pages 845–53.

L. F. Hickernell and A. A. Jones (Anaconda Wire and Cable Company, Hastings-on-Hudson, N. Y.): While the various data and formulas presented in this paper are not new, the particular combination of them to form the analytical method presented by the author appears to be novel. The methods and formulas used in the various steps of the analysis are well established and need no comment. However, some of the basic data dealing with annealing characteristics of copper are not so well established, and specific comments may be warranted.

Any analytical method based on fundamental data derived from various sources involves the inherent danger that these data may not be compatible. If all details regarding test methods and test samples used to obtain these data are not thoroughly scrutinized, it is very possible that inaccurate conclusions may be drawn. This is particularly true in the case of annealing characteristics of copper.

The author states that the rate of annealing depends (among other things) upon the purity of the copper. This is quite true, but it also depends to an even greater extent upon the nature and combination of impurities present, even in minute quantities. Commercial electrolytic tough-pitch copper having approximately the same purity as determined by chemical analysis (that is, copper, 99.95+ per cent) will vary tremendously in annealing characteristics, depending upon the characteristic impurities of the ore source from which it was derived. As an example, medium hard-drawn copper wire annealed at 150 degrees centigrade so as to give a reduction of 10 per cent in tensile strength required only about two hours for one sample but 400 hours for another.

Similarly, referring to Figure 3 in the paper, variations of tensile strength with per cent cold drawing are to be expected. Tests made by our company on a representative commercial tough-pitch copper indicate tensile strength of approximately 59,000 pounds per square inch for 50 per cent reduction, and 64,000 pounds per square inch for 75 per cent reduction. These represent an appreciable increase in the values presented in Figure 3.

Referring to Figure 4, the data presented are presumably derived from short-time high-temperature anneals such as used in the industry. They indicate 30 per cent annealing in one per cent of time for complete annealing. Tests made by our company on a representative commercial tough-pitch copper annealed at 150 degrees centigrade indicate a seven per cent reduction in tensile strength (or approximately 14 per cent annealing) in one per cent of time for complete annealing in the case of hard-drawn wire and no measurable reduction in the case of medium hard-drawn wire. In order to obtain a seven per cent reduction in tensile strength on medium hard-drawn wire, approximately four per cent of time for complete annealing was required.

The author expresses the hope that "carefully controlled tests can be run to establish the time–temperature data for hard-drawn transmission conductors definitely in order to make available their full capacity." This investigation is now under way in our laboratories. Unfortunately, such tests require a long period of time and, hence, have not progressed sufficiently to warrant the results being presented at this meeting. Until these data are available, we prefer to postpone any discussion of Figure 7.

D. D. Clarke (Kansas City Power and Light Company, Kansas City, Mo.): The author emphasizes the limiting factor of clamps, connectors, and joints on the conductors, especially those caused by the oxidation of the contact surfaces with resulting overheating; but he has given us very little discussion on, or hope of, relief from this factor. Both Mr. Olmsted's paper and the working group report given by Mr. Hansen are in line with the heavy loads we are asked nowadays to put on overhead copper conductors, but there was very little said with regard to preserving the conductivity of the joints, which, because of oxidation, may really become the limiting factor in loading the line.

The company with which I am associated has for many years made a practice of protecting all conductor joints against oxidation. This practice started in 1911 and became more effective as experience was gained with the results. Our operators believe that this practice of protecting all conductor joints against oxidation has saved us an untold amount of trouble, especially when our conductors are forced to carry heavy loads. The small cost of an oxidation-preventative coating in the joints is entirely incommensurate with the considerable cost of repair that we experience on joints that have not been so protected.

L. R. Gaty (Philadelphia Electric Company, Philadelphia, Pa.): This paper is an excellent addition to a subject about which there are still some uncertainties. It is particularly important to obtain additional data on the relations of temperature, time, and annealing. If these relations can be established reasonably well, a major step forward will have been completed.

There are two definite problems that should not be confused:

1. Normal rating of the conductor. The limitations of present available information indicate a need for conservation in establishing normal ratings. Voltage regulation will be the principal limitation in many instances.
2. Determining the conductor rating for emergency loads, the infrequent occurrence and short duration of which make it unnecessary to be quite so free with generous factors of safety.

There are several factors involved in any determination of conductor ratings, and the author has introduced some margin of safety at nearly every step. He uses an emissivity factor of 0.5, which is slightly low for well-oxidized copper. The author determines equivalent ambient temperatures by a method which is conservative and then allows also for some increase in wire temperature due to the effect of solar heating, which is present only on clear days. Convection is calculated at wind velocities which are discounted arbitrarily to recognize some possible effects of shielding and axial air motion in the conductor plane, but ignore the vertical components of air motion which are crosswise at all times, the cooling effects of which are predominant outdoors whenever the horizontal-crosswise component of wind velocity is small. Temperature ceilings are selected conservatively. When one considers these various factors and the improbability that the maximum possible emergency load will be coincident with critical atmospheric conditions, it appears that factors of safety have been pyramided with exceptional generosity.

In attacking the problem faced in Philadelphia, the Weather Bureau records of simultaneous wind and ambient observations were used as a basis for determining the approximate effect upon conductor heating for many years. The atmospheric conditions which would produce the maximum possible conductor temperature have occurred on an average of six nonconsecutive hours per year, or so infrequently as to have been considered unlikely to coincide with the hours when it might be necessary to operate the conductor at its emergency load limit. Few emergencies occur at the time of maximum load; hence, the load probably would not be nearly equal to the emergency load limit of its conductor, even if an emergency should occur at the time of a critical atmospheric condition.

Operations at the emergency load limits in Philadelphia will produce a maximum hour conductor temperature of 85 degrees centigrade on the average summer day and less than 135 degrees centigrade in all but the six most critical hours when it is possible, but very improbable, that emergency operation might carry the temperature up to a ceiling of 175 degrees centigrade. These conditions appear to be reasonably acceptable for emergency load ratings. Emphasis should be placed on checking sags and clearances for whatever ceiling temperature is decided. A 66-kv line recently checked showed that contact would occur at two locations if the conductor temperature should reach 135 degrees centigrade. The limitations of clamps, fittings, and terminal equipment are also important.

E. Hansson (Pennsylvania Water and Power Company, Baltimore, Md.): The author is to be congratulated on his thorough analysis of the problem of rating overhead conductors. I am particularly pleased to see him ending his discussion with a word of caution. There is another reason, not mentioned by the author, why such caution is necessary. It is mentioned in the interim report presented at this meeting and may be worth elaborating to some extent.

In making the tests on the heating effect of clamps, as mentioned by the author, it was found that the core strand of a seven-strand 2/0 copper conductor reached a temperature 17.5 degrees centigrade above that of the surface of the conductor at a 425-ampere current. In order to confirm this, further tests were made on 7- and 19-strand 4/0 conductors. In both cases the difference between center strand and surface was 16.9 degrees centigrade at a current of 490 amperes. Another check on a 300,000-circular mil all-aluminum conductor gave a difference of 7.2 degrees centigrade at 375 amperes. If these values are placed on the basis of identical temperature rise, 60 degrees centigrade, the differences are substantially the same, varying from 10 degrees for the 300,000-circular mil aluminum to 13 degrees for the 2/0 copper. No such difference was found in the original test on the 795,000-circular mil steel-reinforced aluminum cable, the measured difference between inside and outside layers being 3.2 degrees centigrade at 920 amperes.

The rather meager data seem to indicate that the gradient in a homogeneous conductor is considerable, but that it is practically absent in steel-reinforced aluminum cable. The gradient seems to be proportional to the temperature rise, and the values given are for rather high currents, but, even for such values of current as may well be used for the conductors in question, the difference will be ten degrees centigrade or more. Considerable more test data will be required before any definite values can be established for the various conductor sizes, but it is apparent that the effect must be considered when establishing maximum ratings.

Leonard M. Olmsted: The several objectives of the paper were to take the fullest possible advantage of the limited information already published in engineering literature, to indicate those phases which most need investigation, and to provide a procedure by which additional data may be applied as they become available. It is fortunate that the subject may be divided into components to be investigated independently, as that approach permits simultaneous work by a number of investigators to expedite the ultimate determination of the whole. The four discussions presented indicate that such independent investigations are being made and lend encouragement to the hope that standardized ampere ratings may be prepared for open-wire conductors comparable with tables now available for underground cables.

Mr. Hickernell and Mr. Jones have emphasized the marked variations in the behavior of commercial hard-drawn and medium-hard-drawn copper wire. The data they give are typical of those from previous publications which were the only information available from which to develop conductor ratings. Nothing can be accomplished by attempting to compare these few additional data with the paper until sufficient tests have been conducted and analyzed to justify preparing entirely new curves to replace those of the paper. It is most encouraging to know that an extensive series of tests is now under way, as it is reasonable to hope that the resulting data will reduce materially the probable errors in one of the most important components in the derivation of safe conductor ratings.

Mr. Hansson calls attention to the variations in temperature measured at various points in a stranded conductor. It does not seem probable that higher temperature in the core strand of a stranded copper conductor is serious, however, as the tensile characteristics of the conductors depend principally upon the time–temperature exposure of the other strands. Steel-core conductors apparently are almost free from this temperature difference, and are inherently less affected because of the greater allowable temperature for the steel. The fact remains, however, that considerable care is necessary in testing the temperatures of conductors and associated equipment if the results are to be dependable.

Mr. Clarke's discussion of connector and joint heating is an important contribution to the subject. This phase was only mentioned in the paper, however, because it appears that the contact surfaces of the connectors should be cooler than the conductor alone, and the conductor temperature limits established by annealing of hard-drawn copper are so low that oxidation of the contact surfaces seems improbable. Other conductors with higher temperature limits are more likely to be affected by joint oxidation.

Mr. Gaty's observation of the difference between normal and emergency ratings is appropriate, and the paper indicates a method for allocating the total allowable time–temperature exposure to these two types of service. The author agrees, also, that an effort was made to be conservative at certain points in the paper. The effect of solar heating was included only for the portion of time that sunshine actually exists according to Weather Bureau data. It is difficult to place any particular value upon the vertical components of air movement, but it seems improbable that any appreciable margin of safety has been introduced by neglecting this factor. A correct measure of the degree of conservatism can be ascertained only by a detailed hour-by-hour analysis of coincident atmospheric and current loading conditions during a period of many years, such as has been reported by Mr. Kidder and Mr. Woodward. Such an extensive analysis has not been undertaken by this author, but an investigation of several typical months has shown no perceptible margin of safety other than the 20 per cent mentioned in the final paragraph.

Comparison between the conductor temperatures used by the author and those mentioned by Mr. Gaty is misleading, as the former are intended for hard-drawn copper and the latter for medium-hard-drawn. This difference alone justifies a conductor temperature approximately 28 degrees centigrade higher on medium-hard-drawn copper conductors, giving temperatures comparable with those allowed in Philadelphia.

In conclusion, it is interesting to observe that the Duquesne Light Company has authorized ratings for certain of its lines derived in accordance with this paper. Subsequent experience with suspension clamps has indicated that the 20 per cent margin of safety is none too great until methods are developed to eliminate conductor burning and magnetic heating. Certain tests are being conducted to this end. It is highly desirable that the results of all such test programs be made available in order that this margin of safety may be reduced intelligently to permit the greatest possible use of overhead line facilities.

Interim Report on Emergency Overloads on Overhead Conductors

Discussion of paper 43-105 by the AIEE working group on towers, poles, and conductors of the transmission subcommittee of the AIEE committee on power transmission and distribution, presented at the Cleveland national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 588–90.

L. R. Gaty (Philadelphia Electric Company, Philadelphia, Pa.): A point of considerable interest is that illustrated in Figure 3 of the paper, where it is indicated that the center or core strand of a seven-strand 2/0 copper cable may reach a temperature of 15 degrees centigrade in excess of that at the surface. It is believed that a part of the apparent temperature difference between thermocouples 2 and 3 in Figure 3 of the paper may be due to the difficulties inherent in an attempt to measure surface temperature accurately with a thermocouple. Additional information regarding this temperature difference will be helpful in determining the conductor ratings which may be applied without destructive annealing of the conductor. Possibly, the core temperature, which can be measured without difficulty, should be used to determine maximum ratings.

The tests reported by Kidder and Woodward in their paper[1] stated that the copper temperatures were observed by a thermocouple that was inserted between the core strand and the outside layer of seven-strand 2/0 copper.

Tests of magnetic heating in the standard-suspension and dead-end clamps, used by Philadelphia Electric Company with 2/0 copper, have shown that the maximum temperature in the clamp is somewhat lower than the temperature of the conductor at all loads up to 560 amperes. The original tests were made in 1933 and were confirmed by recheck in 1943. Variations occur as a result of the type of clamp used, the condition of the conductor, and the size of conductor in relation to the size of the clamp. Care must be used in applying the results of any test to conditions different from those tested.

Reference

1. Ampere Load Limits for Copper in Overhead Lines, A. H. Kidder, C. B. Woodward. AIEE Transactions, volume 62, 1943, March section, pages 148–52.

The Effect of Corona on Coupling Factors Between Ground Wires and Phase Conductors

Discussion and author's closure of paper 43-77 by G. D. McCann, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, pages 818–26.

E. D. Sunde (Bell Telephone Laboratories, Inc., New York, N. Y.): In this paper it is implied that the coupling between the ground wires and the phase conductors is due entirely to electric induction. This is no doubt true when the ground wire carries a negligible current, as in the measurements on short wires described in the paper. However, in the case of a lightning stroke to a transmission-line tower or to a ground wire, the current carried by the latter will cause magnetic induction in the phase conductors. To obtain the voltage of the phase conductors, it is then necessary to consider magnetic, as well as electric, induction.

Consider a ground wire and a phase wire of such length that reflections need not be considered, at least in calculating the crest voltage. The coupling factor, or the ratio of the voltage of the phase conductor 2, to that of the ground wire 1, is then

$$K = S_{12}/S_{11} = (L_{12}/C_{12})^{1/2}/(L_{11}/C_{11})^{1/2} \\ = (K_e K_m)^{1/2} \qquad (1)$$

where S_{11} and S_{12} are the self- and mutual surge impedances and K_e and K_m the electric and magnetic coupling factors, respectively.

$$K_e = A_{12}/A_{11} = (1/C_{12})/(1/C_{11}) \\ K_m = L_{12}/L_{11} \qquad (2)$$

A_{11} and A_{12} being the self and mutual potential coefficients, L_{11} and L_{12} the self- and mutual inductances.

Since the crest voltage is obtained after a few microseconds, the effective frequency is so high that the equivalent ground plane for the magnetic coupling may be taken at the surface of the earth, as in the case of the electric coupling, so that

$$K_m = K_e$$

and

$$K = (K_e K_m)^{1/2} = K_e \qquad (3)$$

This relationship is true only when K_e is the electric coupling factor without corona. The magnetic coupling factor is not affected by corona, because the conductivity of the corona is not large enough to affect the inductances L_{11} or L_{12}. If K_c is the coupling factor with corona, as determined in the paper, and $K_m = K_e$ the electric coupling factor without corona, then the coupling factor for an actual line is

$$K = (K_e K_c)^{1/2} \qquad (4)$$

That is, the coupling factor to be used on an actual line is the geometric mean of the coupling factor K_c with corona which is obtained from the measurements in the paper, and the coupling factor K_e without corona, which may also be obtained from the results of the measurements, or may be calculated by the usual formulas.

As a first approximation, it is usually permissible to take the arithmetic mean $K = (K_e + K_c)/2$ instead of the geometric mean as in equation 4. If using the formulas for K_e and K_c, it then follows that the effective radius to be used in calculating the coupling factor for an actual line is the geometric mean of the actual radius and the equivalent corona radius of the ground wire, the latter being given in the paper for various conditions.

In the preceding discussion, reflections in the ground wire caused by the presence of tower grounds along the ground wire were not considered. Such grounds will reduce the voltage and increase the current in the ground wire, so that magnetic coupling will tend to predominate. The coupling factor will then be smaller than that obtained from equation 4 and will approach the magnetic coupling factor, or the electric-coupling factor without corona.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): As usual, Doctor McCann has made a very thorough investigation of this problem. The question is whether these results are applicable to actual transmission practice. The question of insulator string flashover on towers with low tower-footing resistance has long been a puzzling one. There are numerous instances recorded in the literature where the tower potential is of the order of 80 per cent to as low as 10 per cent of the spark-over voltage based on a 1.5x40 wave, and the *IR* drop across the tower ground resistance. Now, Doctor McCann's tests show that the coupling factors should be from 30 per cent to 100 per cent greater than the purely electrostatic coupling factors. While there are many other explanations for such flashovers at apparently low tower voltages, such as direct strokes to the wire, the principal ones responsible probably are higher short-time impulse resistance of grounds, the inductive drop in the tower proper and in the ground connections, such as counterpoise and ground rods. The effect of these factors, as well as the repeated reflections from nearby towers, is to produce highly peaked voltage waves at the tower, and, therefore, the ground wire would require a still higher voltage across the insulation than that caused by a 1.5x40 wave. From this point of view, it is very difficult to believe that the coupling factors during an actual stroke to a tower are really as high as obtained during Doctor McCann's tests.

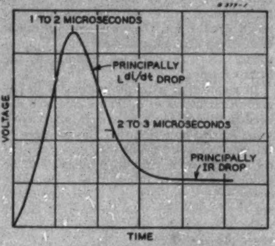

Figure 1. Typical ground-wire voltage for lightning-stroke currents of steep fronts

A considerable difference for wave shapes of the type shown in Figure 1 of this discussion, where the crest is reached in one to two microseconds, and the wave decreases to half value in two to three microseconds, should be due to two factors:

1. The starting voltage of visible corona, and, consequently, the corona radius on a wire, have a time lag similar to that of the breakdown voltage. The corona shown in Figure 13 and Figure 14 of the paper is due to 1.5x40 waves, where a time interval of from four to ten microseconds is available for formation and expansion of the streamers.

2. On a long line, the surge-impedance effect of the line enters into discussion as soon as a traveling wave is formed on the ground and line conductors with currents of thousands of amperes. In the tests, the line conductor acts as a kind of capacitance-divider pickup device.

There seems to be a question of a race between the time to form the corona on the wire and the speed of travel of the induced wave on the line conductor. For slow fronts this is not important; however, for fronts of the order of one to two microseconds, the increase in coupling factor caused by corona should be considerably less than that obtained in the tests described. Direct-stroke measurements on the Empire State Building have shown that 50 per cent of the current fronts are one microsecond or less. Similar steep fronts are to be expected on transmission lines. It would appear necessary to make further tests to represent more nearly actual stroke conditions.

It might be well to insert in the paper a word of caution to what extent the curves of Figure 18, in conjunction with Figure 16 and Figure 17, can be used. For instance, for the 30-foot ground wire, potentials on the line wire can be obtained which are greater than the ground-wire potential. Furthermore, the line-conductor potential attains a maximum at around 1,000,000–2,000,000-volt ground-wire potential.

Doctor McCann does not state to what extent the line conductor produces corona and what effect such corona would have on the corona formation on the ground wire. In particular, on the tests with the ten-foot-high ground wire, where coupling factors have been obtained of the order of 0.8, it would seem that considerable corona should have been observed on the line conductor. With two wires under corona conditions, the capacitive coupling would become more nearly that of two cylinders of considerable diameter. Perhaps this accounts partly for the fact that the equivalent corona radius is about one half of the observed streamer length.

Leonard M. Olmsted (Duquesne Light Company, Pittsburgh, Pa.): In his discussion of coupling between ground and counterpoise wires and phase wires, Doctor McCann has mentioned the installation described in a previous publication by E. R. Whitehead of the Duquesne Light Company. It is interesting to note that the 66-kv double-circuit steel-tower line with single overhead ground wire, to which the counterpoise wire has been added, is showing an improvement in reliability, even beyond our expectations.

G. D. McCann: Mr. Sunde has raised a very fundamental point regarding the relationship between the measured coupling factors, given in this paper, and the total coupling as produced by both electrostatic and magnetic effects. The measured coupling factors represent only electrostatic coupling. However, it does not appear that the equations given by Mr. Sunde are rigorously correct, even for the assumptions he made. These were that all distortion and losses such as those caused by corona are neglected. It is assumed that the capacitances can be determined from the effective ground-wire corona radii, while the inductances are determined from the true radius of the ground wire. It is not known to what extent these assumptions are justified. Also, the author has, as yet, been unable to obtain a solution based upon these assumptions which, for the case of aerial conductors, gives the relation between the induced-conductor potential and ground-wire voltage in the form of a purely numeric factor. Such a solution can, however, be obtained readily for the simpler case of concentric cylinders for which the coupling-factor equations of Mr. Sunde should also apply.

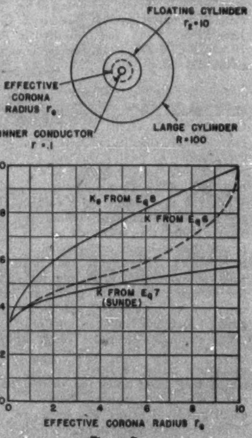

Figure 2

Consider the case illustrated in Figure 2 of this discussion in which the ground wire is represented by the inner conductor of radius r_1, the phase conductor by the floating cylinder of radius r_2, and the ground plane by the large cylinder of radius R. Let it be assumed that the system of cylinders is semi-infinite in length, and that a voltage is applied between the inner conductor and the ground cylinder at the one end. For this case the voltage induced on the floating cylinder in terms of the inner conductor potential is given by the coupling equation:

$$K=\frac{Z_2}{Z_1+Z_2} \tag{1}$$

where Z_1 is the surge impedance for the system consisting of the inner conductor and floating cylinder. Z_2 is the surge impedance for the floating and outer cylinders. Three cases will be considered:

CASE 1. NO CORONA, DIELECTRIC CONSTANT EQUAL TO 1

For this case

$$Z_1 \alpha \log\frac{r_2}{r_1}; \qquad Z_2 \alpha \log\frac{R}{r_2}$$

$$K=\frac{\log\frac{R}{r_2}}{\log\frac{R}{r_1}} \tag{2}$$

From equation 1 of Sunde's discussion

$$K=\sqrt{\frac{L_{12}C_{11}}{L_{11}C_{12}}}$$

The equations for these constants are the following:

$$L_{11} \alpha \log\frac{R}{r_1}; \qquad L_{12} \alpha \log\frac{R}{r_2};$$

$$C_{11} \alpha \frac{1}{\log\frac{R}{r_1}}; \qquad C_{12} \alpha \frac{1}{\log\frac{R}{r_2}}$$

These give the following equations for the coupling:

$$K=\sqrt{\frac{\log\frac{R}{r_2}\times\log\frac{R}{r_2}}{\log\frac{R}{r_1}\times\log\frac{R}{r_1}}}=\frac{\log\frac{R}{r_2}}{\log\frac{R}{r_1}} \tag{3}$$

For this assumption, Sunde's equation checks that derived from the surge impedances.

CASE 2. MEDIUM BETWEEN INNER CONDUCTOR AND FLOATING CYLINDER HAVING DIELECTRIC CONSTANT k

For this condition Z_1 is changed to the following:

$$Z_1 \alpha \frac{1}{\sqrt{k}} \log \frac{r_2}{r_1}$$

$$K = \frac{\log \frac{R}{r_2}}{\log \frac{R}{r_2} + \frac{1}{\sqrt{k}} \log \frac{r_2}{r_1}} \qquad (4)$$

For Sunde's equation

$$C_{11} = \frac{1}{\log \frac{R}{r_2} + \frac{1}{k}\ \text{'og} \frac{r_2}{r_1}}$$

$$K = \frac{\log \frac{R}{r_2}}{\sqrt{\log \frac{R}{r_1}\left(\log \frac{R}{r_2} + \frac{1}{k} \log \frac{r_2}{r_1}\right)}} \qquad (5)$$

The equation given by Sunde does not check for this condition. The difference is greater the higher the dielectric constant. However, for values of k no greater than 4 or 5, the difference between the two solutions is usually not very great.

CASE 3. DIELECTRIC CONSTANT ONE; EFFECT OF CORONA ON INNER CONDUCTOR REPRESENTED BY CORONA RADIUS r_e

For this case Sunde's assumptions, that capacities are given by the effective corona radius r_e and inductances by r_1, will be used.

This gives for the surge impedance Z_1 the following:

$$Z_1 = \sqrt{\frac{L_{11}}{C_{11}}} \quad \alpha \sqrt{\log \frac{r_2}{r_1} \times \log \frac{r_2}{r_e}}$$

Then from Equation 1

$$K = \frac{\log \frac{R}{r_2}}{\log \frac{R}{r_2} + \sqrt{\log \frac{r_2}{r_1} + \log \frac{r_1}{r_e}}} \qquad (6)$$

For Sunde's equation

$$L_{11} \alpha \log \frac{R}{r_1}$$

$$C_{11} \alpha \frac{1}{\log \frac{R}{r_e}}$$

$$K = \frac{\log \frac{R}{r_2}}{\sqrt{\log \frac{R}{r_e} \times \log \frac{R}{r_1}}} \qquad (7)$$

The electrostatic coupling for this case is given by the equation

$$K_e = \frac{C_{11}}{C_{12}} = \frac{\log \frac{R}{r_2}}{\log \frac{R}{r_e}} \qquad (8)$$

To illustrate the difference in equations 6, 7, and 8, calculations were made for the conditions illustrated in Figure 2 of this discussion. As shown by the three curves of this figure, all three equations (as they should) give the same result when r_e equals r_1. When r_e equals 10 or r_2, the coupling factor should be 1, as correctly given by equations 6 and 8. However, the curve derived from Sunde's equation lies somewhat below the curve which should be correct for the given assumptions (equation 6) and never reaches one. It starts to diverge rapidly at r_e equals about 6.

Over the principal range of ground-wire potentials, the measured effective corona radii as shown by Figure 15 of the paper do not exceed about five feet. Thus, for most transmission lines, the effective corona sheath does not bridge more than about half the spacing between the ground wire and phase conductors. The curve of Figure 1 indicates a fairly close agreement between Sunde's equation and equation 6 over this corresponding range (up to r_e equals 5). It thus appears probable that the use of Sunde's equation, as given in his discussion, together with the measured electrostatic coupling factors in the paper will give values of the initial induced voltages that are only about 10 or 20 per cent low.

Further consideration is being given to this problem. It is recognized that there are other factors determining the surge voltages produced on phase conductors which have not been given consideration in the past. Most important of these are the effects of the electrical conditions of the stroke channel itself. It was for this reason that no attempt was made in the paper to determine directly transmission-line flashover conditions.

Guide for Wartime Conductor Temperatures for Power Cables in Service

Discussion and closure of paper 43-104 by the Association of Edison Illuminating Companies subcommittee on wartime temperatures for cable circuits, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 606–10.

H. L. Davis, Jr. (Philadelphia Electric Company, Philadelphia, Pa.): Past operating experiences of the utilities have been a major contribution in paving the way for the increased operating temperatures which have been outlined so thoroughly in this guide. While the new temperature ceilings given are feasible, the guide points out very clearly what the operating effects might be and the conditions under which they may be justified. Experience in Philadelphia with cable operating at temperatures above the normally recognized peacetime limits, confirms the discussion in section 2.4 of the guide, "Assumed Influence on Cable Life."

For example, operation of 22 miles of three-conductor 13-kv belted cable at full peacetime rating resulted in a failure rate which was three times the system failure rate. When the copper temperature on this small group of cables was increased from 77 degrees centigrade to approximately 100 degrees centigrade, the failure rate increased to 11 times the original system rate (six per 100 miles). However, the system failure rate only increased 50 per cent. It is obvious that no system is exposed to general load increases, but only limited parts of a system will be effected by the war demands. Consequently, while 400 per cent increase in failure rate, referred to in the guide, may seem to be exorbitant, it will involve an over-all failure rate that should be well within the ability of the maintenance personnel to handle without an undue influence on continuity of service.

The cable temperatures of about 100 degrees centigrade experienced in Philadelphia did not produce an appreciable number of dielectric failures. The trouble has been mostly that of openings in the lead sheath, which caused about 83 per cent of the failures on the heavily loaded lines. At the higher temperatures the lead sheath is very much weaker and therefore scoring, minor electrolysis attacks, and mechanical injuries to the sheath are hastened to failure by the higher loading.

In connection with the statement in the guide that increases in failure rate will not become apparent in less than six months or a year, it is to be noted that the presence of the sheath cracks, which may have required six months or a year to produce, will be brought quickly to failure only if the cable continually is submerged. Where the ducts are dry, experience has shown that cables with open sheaths may operate for several years before failure.

Robert J. Wiseman (The Okonite Company, Passaic, N. J.): The recommendations of the Association of Edison Illuminating Companies subcommittee on wartime temperatures for cable circuits, as reported to Mr. Halperin, are of interest to us in that the committee has set up not only tables indicating temperatures at which it believes a cable may operate, but also warning signals, or "red flags," indicating what one can expect if a cable load is increased to a value that results in these temperatures: namely, a reduction in the over-all life of the cable, whether insulation or sheath, and increased cable movement (therefore mechanical effects on the lead, and possible effects on accessories such as joints, reservoirs, and terminals)—all resulting in an increase in failure rate. I would like to recommend that the utility engineer who is contemplating taking advantage of this emergency loading of cables read the nine papers given at the AIEE national technical meeting in January 1943, in New York.* These papers tell in detail the views of the utility engineer and the manufacturer about overloading of cables. I understand that the authors took into consideration the views expressed in these nine papers when preparing the guide.

The emergency temperatures set up are presumably based on the experiences of some companies. I believe that under closely observed control it may be safe to reach these temperatures for impregnated-paper cables. I am not so sure about the values for varnished-cambric cables. I think they are high, particularly for voltages over 7.5 kv when dielectric losses are of importance. The emergency temperatures for code insulation are too high, unless it is expected and accepted that a very short life will result. The values for ozone-resistant compounds of the oil-base type can be raised to 90 degrees centigrade, provided they are not subject to a high compression, such as a clamp, during the emergency temperature period.

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* All nine papers are available in a single pamphlet (Special publication 6), in limited quantities, from AIEE headquarters at 75 cents per copy.

I believe that, before emergency loadings are applied to a cable system, a careful survey should be made of the operating record. How does the rated load of the cable compare to the actual load or contemplated load? In the past we have been conservative in many cases in the rating of a cable. Perhaps the new load is still within the allowable rating of the cable; therefore, there is no emergency loading. If the new load is definitely an overload and the expected temperature is higher than given in this war guide, then means must be taken to reduce this temperature as suggested in the guide, or the load must be reduced.

Although this guide is intended to be used for the duration of the war, it easily can be a guide for future peacetime emergency operation. At that time it will be necessary to review the experiences of those companies that have operated under the guide. I am hoping that all who do so will keep a very complete record of the loadings on their cable circuits, the amount and total time of each emergency loading, and the record of cable failures or cable replacements. Unless this information is available, it will be hard to convince some of us that emergency loads have been carried and that the cables have given no trouble. The cable manufacturer is just as anxious as the operator to know what is the limit on temperature operation of a cable. This information can come only from the experience of the operating companies, and they will be asked to present the record as a future guide.

This guide and the nine papers given at the January meeting refer to allowable emergency temperatures, and practically nothing is said about what this means in current loading. The latter is what the operator wants to know when he is called upon to carry extra loads. The permissible load current will depend upon the time the emergency exists, the conditions of installation, cable size, number of cables in duct bank, what load current is being carried at the time the emergency starts, and how many are to carry the emergency load. Some of the large utilities have engineering staffs who have set up their own methods for determining what emergency load current can be carried on a cable. Technical papers have been published recently which give methods for estimating these currents. One by N. B. Walsh of the Duquesne Light Company was presented before the transmission and distribution committee of Edison Electric Institute in Pittsburgh last February; another by A. R. Eaches of the Philadelphia Electric Company, was presented to the AIEE Philadelphia Section; and a third one I gave before the joint meeting of the AIEE Southwest District and the Missouri Valley Electrical Association. In it, I show how it is possible to determine the emergency current under the conditions stated previously from the Insulated Power Cable Engineers Association current ratings, the thermal time constant of a cable, and the time of the emergency. Naturally, a short time such as one hour will permit a higher current than a two-hour or four-hour overloading, and a cable carrying less than its rated load at the time the emergency load starts can carry a larger current than if it were carrying full load.

I believe this war guide, along with the conference papers, is going to be very helpful in guiding the utility engineer as to how far he can go to meet emergency conditions with the knowledge that he is shortening the life of a cable and must accept an increase in the cable-failure rate. A large utility with several cables supplying power between two points and an installation crew available can take advantage of this guide better than one not so well placed. In the latter case the risk may be too costly to take.

C. T. Hatcher (Consolidated Edison Company of New York, Inc., New York, N. Y.): As a member of the AEIC subcommittee which prepared the guide, I wish to bring out that we recognize the fact that we do not know definitely what will occur to systems when the proposed loads are applied. However, we believe that we have taken into consideration all available data and factors and that the proposed loadings are reasonably satisfactory for the conditions under which they are to be applied.

In order to obtain the full value of the use of the proposed loadings, the subcommittee requests that all companies using the proposed loadings maintain accurate records of the operating results, so that they will be readily available for further study. Data obtained in this manner will be of great help in future consideration of allowable operating copper temperatures.

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): Mr. Halperin's committee has taken a useful step in preparing this guide to indicate allowable cable temperatures under war conditions. It is unfortunate, however, that the guide expresses its recommendations in terms of temperatures without giving any indication of how such temperatures are to be used.

The temperatures referred to in the tables are defined as "those at the hottest portion of the cable in a given line or circuit."

In the case of cables in ducts, this hot spot will be at the conductor surface and at some considerable distance from the duct mouth. It is evident that the operating engineer has no way to ascertain what is the hottest portion of the cable, and, even if he did, he would be unable to measure its temperature.

This condition is complicated by the fact that the great majority of cables must be laid under such a variety of conditions with respect to thermal dissipation and vintage that they cannot be engineered individually, but must be treated in groups corresponding usually to types only, and sometimes to general installation conditions with uniform rules applying to all members of a group.

The temperatures recommended in the guide have been derived either from practice or from laboratory data. In the former case there must have been some formula whereby hottest-spot temperatures were derived from something that was measured. In that case the authors of the guide would do well to reveal this rule, so that those who want to use it may work the rule backward and derive something measurable from the tabulated unmeasurable temperatures.

However, I suspect that no such rule has been formulated and that the tabulated temperatures have been derived mainly from laboratory work, in which case the hottest spots were obtained by substantially direct measurement. If that is the case, the method of using the guide is anyone's guess.

If I might hazard a guess, the average user of the guide will, in all probability, take the IPCEA tables and increase the current ratings by a factor based on the greater temperature rise and higher final temperature allowed by the guide. For example, 750,000-circular-mil three-conductor-shielded 22,000- or 23,000-volt cable alone in a duct bank, operating at 100 per cent load factor and earth ambient of 20 degrees centigrade has a carrying capacity of 495 amperes according to the IPCEA table, which allows a final temperature of 77 degrees centigrade. Corrected for the guide's hottest-spot temperature of 82 degrees centigrade for normal operation, the current will be

$$495\times\sqrt{\frac{(82-20)}{(77-20)}\times\frac{1+(0.00393\times 57)}{1+(0.00393\times 62)}}$$
$$=512 \text{ amperes}$$

The same method applied to the 100 degrees centigrade temperature for emergency operation gives a current of

$$495\times\sqrt{\frac{(100-20)}{(77-20)}\times\frac{1+(0.00393\times 57)}{1+(0.00393\times 80)}}$$
$$=566 \text{ amperes}$$

If the IPCEA carrying-capacity tables had been based upon the most optimistic assumptions of thermal and electrical properties rather than upon the conservative values actually used, a current of 587 amperes would be required to bring the conductor temperature even to the normal 77 degrees centigrade, allowed by IPCEA.

We may, therefore, conclude that *new* paper cables, installed under favorable conditions subjected to currents calculated from the guide temperatures, by this procedure, will not be harmed, because they will not exceed normal operating temperatures. On the other hand, old paper cables and those not favorably situated will attain temperatures which will cause an increase of failure rate and an ultimate lowering of useful life.

The recommendations for varnished cambric are, however, in another category. This material is a very capricious one, and capricious materials are not very reliable when pushed to their limits. That is, however, precisely what the guide proposes to do.

One reason for the capricious nature of varnished cambric is its extremely hygroscopic nature which causes the dielectric loss to vary over a range which is dangerous when considered in connection with accumulative heating. This action may be initiated by high hottest-spot temperatures such as are listed in Table II of the guide. Numerous failures of high-voltage varnished-cambric cable are on record where accumulative heating has been caused either by high ambient temperatures or by heavy loads combined with poor conditions of heat dissipation, neither of which would have affected paper-insulated cables. While the IPCEA tables are conservative enough for low voltages, they leave very little margin of safety for cables rated at over 7,500 volts. I, therefore, consider the recommendations of Table II to be distinctly dangerous.

The recommendations for rubber, in Table III of the guide, are also questionable, particularly for code- and ozone-resistant rubber. The former will not stand 60 degrees centigrade for very long, especially if not protected by a lead sheath.

With regard to ozone-resistant rubber, a series of oven tests on several of the best-known makes shows that they deteriorate quite appreciably even at 70 degrees centigrade and have very short life at 80 or 85 degrees centigrade.

T. H. Haines (Boston Edison Company, Boston, Mass.): The AIEE paper entitled "Guide for Wartime Conductor Temperatures for Power Cables in Service" covers the field quite thoroughly and is particularly valuable for showing the many factors that affect the operating temperature of cables.

In paragraph 2.41 of the guide it is stated that the wartime "normal" temperatures are assumed at least to double the rate of deterioration of the cable as compared with previous practices. It would be of interest at this point, if possible, to state approximately what these rates might be.

In paragraph 3.22 of the guide the effect of racking length and offset of cable on sheath cracking are mentioned, and it is stated that when these values are below the limits of good practice the allowable temperatures should be reduced. Although the limits of good practice must be more or less arbitrary, an approximate table of values would be a helpful addition to the paper. The Boston Edison Company uses the following table:

Cable Size	Minimum Racking Distance (Feet)	Minimum Offset (Inches)
3x1/0 to 3x4/0	9	14
3x300,000 and 350,000 circular mils	10	16
3x400,000 and 500,000 circular mils	11	18
3x700,000, 2x1,000,000 and 2x1,500,000 circular mils	12	20

Section 3.32 of the guide is valuable in emphasizing the importance of temperature surveys in the field. It might be well to mention here the value of soldering thermocouple wires to the sheaths of cable sections during installation in known "hot spots." This permits accurate determination of sheath temperatures, and, hence, conductor temperatures, to be made whenever desired.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): With regard to allusions by Del Mar and Wiseman, to some of the temperatures being excessive, I should like to re-emphasize the points in the last two sentences of the preface of the guide:

(*a*). The use of the temperatures will cause large increases in troubles.

(*b*). The temperatures are proposed only as a wartime measure, because their continued use may make the total annual charges for a cable system 20 or 25 per cent, instead of the approximate normal figure of 12 per cent.

Mr. Davis brings out the point, which has been demonstrated elsewhere, too, that day-in–day-out operation at full peacetime temperatures will cause great increases in failure rates. A large majority of the underground circuits in peacetime seldom operates up to peacetime temperature limits, and the country's average rate of cable failures in service of five per 100 miles of cable per year is really based on lower temperatures.

In answer to Mr. Haines' question, continued daily operation at the "normal" temperatures given in the guide might cause a failure rate of 15, 20, or 25 per 100 miles per year for three-conductor 13-kv cable. Usually, more is to be feared from the effects of daily operation at proposed "normal" temperatures than from operation at the proposed temperatures for rare emergencies.

Determination of maximum temperatures to use in wartime is no easy task. Differences of opinion exist as indicated, for example, in the difference of 10 or 15 degrees centigrade for ozone-resistant rubber implied in the remarks of Del Mar and Wiseman. Experience substantiates their implication that the resistance of varnished-cambric cables to temperature varies considerably. However, it may be noted that such insulation, when in apparatus other than cable, is usually given higher temperature limits than in the guide.

It is difficult to frame a short answer to Mr. Del Mar's apparent confusion as to how the temperatures will be used. The proceedings of the American Institute of Electrical Engineers, National Electric Light Association and Edison Electric Institute, as well as foreign publications, contain considerable information as to the determination of the maximum temperatures of a cable circuit. Temperatures found in field experience as well as laboratory data were used in setting the guide temperatures. Of course, the actual "hot-spot" temperatures of a circuit most likely will be a few degrees different from the value as ascertained by the methods available. Likewise, the literature, including present publications, is full of information and argument on methods of determining accurately the "hot-spot" temperature for transformers, motors, and so on.

As recently as this summer, I find, to my surprise, that the temperature limits for wartime conditions in England are still *even less* than the *peacetime* limits of this country. Surely copper, lead, rubber, and so forth are not more plentiful there than here. These facts and others demonstrate that the utilities that started this guide are willing to make, as necessary, unusual sacrifices to save critical materials as a wartime measure.

The discussions at, and prior to, this meeting regarding the guide have been interesting and worth while. However, as indicated in the discussions, the maintenance of accurate and complete records of operating experiences is very desirable from several angles for those cases where cables are operated at, or near, the temperature limits given in the guide.

A Capacitance Bridge for Determining the Ratio and Phase Angle of Potential Transformers

Discussion and author's closure of paper 43-81 by H. W. Bousman and R. L. Ten Broeck, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 541–5.

F. B. Silsbee (National Bureau of Standards, Washington, D. C.): This paper constitutes a very noticeable milestone in the progress of precise measurements in the field of high-alternating voltages. Capacitance potential dividers have been used hitherto with good success in determining the ratio and phase angle of voltage transformers, but in these earlier methods it has been necessary to know with high precision the capacitance as well as the phase angle of the capacitor. The present authors have made the very great contribution in that they have divided the responsibility of the measurement in such a way that the precise determination of ratio depends only upon the values of resistors, which can be measured readily, and which are very constant in time, while the values of phase angle are determined by the air capacitors which are eminently suited for this purpose. Their use of the bridge connection, which is usually called Schering, but which should more properly be called Thomas (as will be seen by the authors' reference 11), has proved to be the key which brings about this much-desired situation.

A number of questions of detail naturally arise in the mind of the reader, and I hope that the authors can supply the answers to these in their closure:

1. What type of detector do they use, and what is its sensitivity?

2. How are the conductances of the many resistors intercompared, and what is the construction of the conductance dials?

3. What voltage is used on the bridge circuit when they obtain their calibrated balance? Presumably, if the voltage were the geometric mean between the primary and secondary voltages of the transformer under test, the current through the resistance arms would be the same as it is when the balance against the transformer is made. Is it necessary to take this precaution?

If, as the authors state, the reference list is to show the present state of the art, it should perhaps include, in addition to references 1, 5, and 10, which give the methods used at Pittsfield, Manchester, and Berlin, those listed at the conclusion of this discussion which describe in similar fashion the methods now in current use at Teddington and Washington.

REFERENCES

1. PRECISE ELECTROMETER METHOD FOR VOLTAGE-TRANSFORMER TESTING, R. S. J. Spilsbury.

Institution of Electrical Engineers *Journal* (London, England), volume 67, September 1929, page 1143.

2. DESIGN AND CONSTRUCTION OF A SHIELDED RESISTOR FOR HIGH VOLTAGES, R. Davis. Institution of Electrical Engineers *Journal* (London, England), volume 69, August 1931, page 1029.

3. A SHIELDED RESISTOR FOR VOLTAGE-TRANSFORMER TESTING, F. B. Silsbee. Scientific papers of the National Bureau of Standards, volume 20, 1926, page 489 (*S*516).

4. A TRANSFORMER METHOD FOR MEASURING HIGH ALTERNATING VOLTAGES AND ITS COMPARISON WITH AN ABSOLUTE ELECTROMETER, F. B. Silsbee, F. M. Defandorf. *Journal of Research* of the National Bureau of Standards, volume 20, March 1938, page 317 (*RP*1079).

H. W. Bousman: Doctor Silsbee's remarks are very helpful, and we appreciate them very much. He has asked several questions and we are glad to answer them as follows:

1. The detector is a battery-operated electronic-tube amplifier, housed complete with batteries in the small case within the bridge shown in Figure 8 of the paper. Sensitivity is such that one microvolt may be detected.

2. The chart listing the successive steps in comparing resistors is rather lengthy. It will suffice to say that it takes account of residual conductance of the dielectric supporting the switches.

3. The calibrating balance is done at about 700 volts rms. The maximum power dissipation in a resistance coil is only 11 milliwatts; therefore, the authors have not been concerned with maintaining equal power in the coils.

A Method for Correlating Duty-Cycle Tests on Solenoids

Discussion of paper 43-108 by J. E. Ryan, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 579–81.

L. T. Rader (General Electric Company, Schenectady, N. Y.): A-c solenoids perform many more useful functions in industry than are generally realized. They are used primarily for applications where a high force must be exerted through a one- or two-inch stroke in a very short time, and such applications are very numerous. The problem of calculating the permissible duty cycle for such a device is practically impossible because of the large number of variables involved. Some of these will be discussed briefly.

An a-c solenoid is normally designed to give a certain pull at a certain stroke. The allowable rating is determined from a heat run in the closed position. The total watts in the device are divided among the coil, pole shader, and iron. The rating, however, also depends upon the type of mounting: for example, whether it be on a wood base or a steel base. Although the stated pull of a solenoid is given as a definite figure, that figure is usually the minimum point on a pull curve, such as shown by Figure 1 in the paper. The pull, therefore, may vary considerably from point to point, making an analytical expression for it impossible.

When a solenoid in the open position is energized, a current flows, the magnitude of which may be as much as ten times the magnetizing current of the device; in addition, there will be a transient depending upon the point of closure. In the open position, the watts are divided primarily between coil and iron, but the percentage varies with the stroke. The loss in the pole shader is negligible when the solenoid is in other than the seated position.

Mr. Ryan's analysis, depending, as it does, on only a few test runs, is extremely useful and successfully eliminates inaccuracies which might occur simply by extrapolating test data, as well as by eliminating the necessity for taking so much data. This method of analysis can also be used to compare two solenoids which have the same rated pull but are radically different in physical dimensions. By taking a small amount of data on each, the duty-cycle ability of the two solenoids can be evaluated and an accurate comparison made. This latter point is often overlooked by those who compare solenoids on a basis of rated pull and stroke and use size as the only criterion.

Mr. Ryan's method of analysis, of course, is a perfectly general one which can be applied to any electromagnetic device operated under conditions which make the calculation of performance difficult, and for which usually large numbers of experimental data must be taken.

A very interesting conclusion may be drawn from Figure 5, curve *B*. If the stroke is reduced from 100 per cent to 50 per cent, the degrees rise per operation per second falls from 155 to 45, a ratio of 3.4 to one. The solenoid, therefore, is often capable of doing more useful work when operating at a short stroke than at a long stroke. When there is a lever arrangement to the load, as there so often is, this type of data allows the optimum stroke to be chosen for maximum output. This same conclusion agrees with those made by D. K. Frost.[1]

REFERENCE

1. SOLENOIDS SPEED SEQUENCE CONTROL, D. K. Frost. *Machine Design*, volume 12, September 1940, pages 41–4, 92.

Adequate Electrical Maintenance Essential to Transportation

Discussion of paper 43-78 by W. J. Clardy, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 517–21.

E. E. Kearns (General Electric Company, Erie, Pa.): Present-day maintenance emphasis is concentrated on practical scheduling for a particular equipment and a particular service. Greater care is given to attain a practical median between excessive tinkering on the one hand, and, as they fail, repairs on the other, and to schedule this median program insofar as possible into three gradations of inspection and a major overhaul, usually on a mileage basis.

Close engineering study is given particular equipment items so as to schedule each adjustment, renewal, or rehabilitation at its proper time in the graded programs.

For rotating generators and motors, the four most important items of maintenance can be classed as follows:

1. Keep equipment clean and as free as possible from dust, dirt, and grease. This includes nonvisible locations, particularly armature core ducts. Life of modern rotating machines depends on their thorough ventilation; consequently, it is of utmost importance that armature core ducts be kept open.

2. Watch commutators closely, and dress at first signs of any distress, until commutators take on their proper polish. If eccentricity or high or low bars develop, commutators should be stoned in their bearings or turned, as close limits and concentricity are prerequisites to good commutator performance, especially on modern high-speed machines.

3. Lubricate properly and in this connection *do not overgrease antifriction bearings.*

4. Dip and bake armatures and fields at regular intervals.

The four most important items of control-equipment maintenance can be classed as follows:

1. Keep the equipment clean, and free from dust, dirt, and grease.

2. Keep apparatus properly adjusted, including contacts and contact pressures.

3. Maintain tight connections of terminals, contacts, tips, and so forth.

4. Inspect and replace items which would court trouble before next inspection.

Application of Carrier to Power Lines

Discussion and author's closure of paper 43-80 by F. M. Rives, presented at the national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, pages 835–44.

E. H. Bancker (General Electric Company, Schenectady, N. Y.): When carrier started as a youngster some 20 years ago, it undertook the hardest job first, that is, the transmission of intelligible speech. This had both good and bad consequences. It was good because it showed the need for further development work to surmount the difficulties encountered and instigated the program of development which has been continuous. It was good because it commenced the acquisition of experience with about all of the application obstacles instead of adding them one by one, as more complex applications were attempted. This has given a great many more years' experience from which to analyze and predict the probable behavior of proposed applications. On the bad side is the fact that failure to secure wholly successful operation implanted in the minds of certain individuals a distrust of anything bearing the name "carrier," and this has been most difficult to eradicate. This probably is the major factor in the relatively slow growth of carrier installations over as long a period as 20 years.

Carrier has now come of age, being something over 21 years old, and its possibilities are just beginning to be realized by those who can gain the most from its use. During these years it went through the usual development era of a new product as experience and development in associated equipment showed the way toward improvement. Like most products, this development era explored many possibilities that showed promise and eventually settled down with component designs, utilizing those constructions which give the most for the money. While carrier equipment scarcely falls in the category of mass-produced apparatus, the designs have become standardized sufficiently so that it is repetitively produced. As always, this means still more for the money since engineering can be concentrated on improving a small number of standard items rather than scattered over custom design of a wide variety of items. There is also the usual factory saving in the skill acquired by workmen and the possibility of using tools and production methods that either cut the costs of a given article or permit a better article to be made at the same cost. It is evident from this paper that many of the components of carrier have reached this standardized stage, in which the user gets an ever better product at a constant or decreasing price.

Once a product has become standardized and has been in service long enough to acquire a background of experience, it becomes possible to analyze and predict with fair accuracy its application in service. This leads toward standardized applications where the application engineer is provided with necessary information to analyze the behavior and properly apply equipment. Wide dissemination of the application data acquired by experience is necessary for the lowest-cost successful application. Users must know what the capabilities and limitations of the entire channel are, so that they may make successful applications and understand what lies behind the manufacturer's recommendation. Users sometimes intimate that they think manufacturers are loading up a proposition with unnecessary equipment just to sell more, but this is seldom true. Information such as that contained in Mr. Rives' paper should form a fundamental basis for an intelligent approach by any user to the investigation of a proposed carrier installation. It ought to be in the file of every one who has any contact with carrier.

E. E. George (Southwest Power Pool, Little Rock, Ark.): This paper presents excellent material long needed by power system operating and design engineers. It takes much of the mystery out of carrier-current application and it should result in more extensive use of carrier-current equipment as well as more economical use of the various types and sizes available.

It is hoped that other manufacturers as well as users will contribute to the discussion of this paper. It fills a gap in present power-engineering literature and deserves wide distribution and study.

J. D. Booth (Westinghouse Electric and Manufacturing Company, Baltimore, Md.): The data in this paper agree very closely with similar data collected independently by the Westinghouse Electric and Manufacturing Company. However, in a paper "Power-Line Carrier Communication" by the writer and A. P. Bock, presented in 1941 (AIEE miscellaneous paper 41-63), an average figure of four decibels was given for coupling losses including the coaxial cable. This average figure also included losses due to unavoidable impedance mismatches and stray coupling to power conductors not a part of the carrier circuit. Since these effects are usually associated with the terminal and coupling equipment they were also included by me as coupling losses rather than by a multiplying factor as shown by Mr. Rives in Table VI. While the recommended figure of four decibels is somewhat pessimistic, Mr. Rives gives a figure of one decibel which may be misleading since it does not include several important factors.

A. H. Burkhalter and **D. M. MacGregor** (Ebasco Services, Inc., New York, N. Y.): The growth of system interconnections has increased the importance of both frequency selection and carrier attenuation beyond anything suggested by previous experience with inherently isolated channels. Many of the interconnection channels must operate on a broadcast basis, and the approach to the problems of application are totally different from those encountered in the past. One such application problem with which we have been connected involves frequency allocations for some 35 new channels in an area in which a number of carrier channels are already in operation. Using the data given in Table IV of Mr. Rives' paper it has been calculated that many of these new channels will be subject to greater attenuation due to branches and loops than that caused by straight away line losses.

In discussing the reflection losses due to relatively short taps or loops, Mr. Rives omits mention of the fact that it is relatively simple to calculate the critical lengths of any given frequency. One of the smaller manufacturers dealing extensively in carrier-current equipment has prepared this information in the form of curves showing critical lengths as a function of frequency. These curves have been most useful in determining appropriate frequencies in advance, and also as an aid in locating troublesome taps or loops when high attenuations are encountered in the field.

Advance field determinations of carrier attenuation may also be used to advantage although it is sometimes found that variations in switching setups make it appear that there is no suitable frequency. This is particularly true when a number of channels are to operate in a single closely spaced frequency group from several remote points to one common receiving point. Probably the results of such an attenuation study are chiefly useful in avoiding particularly poor frequencies.

The arrangement for a line-tuning assembly for three independent and widely spaced carrier channels, shown in Figure 5, can be simplified by the elimination of one low-voltage trap without increasing difficulty of adjustment and with some reduction in both losses and cost.

The use of band-pass elements for multichannel by-pass circuits, mentioned briefly in Mr. Rives' paper, should receive very serious consideration because of the flexibility, low loss, and relatively low cost of such elements compared with the more conventional by-pass assemblies. A recent problem involving a by-pass for three phase–ground channels and one phase–phase channel and including a shift of phase use has been solved very nicely by the use of a band-pass system for the phase–ground channels combined with a conventional by-pass for the phase–phase channel. Adoption of this arrangement reduced the number of capacitors and the quantity of concentric cable very materially without any increase in losses.

Mr. Rives is to be complimented, not only because his paper is clear and concise, but more particularly because it represents a long needed step toward advancing carrier-current application. Information in the paper is of immediate interest to all who have occasion to apply or operate carrier equipment, and its presentation should go far to remove from the minds of operating engineers the feeling that carrier is both mysterious and unpredictable.

S. L. Goldsborough (Westinghouse Electric and Manufacturing Company, Newark, N. J.): This paper is a compilation of information concerning the factors to be considered in the superposition of carrier-current channels on power lines. It is believed that the paper is a beneficial contribution in that it brings together in one place most of the facts which must be known by those engineers who have to deal with carrier-current channels on power lines.

I believe the part pertaining to the application of line traps is liable to give a somewhat false impression. As far as carrier relaying is concerned, the only absolutely compelling reason for using line traps is to prevent interference by external ground faults. Independence of these external faults can be attained by the use of one line trap. The other beneficial results from the use of line traps, such as, "To reduce transmission losses in irrelevant branch lines, to minimize the effect of low-impedance shunts, and to isolate effectively the carrier channel" cannot be realized to the fullest extent by the use of a line trap in one phase. The paper gives the impression that the carrier channel can be isolated effectively by the use of single-phase trapping. This is definitely not true since a number of cases have come to my attention where apparently the effect of one trap in isolating the carrier frequency from the rest of the system was practically negligible. It should be remembered that there is comparatively large coupling between the phases of the transmission line and also to a closely spaced parallel line. The application of traps to only one phase does not affect the coupling of the other two phases, and thus the carrier energy can permeate the rest of the system with the possibility of encountering severe reflections. Accordingly, the statement in the paper that with line traps the carrier can be isolated in a particular section and, therefore, its frequency can be predetermined is not true, but becomes partially correct if all three phases are trapped.

A statement is made in the paper that the main disadvantage of the line-to-ground coupling is in its somewhat higher attenuation and higher noise level as compared with the interphase circuit. I would like to ask the author if he has any definite figures regarding this increase in attenuation and noise level.

With reference to Table IV, I would like to ask if the decibel losses were calculated or were they a result of laboratory or field tests? Some of them looked quite optimistic. The loss figures in item *B* and particularly item *E* are too low. For example, item *B* with the assumption that all lines are long so that reflections are negligible, the power entering the junction goes three ways. It goes forward on two lines and backward on the incoming line. The voltage at the junction is the same on all lines, and, therefore, the power on each line is one-third the incoming power, or 4.7 decibels loss. Item *E* would be 7 decibels on the same basis. Item *E* may give very large losses if the loop differs from the line by an odd number of half wave lengths. In other words, on a loop circuit the carrier waves reaching the junction point adjacent to the receiver may be out of phase by any degree, and therefore, the loss may be practically anything.

F. M. Rives: Mr. Booth appears to think that a loss of only one decibel for the line-tuning and coupling equipment, as indicated by item *A* of Table IV, is somewhat on the optimistic side. As a matter of actual measurement, the losses in this portion of the circuit usually fall well under this figure except for the most complex tuning arrangements. The figure of one decibel, therefore, is considered to be a fairly safe average. Since the losses in the lead-in cable are directly dependent on the length and frequency used, it is frequently advantageous to compute this loss item separately. The mutliplying factor, as indicated in Table VI, is not intended so much to cover miscellaneous losses as it is to provide for variations in the transmission-line loss and to differentiate between the signal-noise requirements of different services.

In the discussion by Mr. Burkhalter and Mr. MacGregor it was indicated that in dealing with reflection losses due to short taps or loops the critical line length for any frequency could be readily calculated. While it is true that the frequency interval between nodes or peaks in the transmission characteristic may be determined fairly closely from the line length, the actual frequency or frequencies at which a tap line will appear as a very low impedance or a very high impedance depends also on the terminating impedance as well as the electrical length. Since most branch or tap lines terminate in a transformer bank, for which the carrier-frequency impedance is not readily determined, it is not usually possible to calculate the frequencies at which a particular tap will cause trouble.

In his discussion, Mr. Goldsborough indicates that a carrier-relay channel may be made independent of external faults by the use of one line trap. It is assumed that he means one line trap associated with each terminal; that is, for a two-terminal channel a line trap would be used in one phase at each of the two terminals. He is quite correct in pointing out that the other benefits provided by line traps cannot be realized to the fullest extent by single-phase trapping. However, the increase in efficiency gained by trapping all three phases of a line for a single channel is usually not sufficient to justify this additional expense and complexity. In speaking of isolating a channel by the use of traps, it is not intended to imply that the use of traps in a single phase will prevent the transmission of the channel frequency into other phases and into other parts of the system beyond the traps. Even with traps in all three phases some carrier-frequency energy will get through the traps and certainly will be transmitted around the traps where parallel circuits are present. However, single-phase trapping will isolate a circuit effectively as far as the influence of portions of the line outside of the trapped section on the carrier-frequency characteristics of the trapped section itself. This definitely provides freedom in the choice of the channel frequency as far as its transmission characteristics are concerned; it also provides independence of line switching. Both of these advantages are quite important for certain types of service.

In answer to Mr. Goldsborough's request for definite figures on the increase in attenuation and noise level encountered on phase-to-ground circuits as compared with phase-to-phase circuits it may be stated that data from actual field measurements indicate an increase in attenuation of from 10 to 50 per cent for the phase-to-ground circuit, depending on a number of factors, including ground conditions, presence of other possible return paths, and others. No definite figures on comparative noise levels are available; however, observations on many installations have indicated a general increase in noise level for the phase-to-ground which has been estimated to be in the range of from one to six decibels.

The circuit configuration losses indicated in Table IV are largely empirical, based on accumulated experience. However, they are not too far from calculated values for similar conditions.

Carrier-Current Differential Protection for Transformer Banks

Discussion and author's closure of paper 43-91 by T. A. Cramer, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 545–8.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): Probably the most worth-while contributions by relay engineers to the supplying of electric power are those which make possible the rendering of adequate service with less system investment in major equipment. Particularly in wartime, when materials are precious, we are justified in expanding the range of special schemes for this purpose. Therefore, the proposed method of utilizing a carrier-current channel to trip a remote oil-circuit breaker when the local differential relays indicate a transformer fault is most timely.

Widely different degrees of reliability of a pilot-wire or carrier-current channel may be required, depending on the method of relay protection used. A-c pilot-wire schemes which inherently may experience false operations if the pilot-wire circuit is momentarily short-circuited or open-circuited may be used on short privately owned pilot-wire channels with excellent reliability.[1] When leased circuits of greater exposure (and which are exposed to personnel interferences at exchanges) are used, it is the more usual practice to provide relay schemes such as the d-c directional comparison type (or a-c pilot-wire systems interlocked with basic fault-detector elements of the d-c type) so that the relay scheme is immune to pilot-wire disturbances which occur when there is no fault on the protected power line. Transferred tripping has been used satisfactorily with d-c pilot-wire schemes, particularly if the impulse is maintained at a level above the usual telephone-exchange battery circuits.

Carrier-current protection is inherently of the type on which a momentary disturbance to the channel does not cause false operations in the presence of load current. Accordingly, some trepidation is experienced in viewing the extension of carrier-current channels to the transfer-tripping field, which by its nature cannot be interlocked with fault detectors. The fact that the author indicates an allowable maximum time-delay margin of 30 cycles shows that he perhaps shares these misgivings.

Satisfactory experience is reported with a three-tone (audio) carrier-current transferred-tripping scheme.[2] However, in some cases a local high-voltage grounding switch has been employed to be operated by differential relays rather than to use carrier-current transferred tripping, although the channel was available for relay protection.[3]

I would like to ask the following questions:

1. In the author's opinion, can a three-tone (audio) system such as described in January of 1942[2] with no intentional time delay, provide approximately the same reliability as the single-tone system with a 30-cycle delay?

2. Since lightning surges or their effects are ordinarily thought of as lasting less than one cycle, it would be interesting to know what specific reasons led to the adoption of the 30-cycle maximum time.

3. Can the scheme described be readily adapted to be used with a conventional two-frequency carrier telephone system?

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2. MULTICHANNEL CARRIER-CURRENT FACILITIES FOR A POWER LINE, P. N. Sandstrom, G. E. Foster. AIEE TRANSACTIONS, volume 61, 1942, February section, pages 71–6.

3. PERFORMANCE REQUIREMENTS FOR RELAYS ON UNUSUALLY LONG TRANSMISSION LINES, F. C. Poage, C. A. Streifus, D. A. MacGregor, E. E. George. AIEE TRANSACTIONS, volume 62, 1943, June section, pages 275–83.

E. H. Bancker (General Electric Company Schenectady, N. Y.): Mr. Cramer's paper should be interesting to those who are faced with extensions to their system under both the war restrictions as to the equipment which may be purchased, and under the economic restrictions of making the extensions at the least-possible cost compatible with successful operation. There have been many lines constructed in the past few years that terminate in transformers without high-voltage breakers, and there will undoubtedly be more. This type of installation always raises the problem of how to give adequate protection against damage within the transformer from a fault within the transformer. Differential relays may readily be applied between the low-voltage current transformers and bushing-type current transformers in the transformer bushings, but tripping of the low-voltage breaker by such relays does not remove the fault. Unless the fault is of sufficient severity to be recognized by the protective relays at the far end of the transmission line, it will continue to wreak destruction within the transformer until it does become bad enough to operate the line relays at the far end. This may result in a very expensive repair.

As an alternative to the high-voltage breaker, a carrier channel using much less critical material and at lower cost, perhaps even available for other relay purposes, can extend the tripping circuit of the differential relays to include the breaker or breakers at the other ends of the transmission line. If carrier is already used for the line protective relays, a relatively small amount of additional equipment will permit its use as an extension of the trip circuits of the differential relays.

Again, the results of experience enter the picture and after experimenting with the several possibilities that Mr. Cramer points out, a standardized method of sending a tripping signal has been developed. It should be emphasized that this is a much more difficult task than sending the blocking signal used in the customary carrier-pilot relaying, because a transient block generated by lightning, switching, or other causes does no particular harm, but transient tripping signal cannot be tolerated since it results in an unnecessary trip that should not be followed with a reclosure until the cause for the trip has been investigated and determined as external to the transformer.

Ben C. Hicks (Shawinigan Water and Power Company, Montreal, Can.): In his excellent paper, Mr. Cramer refers to the scheme he is describing as a remote-tripping scheme. The Shawinigan Water and Power Company has, on one of its 220-kv lines, a similar scheme which it refers to as a transfer-trip scheme. This scheme operates on the heterodyne principle which Mr. Cramer has included as number 3 in his list of possible methods of conveying remote-tripping information over a carrier-current channel. Experience with this installation has confirmed amply the need of special care being taken to avoid faulty operation due to various kinds of interference. It may be of interest to mention one instance of such faulty operation that was experienced.

In the case referred to, all three ends of a three-ended 220-kv transmission line were terminated in transformer banks. A fault occurred on the low-voltage side at one terminal, outside of the zone of the transformer-differential protection and, contrary to expectation, caused the operation of the transfer-trip feature. This fault was studied by the manufacturer of the carrier-current equipment and it was found that a prominent 2,000-cycle frequency was present which passed the 1,000-cycle high-pass filter provided as part of the heterodyne receiver. A slight modification was subsequently made in the heterodyne receiver to prevent a recurrence of the faulty action from this cause.

There are two questions I would like Mr. Cramer to comment on. The foregoing incident suggests the first one. In the scheme described by Mr. Cramer a 3,000-cycle, audio tone is utilized to convey the tripping information. Is it not possible that faulty operation would result should a system fault generate a strong 3,000-cycle frequency? The second question concerns the hand-reset multicontact auxiliary relay. It would appear to be an advantage if this relay did not require the attention of the operating staff to reset it. Would it be possible, with minor modifications, to use a self-resetting relay, or is it essential that a hand-reset relay be used?

T. A. Cramer: In reply to Mr. Hicks' discussion, the possible occurrence of a strong 3,000-cycle voltage on the high-voltage line has been considered in the design of this equipment. By using two inductively coupled tuned circuits in the receiver input, the sensitivity of the receiver at 3,000 cycles is very low. The receiver input circuit is tuned to the carrier frequency, and frequencies in the pass band of these circuits are accepted by the receiver. When the incoming carrier signal is modulated at 3,000 cycles, the output from the receiver operates the relay tuned to this frequency. This double selection gives ample selectivity to prevent operation by switching surges and faults.

In reply to Mr. Hicks' second question, it is possible to use a self-resetting auxiliary relay, but there is some advantage in using a relay which requires the recognition of this operation by the station attendant. When a remote transformer fault is tripped by the carrier equipment the operator should not reclose the line to the faulted transformer, and the hand-reset tripping relay warns him of this fact. If automatic reclosing is used, the hand-reset relay is used to block the recloser until the transformer fault is cleared from the circuit.

The discussion on this paper by Mr. Brownlee brings out some points not covered in the paper. Regarding his first question, I believe that the three-audio-tone system would have approximately the same reliability as the single-audio-tone system with 30 cycles delay.

In reply to his second question regarding the reasons for using a 30-cycle maximum time, this is determined largely by the characteristics of the time-delay auxiliary relay itself. A time-delay relay of the copper-slug type was selected and could be adjusted over a range of 6 to 30 cycles. Field tests have been made on various installations of this type of equipment, and the relay current in the 3,000-cycle relay was observed during staged short circuits, energizing and de-energizing transmission-line sections and transformer banks, and in no case was any interference observed. Until further operating experience has been obtained it has been the practice to adjust these auxiliary relays for an operating time of approximately 15 cycles.

The conventional two-frequency carrier telephone system lends itself readily to the addition of this type of remote tripping. A cut-off filter would be used in the telephone circuit to limit the speech band to 2,500 or 3,000 cycles, and the tripping tone would be sent at some higher frequency such as 3,500 or 4,000 cycles.

It is hoped that a paper will be presented at some later date, giving information on the operating experience obtained by various users of this type of equipment.

A Simple Method for the Determination of Bushing-Current-Transformer Characteristics

Discussion and author's closure of paper 43-110 by S. D. Moreton, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 581–5.

C. A. Woods, Jr. (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It is indeed encouraging to find additional support for the use of open-circuit excitation data in the application of bushing-type current transformers in relaying service, as proposed by Mr. Bottonari and the writer in 1940.[1] At that time we proposed the use of scalar addition of excitation and burden currents to obtain the total primary current, as evidenced by part *b* of Appendix III and Figure 8 of that paper. Its justification was based upon the fact that the burden power-factor angles and excitation current angles were usually of the same order of magnitude when the transformer core was not saturated. When the core was saturated, the excitation current increased so rapidly with any increase in primary current that the error introduced by this method was negligible. Mr. Moreton's paper provides further justification for the scalar method by showing that the errors introduced by the scalar method, as compared with the vector method, are less than the probable difference in the performance between commercial units of the same design.

There has been considerable discussion of the best method of calculating ratio- and phase-angle curves from the excitation data. This has been very natural since such calculations provided the best means of comparing results of transformer performance determined from excitation data with that obtained from ratio- and phase-angle tests. However, as the real meanings and uses of the excitation curves become more widely known, there should be a diminishing need for calculating ratio- and phase-angle curves.

The graphical methods of curve drawing and chart construction, as covered by this paper, are of particular interest to transformer designers and those who must prepare curves of transformer performance. However, the relay engineer will be concerned primarily with the importance of the ratio of Ie/Is, as covered by equations 5 and 6, and Figure 5 of the paper. This term, Ie/Is, when multiplied by 100 has been defined previously[2] as "per cent ratio error," and the equation for the curves of Figure 5 has been published previously.[3] Thus, both the ratio correction factor and phase angle of the transformer are a function of the term Ie/Is, which can be determined readily from the excitation data.

From the standpoint of simplicity and economy, it would seem to be to the advantage of the relay engineer to have the maximum of transformer data in as compact a manner as possible, requiring a minimum of calculating equations and charts. Based on the apparently justifiable assumption that the majority of relay applications are made with current transformers operating within a range of Ie/Is, equal to or less than 0.1, there should be little need for supplying any data, other than an excitation curve of secondary volts versus secondary-excitation amperes similar to the curves of Figure 4. Under these conditions, the per cent ratio error is less than ten per cent, and, as shown by Figure 5, the phase angle is less than six degrees, irrespective of the burden power-factor- and excitation angles. In a few problems, more detailed calculations might require the use of the excitation angle as shown in Figure 6. It is obvious that the curves of Figure 6 give all the data that are required, as the curves of Figure 4 can be calculated readily from Figure 6. However, since those of Figure 4 do not require conversion to the turns base of the current transformer, they provide a means of checking the majority of applications with a minimum of effort.

Since ratio correction factor and phase-angle curves are often inadequate or awkward in the investigation of certain relaying problems, such as those involving transient performance, parallel operation, current leakage paths through other transformer secondary windings in ground relay circuits, and similar problems, it would appear that only the excitation data need be furnished for bushing-type current transformers. It should be provided in such a manner that the type and method of calculation could be any one of the many proposed, which is best suited to the specific problem at hand. The curves of the form shown in Figure 4 and those in Figure 6, with the magnetizing and loss components added, will fulfill adequately the foregoing requirements. This would result in a material reduction in time spent on making ratio and phase-angle curves, which are often inadequate in range of current or burden, and the relay engineer would not have his files burdened with a large volume of unnecessary data.

If a method of determining secondary leakage reactance of current transformers, such as proposed by Mr. Wentz,[4] comes into general use, then the need for overcurrent ratio and phase-angle curves is entirely removed, and the methods used to portray the overcurrent performance of bushing current transformers could become equally applicable to all types of current transformers.

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3. CURRENT- AND POTENTIAL-TRANSFORMER STANDARDIZATION, J. E. Clem, J. H. Neher, E. L. Harder. AIEE TRANSACTIONS, volume 61, 1942, September section, pages 698–706.

4. A SIMPLE METHOD FOR DETERMINATION OF RATIO ERROR AND PHASE ANGLE IN CURRENT TRANSFORMERS, E. C. Wentz. AIEE TRANSACTIONS, volume 60, 1941, October section, pages 949–54.

A. C. Schwager (Pacific Electric Manufacturing Corporation, San Francisco, Calif.) Previously known methods for the calculation of the ratio error of current transformers were based upon vector addition of the secondary and the exciting currents. In his paper, the author shows that, for practical purposes, inphase addition produces sufficiently accurate results.

The author is to be congratulated for this discovery and for having postulated so clearly its usefulness, the result of which appears to be the simplest ratio-error calculation method proposed to this date. Although transformers today are using steel having exciting current phase angles as shown in Figure 6 (25 degrees at maximum permeability), it must be realized that the method becomes inaccurate when lower-power-factor steel is used. An explanation of this limitation seems indicated in order to prevent misapplications of the method, possibly on old transformers in service.

On page 583[1] the author states: "When the ratio errors are desired for a range of primary current values of a multiratio transformer, the labor of calculation becomes tremendous, even with the simplification of inphase addition." He then develops an interesting graphical method and proposes the use of a template.

The writer assumes that, as complete information on a particular transformer, the author intends to furnish a chart similar to Figure 1 or Figure 12 of his paper for a two-ohm burden and supplement this chart by an exciting current chart and a template for use when user's burden differs from two ohms, which is likely to be the case in 99 per cent of all applications. In Figure 4 of the closure to the discussion on a paper on current transformers[2] the writer has shown, that by using a slightly different scale, the author's Figure 1 can be made to apply to any burden value. Combined with the author's findings, this chart seems to offer a complete solution to the ratio-error-data problem, since from it the ratio error, or RCF, can be read directly for:

1. Any primary current value.
2. Any burden value.
3. Any practical burden power factor.
4. Any secondary turn number.

Such a chart eliminates a good portion of the calculations necessary in the author's method and avoids the necessity of the use of a template. It is suggested that it be given further study, particularly on the part of the relay engineer who is vitally interested in obtaining performance data as directly as possible.

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2. CURRENT-TRANSFORMER PERFORMANCE BASED ON ADMITTANCE-VECTOR LOCUS, A. C. Schwager. AIEE TRANSACTIONS, volume 61, 1942, pages 467–9.

W. R. Brownlee (Commonwealth and Southern Corporation, Jackson, Mich.): Many simple methods have been proposed within the last year or two for the determination of bushing-current-transformer characteristics, but most of these have recommended taking into account both the inphase and quadrature components of magnetizing current and relay impedance. While the vector calculations involved are, in themselves, quite simple, the volume of work required, even with such useful tools as a vector slide rule, is generally all out of proportion to the value of the accuracy thus obtained. This is particularly true in view of the uncontrollable variations in the iron characteristics and in saturation of the relay burdens described by the author. It is, therefore, believed that he has done a distinct service in demonstrating so clearly, that for the situations most commonly encountered, the direct arithmetical addition of bushing-current-transformer exciting current to the relay current to give the equivalent primary current (and thus, the ratio) will involve errors within usual slide-rule accuracy. The curves, which illustrate at a glance the approximate error to be expected by this method under various conditions, provide an excellent timesaver.

Unfortunately, saturation curves are seldom available for any except the newer bushing current transformers. Use has been made of the inphase method to secure synthetic saturation curves from available ratio curves. These in turn are then used to secure ratio curves for burdens higher than those covered by the manufacturer's information (particularly for ground relays), for ratios on taps for which curves are not available, or to extend the range of certain curves. For assumed values of primary current, the secondary currents are calculated by means of available ratio curves, and the difference between these currents and values secured by dividing primary currents by the turn ratio is plotted as the difference, or exciting current. Corresponding voltage values are obtained by multiplying the secondary-current values by the assumed burden impedance.

Accurate determination of ratio is frequently required for inverse characteristic ground relays, since the burden is usually high, and since magnetizing current must be supplied to three current transformers instead of to one. Even when using the inphase method, the calculation of residual-current-transformer performance may become involved. If all of the current is considered to flow through one conductor, then the voltage across one current transformer will be that caused by the impedance of the ground-relay plus the phase-relay burden, whereas the voltage across the idle current transformers will be the result of the impedance of the ground relay alone. Separate magnetizing currents may be computed and added to give the over-all equivalent ratio. A simpler method, which is entirely adequate unless high saturation is involved, is to consider that the entire circuit current is of zero sequence, so that currents equal and in phase occur in all three conductors. In this case, the same voltage is impressed across all three current transformers and is equal to the total or ground current multiplied by the ground-relay burden plus one third of the ground current multiplied by the phase-relay burden. Since actual ground faults will involve conditions between these two, the method recommended is to add one third of the phase burden to the ground burden and use this impedance to secure magnetizing-current values from the saturation curve, multiplying such values by three before adding them to the ground-relay current values to secure over-all ratio.

A "super-short-cut" method of securing residual ratio is to read from standard ratio curves the value corresponding to the primary current (total ground current) with a burden equal to the ground-relay impedance plus one third of the phase-relay impedance and multiply the indicated turn-ratio difference by three. For example, if the standard curve indicates a ratio of 62:1, whereas the turn ratio is 60:1; then the proper residual ratio is 66:1.

Theodore Specht (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): Mr. Moreton's method is quite ingenuous, but the making of the template would be no small task, one of which would be required for each size core. Also, the customer still would not be able to determine performance for any burden he desired.

Another approach to this problem can be made by a method that has two advantages. With one curve the customer can determine the ratio of a multiratio bushing-type current transformer for any burden or ratio, and it is a simple curve for the manufacturer to make. The curve is a modified excitation curve, not for the secondary winding of the transformer as in the usual case, but for the primary winding. In magnitude, the primary current is

$$I_p = I_s N_s + I_{ex} \cos (\alpha - \phi)$$

where

$I_s N_s$ is secondary ampere turns.

I_{ex} is the core exciting current from the primary of the transformer.

ϕ is the burden phase angle.

α is the core-exciting-current phase angle.

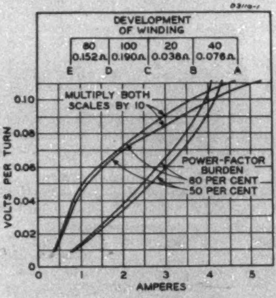


Figure 1. Curve for particular transformer instructions for using curve:

Instructions for using curve:

Given:

Secondary amperes
Secondary burden, ohms

Procedure:

1. Find total burden; add winding resistance to actual burden

2. Find volts per turn from

$$V/T = \frac{\text{secondary amperes} \times \text{ohms}}{\text{turns in winding}}$$

3. Read "amperes" from curve and add to this the secondary ampere turns to secure true primary current

Figure 1 is a curve of primary voltage as a function of $I_{ex} \cos (\alpha - \phi)$, or amperes, as it is called, for two values of burden power factor. To use the curve, the secondary voltage is calculated and divided by the secondary turns to secure the primary voltage. The value of amperes is read from the curve and added to the secondary ampere turns to secure the magnitude of the primary current.

From the manufacturer's standpoint, these curves are simple to make. For a particular grade of steel, a master curve, Figure 2 of this discussion, was drawn for a transformer with a net core area of $2^1/_2$ square inches and a mean core diameter of ten inches. These core dimensions, with the current scale one ampere per inch and the voltage scale 0.01 volt per inch, then gave a curve that fit the sheet well. On the master curve, though, no scales are actually drawn in.

For any particular transformer the voltage will be proportional to the core area. Hence, at one inch on the voltage scale on the master curve

$$E_p = (0.01)\left(\frac{A}{2.5}\right)$$

and, if at y inches the voltage is 0.01 volt

$$\frac{0.01\left(\frac{A}{2.5}\right)}{1} = \frac{0.01}{y}; \qquad y = \frac{2.5}{A} \text{ inches}$$

A curve for a particular transformer is prepared by drawing in the voltage scale on a reproduction (photolitho) of the master curve with 0.01 volt at $2.5/A$ inches, where A is the cross-sectional area of the core.

By a similar reasoning, the exciting current is proportional to the mean diameter of the core. Hence, at one inch on the current scale of the master curve

$$\text{Amperes} = 1\,\frac{ID+OD}{(2)(10)}$$

and, if at x inches, the amperes are 1 ampere

$$\frac{1\,\frac{ID+OD}{20}}{1} = \frac{1}{x}; \qquad x = \frac{20}{ID+OD}$$

Therefore, on the reproduction of the master curve for a particular transformer, the current scale is drawn in with 1 ampere at $20/(ID + OD)$ inches from the origin.

These instructions are given in Figure 3 of this discussion, which shows where to draw in the one-ampere line and the 0.01-volt line. Figure 1 is a curve drawn in this manner for a core with an inside diameter of six inches, an outside diameter of $7^1/_2$ inches, and a net core area of 4.28 square inches. On the curve is shown a development of winding and the winding resistance, along with instructions on how to use it.

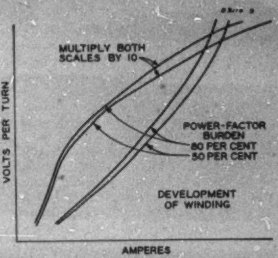


Figure 2. Master curve, reproductions of which are used by manufacturer in preparing curves for particular design

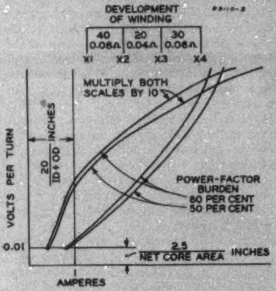


Figure 3. Instructions for manufacturer's engineers

A reproduction of the master curve is ordered, a scale put on according to above formulas, and a curve number assigned to the reproduction

Another possible use of these curves is to predict the inphase component of current drawn by "dead" current transformers in various relaying schemes. This current can be referred to the secondary by dividing the amperes from the curve by the secondary turns.

This system of making curves then has the advantages of simplicity in making, and greater flexibility and range in use. A similar scheme can be used to determine phase angle.

W. E. Marter (Duquesne Light Company, Pittsburgh, Pa.): Mr. Moreton's paper is a valuable contribution to the information on current-transformer characteristics, and, because of the simplification introduced, the methods described in the paper should be accepted widely by operating engineers.

Since the method depends on data taken from excitation curves, it is thought that some additional comment by the author on the method of obtaining the excitation characteristic would be helpful, and that possibly a standard method of obtaining excitation characteristics should be considered.

It has been found in making test-excitation curves on the secondary winding of a bushing transformer that considerable variation in the curve, above the knee of the excitation curve, is obtained, depending on whether sine-wave current or sine-wave voltage is maintained by the test source, and additional variation exists, depending on the type of instruments used to measure the current and voltage.

Mr. Moreton, in the summary of his paper, has pointed out the necessity for more complete information on relay burdens at overcurrents. It was suggested to the relay subcommittee, when revision of the manufacturer's catalogue sections was being discussed, that a curve be included in catalogue data on relays showing the burden at various currents. No results of this suggestion are apparent at the present time.

S. D. Moreton: The fundamental purpose of this paper was based on the desire to offer a satisfactory and conclusive answer to the questions raised by American Standard *C* 57.1, section 4.033, "Application Data for Current Transformers" paragraph *C* and *D* which state:

> *C* "Standard Ordinates and Type of Scales for Curves. Not yet established.
>
> *D* "Standard Method of Calculations. Not yet established."

The use of log–log paper for giving excitation characteristics seems advisable in order to cover a sufficient range of flux density for all relaying applications. The use of the special log–log paper with its triscales seems to present an ideal way of giving all ratio characteristics without calculation. The results obtained by inphase addition have been proved to be within acceptable limits based on available data for relaying applications and accepted without reservation by the discussers. The advantages of inphase addition are manifold:

1. The necessary data are kept to a minimum, since inphase addition does not require a knowledge of excitation and burden phase angles.
2. The required data may be measured easily with available instruments.
3. The calculated results give values on the pessimistic side, thus automatically compensating for the phase-angle characteristics of the transformer in regard to the relay application.
4. The calculation procedure is straightforward and thus easily remembered by the infrequent user.
5. Problems involving ground relaying with many transformers effectively in parallel are easily handled.

Mr. Schwager questions the accuracy of this method when used to calculate the characteristics of transformers having low-power-factor steel. Figure 4 of this closure shows the excitation characteristics of such a low-power-factor steel having a minimum phase angle of 50 degrees and is based on the same size core as the characteristics given in Figure 6 of the paper, which represents a high-power-factor steel having a minimum phase angle of 25 degrees. The errors introduced by inphase addition for this low-power-factor steel are best shown graphically, as in Figure 5 of this closure, which is comparable with Figure 1 of the original paper. As Mr. Schwager pointed out, the errors are somewhat greater for a high-power-factor burden, but they are also correspondingly smaller for a low-power-factor burden. If these calculated results are compared with Figure 6 of this closure, which represents the band ratio characteristics based on ±50 per cent variation in exciting current, the errors introduced by inphase addition are found to be a relatively small part of the total possible variation. A band curve of ±50 per cent is chosen as typical of the variation obtained on this old-type hot-rolled steel, as mentioned in the original paper under "Accuracy Factors." Mr. Schwager also mentions that by the use of Figure 4 of the closure to the discussion of his paper a good part of the calculations necessary for the template method could be avoided. Mr. Schwager's calculations give points on the ratio-correction-factor curves, many of which are necessary to determine the curve. The major advantage of the template method is that by solving only two equations: Per cent $I_e = CN/Z$ and $RCF - 1 = D/N$ (per cent I_e) which is only a single point, the complete ratio characteristic is obtained, and thus Mr. Schwager's point-by-point method is eliminated.

Mr. Marter mentions that in making tests on bushing current transformers considerable variation in the curve above the knee is obtained, depending upon the source and the type of instruments used (see Figure 7 of this closure). This variation is, of course, brought about by the presence of harmonics caused by the saturable characteristic of the iron core. If a sine-wave voltage is used any type voltmeter will give the same results, since the harmonics then must appear in the current circuit; but, because of the wave shape and the flatness of the curve above the knee, there is very little change in the current. If sine-wave current is used an iron-vane-type voltmeter will indicate an rms value of the wave that is appreciably greater than the results obtained with a rectifier-type voltmeter, which indicates the fundamental component very closely on this type of peaked wave. The majority of the data available, including the curves in this paper, are based on fundamental magnitude only, whether they are measured with a sine-wave voltage and any voltmeter, or a sine-wave current and a rectifier-type voltmeter. Thus, this type of excitation curve shows very clearly that there is relatively little increase in the fundamental component of the secondary current from a bushing current transformer once the knee of the curve has been passed. The core can be considered as a generator of odd-harmonic voltages for current in excess of that which saturates the core, and thus harmonics are fed into a parallel path consisting of the primary line circuit and the secondary burden circuit. Usually the impedance of the primary line circuit is relatively high compared with the generated back voltage, and the harmonics tend to flow entirely in the secondary burden circuit, distorting both the secondary voltage and the secondary current. These harmonics affect the relay response in different fashions, depending upon their sensitivity to the harmonics. Fortunately relay burdens, being primarily inductive, have burdens proportional to the order of the harmonic, and, even though the harmonic voltage is high, the actual current flowing through the relay is proportionately small. Finally, once the knee of the curve is passed, the ratio error increases abruptly, as evidenced by the ratio-correction-factor curves on log–log paper, and thus the application engineer would avoid operation beyond this point; therefore, it makes little difference what happens once the knee of the curve has been passed, since it is of academic interest only. Thus the conclusion is reached that the excitation curve could be taken in any manner desired; it is not necessary to use either a sine-wave voltage or a sine-wave current, and the source of excitation may be supplied, either from the primary or from the secondary. But it would be advisable to use a rectifier type of voltmeter in order to avoid the misleading results obtained by the generation of harmonics.

Mr. Specht, in his first paragraph, evidently misunderstood one of the main features of the template method. The ratio-

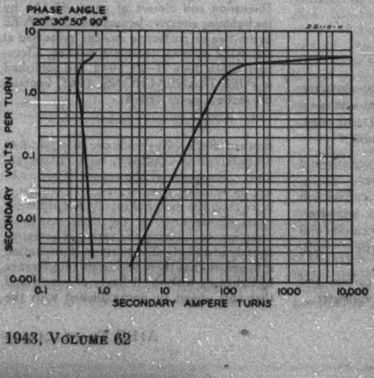


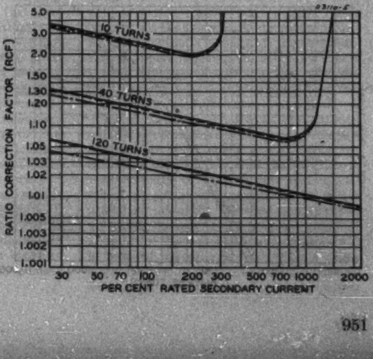
Figure 4 (left). Excitation characteristic of low-power-factor steel

Figure 5 (right). Calculated ratio characteristics with a two-ohm burden for the low power-factor steel

—— Inphase addition

—·— 90 per cent power factor

- - - 50 per cent power factor



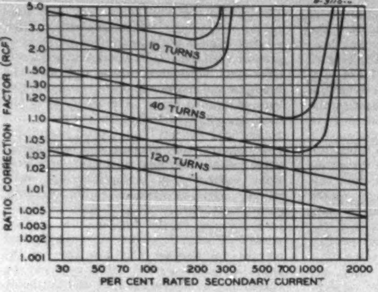


Figure 6. Band ratio characteristics on three ratios of a 600/5 multiratio transformer based on low-power-factor steel and ±50 per cent variation of excitation characteristics

correction-factor template, as given in Figure 11 of the paper, is good for all sizes of cores and all burden magnitudes. Since it is based on inphase addition, it is independent of burden and excitation phase angle. In addition, it is good for all commercial frequencies since the shape of the curves is unaffected by frequency; thus, one template will give any or all ratio characteristics.

The method of calculation described by Mr. Specht is based on the arithmetical addition of the primary component of the exciting current, which is in phase, with the secondary current to the secondary ampere turns to obtain the primary current. It is the same method suggested by Mr. Agnew.[1] This same fundamental method was also used by Mr. Wentz in his paper[2] on current transformers. Vectorially speaking, this type of addition gives $I_p \cos \beta$ which is in error to I_p by the magnitude of $\cos \beta$. Thus, the results so obtained are on the optimistic side, since they indicate ratio errors smaller than those obtained from the actual transformer. The application engineer, therefore, would have to allow a "factor of safety" to ensure correct operation of the relay scheme. This error introduces limitations on the use of the method, particularly as the difference angle $(\alpha - \phi)$ approaches 90 degrees, which is entirely possible with a unity-power-factor burden and core excitation just a little above the knee of the curve. Here the expression $I_{ez} \cos (\alpha - \phi)$ approaches zero as $(\alpha - \phi)$ approaches 90 degrees, thus making I_p equal to I_sN_s. Even though this is an extreme example, it typifies the trend of $\cos (\alpha - \phi)$, thus hiding the fact that the transformer is very badly off ratio. Conversely, the inphase addition method gives values on the pessimistic side, since it indicates ratio errors in excess of those obtained from the actual transformer, and thus a "factor of safety" is not necessary.

Mr. Specht's explanation of the master curves could be summed up more easily by stating that the voltage scale is proportional to the cross-sectional area of the core, and the current scale is inversely proportional to the mean length of magnetic path. Manufacturers could supply easily both an excitation curve based on unit area and unit length of path and one based on the actual area and length of path for the particular transformer; but this still represents a point-by-point method, whereas the template method gives complete curves at one calculation.

Mr. Woods brings out the point that, in the majority of calculations involving current transformers for relaying service, there is very little need for phase-angle data. This point might be emphasized more strongly by an analysis of relay operation. Only those relays that involve the comparison of two currents, or a current and a voltage, are affected by both ratio- and phase-angle errors. Most relays that compare two currents have restraint coils that permit a discrepancy between the two currents. In the case of a differential relay having a 25 per cent slope characteristic, operation will be successful from two current transformers that have a maximum *RCF* difference of 0.25. These relays purposely are made insensitive to phase-angle differences between the two currents. True impedance relays are, of course, independent of phase angle. Reactance relays will develop an error in terms of the line phase angle, which for a fault is usually around 75 degrees. This results in an error, for example, of sin 75 compared with sin 69 (six degrees difference as Mr. Woods pointed out as maximum) or 3.2 per cent error under the worst condition. The ratio error for such a condition, as pointed out by Mr. Woods, would be in the neighborhood of ten per cent. This 3.2 per cent error, introduced by ignoring the phase angle, would be compensated for by the pessimistic results of inphase addition, which would be in the neighborhood of 3.5 to 4 per cent. The conclusion is reached that, of the two inherent errors, the ratio error is by far the determining factor in those relay applications that involve both. It seems possible that, as this becomes more widely known, there will be little or no need for phase-angle data for any relaying application.

Mr. Brownlee's excellent analysis of the ground-relaying problem leaves little more to be said on the subject with the exception that the method chosen depends secondarily upon the location of the fault with respect to the transformer, the source, and the grounding point, and primarily upon the magnitude of the ground burden with respect to the phase burden. If this ratio is large (10 or more), which is usually the case, it does not matter which method is used, since all three transformers are effectively in parallel across the ground burden, and the phase burden is then negligible compared with the ground burden.

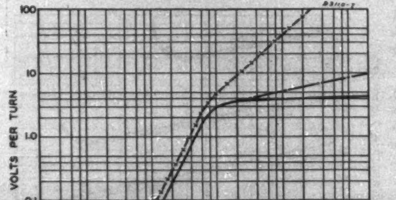


Figure 7. Saturation characteristic curves taken with sine-wave voltage and with sine-wave current

- - - - Sine-wave voltage and any voltmeter
——— Sine-wave current and rectifier-type voltmeter
—·— Sine-wave current and iron-vane-type voltmeter
—x— Sine-wave current and peak-type voltmeter

REFERENCES

1. **P. G. Agnew.** Bureau of Standards *Bulletin*, volume 7, 1911, page 432.

2. A SIMPLE METHOD FOR DETERMINATION OF RATIO ERROR AND PHASE ANGLE IN CURRENT TRANSFORMERS, **E. C. Wentz.** AIEE TRANSACTIONS, volume 60, 1941, October section, pages 949–54.

Report on Application of Lightning Protective Devices in Wartime

Discussion and closure of paper 43-111 by the lightning arrester subcommittee of the AIEE committee on protective devices, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 586–8.

Edward Beck (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The scope of the report, it seems to me, is broader than stated. It embraces more than the material involved in different types of arresters, or the maintenance of arresters, or lowering the rating of arresters already installed. The purpose of protection is to assure that there will be no interference by lightning with the delivery of power, and that this object be accomplished with the minimum expenditure of material and man-hours of maintenance or repair on the system. This is the scope of the report.

Under "Rebuilding and Revamping Old Arresters" it is apparently the purpose of the report as written to recommend merely the reducing of ratings of arresters by removing sections. Where this is safe, it is an improvement that involves no critical material. If, however, by revamping and rebuilding is meant the substitution of modern up-to-date elements in obsolete arresters, it will generally be found that less critical material will be involved in replacing such obsolete arresters by complete new ones.

In connection with mention of protective devices other than arresters, we believe it should be pointed out that devices that do not incorporate circuit-clearing features, such as rod gaps, are not, strictly speaking, lightning arresters because they will not prevent system outage unless they are assisted by other equipment such as fuses and circuit breakers.

There is a subject that has not been covered in the report which we believe should not be omitted, especially since the report puts particular stress on the conservation of material. It has to do with the lightningproofing of transmission lines. This is accomplished successfully by one of two means: overhead ground wires or protector tubes. In the past it has been logical to make the choice of one or the other on the basis of cost. Frequently, especially in new construction, the overhead ground wire has the advantage from this standpoint. However, it should be borne in mind that the amount of critical material involved in protector-tube installations will generally be less than the amount of critical material involved in an adequate overhead ground-wire installation. Consideration should be given to this in the design of new lines or in the rebuilding of existing ones, and I believe that a report on conservation of critical material in lightning-protective devices in wartime should call attention to this matter.

I. W. Gross (chairman, lightning arrester subcommittee 1942–43): Since the purpose of the committee report was to bring to the attention of the industry some features worth while in conserving materials and man-hours in the war effort, it is quite gratifying to find that Mr. Beck's discussion has amplified to some extent a few of the points mentioned in the paper, but not discussed fully therein.

The scope of the report necessarily had to be brief and concise, but as pointed out by Mr. Beck, application of one or more of the suggestions mentioned may have a much broader aspect than the mere saving of critical material; for example, maintenance of electrical service to a standard acceptable under war conditions.

On the subject of "Rebuilding and Revamping of Old Arresters," of the oxide-film type at least, there is involved a considerable increase in protection of equipment as well as a saving or salvaging of usable material, such as the oxide-film cell units. While it may not be possible in most cases to rebuild these salvaged units into new arresters, they are available for replacement when and if the cells in service become defective, as found on periodic inspection, thus not requiring new material in such servicing.

It is rather difficult to visualize how there is any saving in critical material by substituting new arresters for old ones, unless it is maintained that the salvage from the old arresters will more than compensate for the material used in the new ones. This, however, does not seem to be a very plausible basis for analysis.

The point made by Mr. Beck that lightning arresters, strictly speaking, are devices which on functioning will not cause interruption of power flow on the system, while other devices such as rod gaps may result in outages, is well taken. As mentioned in the report, however, the preference in the choice of an arrester as against a similar device which may result in a momentary power interruption is outside the scope of the report.

While the subject of protecting transmission lines from lightning by such means as shielding with ground wires or the use of protector tubes is of importance in any system layout, just how far to go in any particular case is one of individual judgment and local conditions.

This report limited its scope to protective devices as such and to their general application in the protection of various types of devices and equipment, and not to the protection of the electric system as a whole. There are many other ways of dealing with lightning on a transmission line; for example, by high speed reclosing breakers, use of ground wires, counterpoises, high insulation, and so forth. Just which of these devices or methods to use, if any, is again a matter involving a study of local conditions and individual judgment in each case.

Frequency Modulation for Power-Line Carrier Current

Discussion of paper 43-112 by E. W. Kenefake, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 616–20.

E. H. Bancker (General Electric Company, Philadelphia, Pa.): Although carrier-current equipment has become fairly well standardized, this does not mean that development has ceased. It does mean that the talent of the engineering force that previously was devoted to engineering each application has been released for investigation of ways to improve the standard product. Mr. Kenefake's paper gives some of the early results of such an investigation and points the way to what may be in store in the future. To those of us who have been impressed with the performance of frequency-modulated radiobroadcasts in comparison with amplitude modulation, Mr. Kenefake's paper answers our question as to whether, and how, the same good results could not be secured in the carrier field. You will undoubtedly agree that the results of the experiment indicate that reduction in power requirement or increase in range of successful operation are not very remote possibilities in the carrier field.

Aircraft Electric Power-Supply System

Discussion and author's closure of paper 43-123 by J. E. Yarmack, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 655–8.

E. C. Wentz (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): To those who have been associated closely with the aircraft industry, the accomplishments set forth by Mr. Yarmack may not seem remarkable, but a comparison of Mr. Yarmack's transformer with the ordinary dry-type transformer will emphasize the magnitude of his accomplishment. For instance, we recently redesigned our standard dry-type transformers and reduced the weight of active material in the 25-kva 60-cycle design to 218 pounds, which we still think is very good by ordinary standards, but Mr. Yarmack gets 33.7 kva in 30 pounds total weight.

The interesting conclusion is that the transformer engineer must forget nearly all he knows about transformer design before he begins to design an aircraft transformer. It is still one of the almost unexplored fields of engineering. Not nearly as much has been done as will be accomplished in the future.

As an illustration, a design for the same service which we made, after Mr. Yarmack had made his, put out 36 kva and weighed only 25 pounds. Its iron loss was 230 compared with Mr. Yarmack's 320, and our full-load copper loss was 1,420 (at 75 degrees centigrade) compared with 1,230. This is not by way of disparagement of Mr. Yarmack's accomplishment; it merely illustrates the rapid progress in the industry and the opportunities still open to the designer. Mr. Yarmack can undoubtedly produce figures on a newer design, even lighter and more efficient than ours.

It is interesting to note the relatively high efficiencies of these new transformers. The losses in a conventional 25-kva 60-cycle design are 140 iron and 410 copper compared with 230 and 1,420. The increase in loss does not go in inverse proportion to the decrease in weight.

The greatest reduction in size comes from the higher frequency of 400 cycles. Hipersil, in the thickness of 0.007 inch, can be worked at nearly saturation density at 400 cycles without excessive loss. As far as the transformer industry is concerned, the introduction of Hipersil means that 60 cycles is now a most uneconomical frequency. For aircraft transformers it now appears that 600 or 800 cycles would be more economical.

These transformers are air-blast cooled and operated at about 80 degrees centigrade temperature rise, which is convenient because the rectifier needs the air blast anyway. Nearly as good results can be attained in self-cooled designs if the newer insulating materials, allowing temperatures up to 250 degrees centigrade, are used. It is doubtful that air-blast transformers will be worked normally at 250 degrees centigrade because of even lower efficiencies and because of the hazards of working copper so hard that a few seconds of interruption of the air blast will melt the windings.

Mr. Yarmack has done a notable job in organizing and successfully completing a development which presented so many new problems.

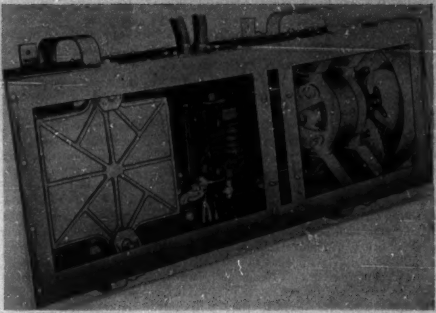


Figure 4. Fan-cooled transformer-rectifier unit for aircraft service

Rated 12 kw

H. J. Braun and C. G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Rectified alternating current, as a source of d-c power for aircraft use, has long been an intriguing possibility. Mr. Yarmack's paper summarizes some of the more recent attempts to use selenium rectifiers for this purpose and represents a worthwhile contribution to the art. It may be well to point out, however, that there are other types of rectifiers which also have possibilities for this application.

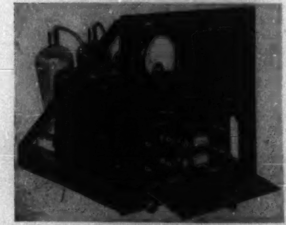


Figure 1. Aircraft transformer–rectifier unit

Built in 1930. Output 1,000 volts, 400 milliamperes

In 1930 operating experience of the United States Navy demonstrated that a-c generators were far more reliable for aircraft use than the 1,000-volt d-c generators it was then using. At their request we developed a rectified a-c system delivering 1,000-volts direct current. For this system a mercury-vapor rectifier, shown in Figure 1 of this discussion, was used.

More recently, because of the low d-c voltages required, we have devoted our attention to the use of dry-plate rectifiers. While both selenium and copper-oxide rectifiers have been considered, most of our activity has been with the copper-oxide type.

During the winter of 1941–42, a 200-ampere 30-volt rectified a-c system was developed. The rectifier built for this system used copper-oxide plates and was cooled by a blast tube. It weighed only 13 pounds. Figure 2 of this discussion shows this rectifier. The a-c power for this system was furnished by an alternator which weighed 24.6 pounds and which operated at a speed of 5,000 to 10,000 rpm. Figure 3 of the discussion shows this alternator. The weight of the total system was 37.6 pounds which compares quite favorably with the 36-pound weight of the equivalent d-c machine.

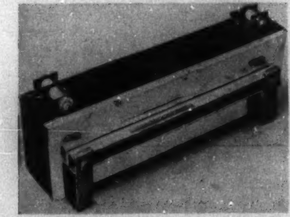


Figure 2. Aircraft rectifier

Rated at 200 amperes, 30 volts. Weight 13 pounds. Built in 1942

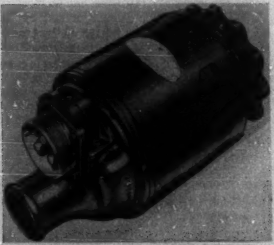


Figure 3. Engine-mounted aircraft alternator made to supply power for rectifier shown in Figure 2

Later in 1942 a 400-ampere rectified a-c system was developed. The rectifier in this system was part of a packaged unit containing a transformer and a motor-driven cooling fan. This unit was probably the first of its kind ever built for aircraft use. Figure 4 of this discussion shows this complete unit with the cover removed. The component parts are clearly visible. It has been tested to a pressure altitude of 39,000 feet.

Since both copper-oxide and selenium rectifiers offer promising possibilities for use in aircraft, some comparisons may be in order. Recently we have built rectifying elements for a 200-ampere copper-oxide rectifier. A comparison with the data in Mr. Yarmack's Figures 3, 4, and 5 shows practically identical efficiency and weight, but the copper-oxide elements occupy a space of 0.14 cubic foot compared with Mr. Yarmack's "less than 0.25 cubic foot."

One of the original reasons for giving serious consideration to the use of rectified alternating current as a possible alternate to a d-c generator was the extreme difficulties then being encountered with excessive brush wear at high altitudes. So much progress has now been made in the solution of the high-altitude brush problem that rectified alternating current is not now so likely to replace d-c generators as a main source of electric power as it might have been had progress not been made in the brush problem.

C. C. Horstman (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): Mr. Yarmack has presented a very interesting solution to the problem of obtaining large power-plant capacity in aircraft. His solution for obtaining a very light transformer has been especially interesting to us.

As stated by Mr. Yarmack, he achieved this result by using a very unique design with the latest kind of material. The advent of the new low-loss high-permeability oriented type of electrical steels has been timed most fortunately to coincide with the demands for lighter transformers. Mr. Yarmack has pointed out that in a transformer for aircraft it is most important to have minimum weight, but it is also important to have high efficiency and satisfactory service.

For some years we have had Hipersil core material in standard 29-gauge thickness for use in power transformers. This material is characterized by very low losses and high permeability in the rolling or grain direction. These results are obtained by a carefully controlled rolling and heat-treating cycle, such that the crystals of silicon steel are oriented preferably with an easy direction of magnetization in the rolling direction.

Figure 5. Hipersil type-C three-phase core

Figure 6. Comparative core loss of Hipersil conventional cores

In order to make efficient use of these good magnetic properties in the grain direction, it is necessary that the material be made into a core so that all the flux coincides with the grain direction.

This construction is illustrated in Figure 5 of this discussion. The core is made by winding two loop sections with a third section wound on the outside of the two, the whole core annealed at high temperature, impregnated, bonded solidly together, and then cut in two to form two pieces for ease of assembly. In this type of construction all the core material is active, resulting in a minimum weight for the core. Mr. Yarmack has shown the complete simple framing construction possible in his paper.

For use in this type of transformer operating at 350 cycles and upwards, Hipersil steel has been developed to have preferred orientation down to a thickness of seven mils. It is only by using a core material of this thickness with the very low hysteresis losses and high permeability of Hipersil that the unique features of the design can be achieved. Figure 6 of this discussion is a comparison of the core losses of this type of core compared with standard cores; it illustrates the large increases in working inductions that are possible with this core material.

It might be well to mention that the butt joint used in this type core is very beneficial at the high frequencies and high flux densities involved. Figure 7 of this discussion will illustrate this point showing how the butt joint of the Hipersil core eliminates crowding of flux in every other lamination of a conventional alternate butt and lap joint. At high frequencies or high densities this flux crowding will cause early saturation, high losses, and high magnetizing current.

Mr. Yarmack has very ingeniously incorporated this new thin-oriented-Hipersil three-phase core into a transformer having high efficiency and minimum weight. The reduced section of iron has enabled him to develop a very clever design for the copper coil and by careful design of all features has produced a transformer that will fit very well with the lightweight rectifier into an a-c–d-c aircraft system.

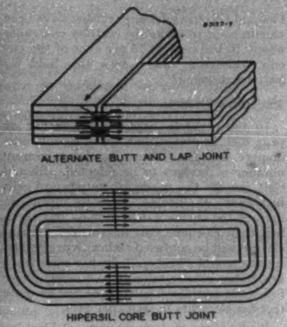


Figure 7. Comparison of Hipersil core butt joint with the conventional alternate butt and lap joint

J. E. Yarmack: The comments expressed by Mr. Horstman and Mr. Wentz are gratifying, since they amplify the transformer-core data. They are also welcome because they represent additional valuable remarks on the properties of Hipersil. This type of transformer core will play an important role in the electrical industry and, in even a greater degree, among the electrical engineers participating in the aviation industry.

Table I. Approximate Weight Comparison of Component Parts in Three Aircraft Power Schemes

3-Kw Direct Current and 1.5-Kva Alternating Current—D-C System (P-2)		6-Kw Direct Current and 1.5-Kva Alternating Current—A-C–D-C System (400 Cycles)		6-Kw Direct Current and 1.2-Kw Alternating Current —NEA-5, U. S. Navy (800 Cycles)	
Equipment	Pounds	Equipment	Pounds	Equipment	Pounds
D-c generator	35	Alternator	30	A-c–d-c generator	45
Regulator	3	Rectifier	10	A-c regulator	3
Current relay	2	A-c regulator and transformer	10	D-c regulator	3
1.5-kva inverter	35	D-c regulator	3	Current relay	2
Total	75	Total	53	Total	53

The discussion by Braun and Veinott should be taken only with full cognizance of the fact that the author's paper represents a report as of the end of 1942. A selenium rectifier of much smaller size and weight than the one illustrated in the paper was already available during the first half of this year. It did not seem desirable, however, to make emphatic engineering statements until after the completion of extensive tests and experiments.

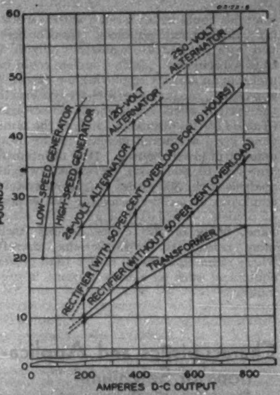


Figure 8. Relation of weights of aircraft electric-power generating equipment to the d-c output current

Advocation of the a-c–d-c system as a substitute for the straight *P*-2 system may be viewed in the light of characteristics shown in Figure 8 of this discussion. The weights shown on this plot may deviate somewhat and are being cited with due respect for the skill of individual designers and manufacturers. The steepness of the curve illustrating the weights of d-c generators indicates that the weights of d-c machines of higher than 200-ampere rating are prohibitingly greater if they are flange-mounted, that is, according to present practice. One of the possible and very feasible

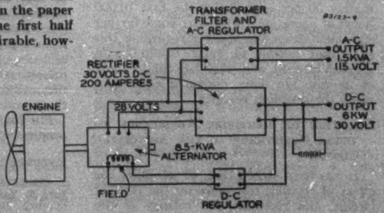

TRANSFORMER FILTER AND A-C REGULATOR
A-C OUTPUT 1.5KVA 115 VOLT
RECTIFIER 30 VOLTS D-C 200 AMPERES
D-C OUTPUT 6KW 30 VOLT
ENGINE
28 VOLTS
8.5-KVA ALTERNATOR
FIELD
D-C REGULATOR

Figure 9. Schematic diagram of aircraft electric-power plant utilizing alternator, rectifier, and also a-c and d-c regulators. The system gives dual output

solutions could be an a-c–d-c system utilizing alternators, rectifiers, transformers, and both a-c and d-c regulators (Figure 9 of this discussion). Such a system could be useful in an aircraft employing a-c-operated apparatus. As far as weights are concerned, the system offers considerable advantages (Table I of this discussion) and, at the same time, gives a marked increase in the total amount of electric power. It is of interest, however, to point out the similar weight analysis of the 800 cycles per second system employing an a-c–d-c generator that weighs 45 pounds. The latter plant, on the other hand, being of 800 cycles, adapts itself more readily to a smaller-size aircraft where radio equipment, rather than power equipment such as electric motors, is prevailing.

Characteristics and Applications of Selenium-Rectifier Cells

Discussion and author's closure of paper 43-124 by Edgar A. Harty, presented at the AIEE national technical meeting, Cleveland Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 624–9.

C. A. Kotterman (consulting engineer, New York, N. Y.): The author of this interesting paper states in his introduction that a manufacturing plant has been set up which permits producing cells in large quantities within close electrical tolerances. Yet, in his introduction to "Electrical Characteristics of the Elements," he states that " it is impossible to make all cells exactly alike as to characteristics. . . ." "A slight deviation should be expected between individual cells." It would be very helpful to users of rectifiers if they could be given something more concrete about this deviation instead of the rather indefinite statements in the paper.

Aging

The increase in forward resistance with time of operation of metal-plate rectifiers is known as aging and is common to all such types of rectifiers, although different types age at different rates.

Mr. Harty refers to this phenomenon of aging at least five times in his paper, but, unfortunately, he does not give us any quantitative information on this point. Another manufacturer of selenium rectifiers in the United States asserts that the forward resistance of his elements increases approximately 50 per cent during the first 10,000 hours of operation. I am sure some further statement from the speaker regarding this important matter of aging would be of general interest and very helpful to those of us who are daily designing equipment using the selenium rectifier.

Relation of Rectifier Resistance to Total Resistance of the Associated Circuit

It has been stated that, to minimize the effect of rectifier resistance on the rectifier circuit, the recommended ratings of the General Electric selenium rectifier have been chosen so as to make the rectifier resistance about 10 per cent to 15 per cent of the circuit resistance. How does this hold for battery charging?

Rectifier Design

In the example cited by the author on page 629, he states that, for a 30-volt one-ampere rectifier, three is the minimum number of bridges to be selected. Should that not be the minimum number of cells in series? If "bridge" is really meant, in light of the author's definition of unit rectifier, then I believe that the rectifier nomenclature will be confused rather than clarified by the use of "bridge." In my experience bridge has always referred to a single-phase full-wave rectifier consisting of one or more rectifier elements in series per arm.

Life Tests

The published life-test data look very good. However, the data are not of very much practical value to the users of rectifiers, because they do not give the ambient temperature in which the tests were run, nor the current density. Were the test stacks derated in any way? As the data represent constant load, what sort of curves would be expected for constant current? What percentage of increase in reverse resistance would be expected along with the increase in forward resistance? What sort of curves could be anticipated where the current density per unit area is above normal, say up to 2.5 times the normal current rating?

In view of the interest shown in the heavy-current metal-plate rectifier application to d-c power supplies for aircraft, would Mr. Harty care to discuss the probable trend of these life-test curves where the current density is between two and three times normal and the frequency varying between 200 and 800 cycles?

H. J. Braun and **C. G. Veinott** (Westinghouse Electric and Manufacturing Company, Lima, Ohio): E. A. Harty has presented many useful technical data on the characteristics of selenium cells. These data should prove of value to engineers in applying these cells.

Mr. Harty has used a symbolic notation for the various rectifier circuits different from the one with which we are familiar. In the interests of standardization, a common language in speaking of the various metallic-rectifier circuits would be highly desirable. We have been using a notation based upon the mnemonic word *ASP*, where *A* stands for the number of arms in the rectifier, *S* stands for the number of cells in series in each arm, and *P* stands for the number of cells in parallel in each arm. Thus a three-phase bridge circuit would be represented by the notation 6–1–1, or 6–3–5, or 6–*S*–*P*, depending, of course, upon the necessary combination of plates to handle the voltage and current requirements of the completed rectifier.

For further use of this system by other people see reference 1.

Reference

1. Selenium Rectifiers and Their Design, John E. Yarmack. AIEE Transactions, volume 61, 1942, July section, pages 488–95.

Edgar A. Harty: The author expresses thanks to H. J. Braun, C. G. Veinott, and C. A. Kotterman for their comments.

The symbolic notation used by the manufacturers of copper-oxide rectifier cells was based on the formula "arms-series-parallel" where a figure showed the number of arms in the circuit, another one the series cells, and finally a third one the cells in parallel. For example, 4–1–1 represented a single-phase full-wave bridge circuit using one cell per arm. A three-phase full-wave bridge circuit was indicated by 6–1–1. However, a two-phase center-tap circuit also used 4–1–1, and all three-phase circuits shown in Figure 19, except the half-wave one, used 6–1–1.

The symbolic notation used in this paper avoids this duplication and also attempts to offer a notation which not only describes the circuit but also shows how the cells are functioning.

In connection with the variation between cells, our one-square-inch cells are held between plus or minus one milliampere at 15 volts for leakage values, as shown in Figure 2, and plus or minus 0.05 volt at 0.5 ampere in the forward direction.

All aging curves are based on tests running at full-rated load and at ambients around 27 degrees centigrade. For the duration of these tests the forward resistance of each cell measured at 0.5 ampere changed only 18 per cent.

In applying cells to battery-charging circuits, a certain amount of ballast must be used in the form of reactance to minimize aging.

When overloading cells, for the same change in resistance, aging will be greater at the overload point. It is, therefore, very important to rerate the cells to a lower voltage to obtain a reasonable life.

Finally, an article by A. E. Richards[1] should have been included in the list of references. This article is highly recommended, although describing a different make of selenium rectifier.

Reference

1. The Characteristics and Applications of the Selenium Rectifier, A. E. Richards, *Journal of the Institution of Electrical Engineers* (London, England), volume 88, October 1941.

Fundamental Principles of Amplidyne Applications

Discussion and author's closure of paper 43-125 by F. E. Crever, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 603–06.

W. H. Formhals (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The word "Amplidyne" is the trade name given a form of the Rosenberg generator or Pestarini metadyne when operated as a regulator or control device and is so used in this paper. In view of this, it is believed that the subjects of terminal markings, polarity, and so forth, must be considered primarily from the aspect of its use as a rotating regulator.

On this basis it would appear that the tabulation of Figure 5 would be of value to a design engineer as a standard sheet for use in his own organization, but it has no place in the technical literature where it might be misunderstood as setting up standards for terminal markings for all rotating machines used as regulators and control devices.

The Rototrol which was the pioneer among rotating regulators has always followed the National Electrical Manufacturers Association and AIEE Standards for terminal markings for both clockwise and counterclockwise rotation. In contrast to these standards the armature polarity of the Amplidyne does not reverse with change of rotation as in d-c machines, and there are numerous rules to observe concerning the necessity for reversal of field leads.

Since the rotating regulator is a control element and since each is designed for an individual job, it is believed that it would be better for the users to rely entirely upon the control diagram for the connections and use of each of the many fields rather than to feel that fields with a given marking have a specific function in all cases.

We therefore believe that this paper should not be construed as setting up a set of standard markings for rotating regulators.

F. E. Crever: A chief purpose of my paper was to point out how a rotating machine is used as an amplifier in regulating systems. The paper clearly shows that the machine itself is not a complete regulator, but rather a new form of power amplifier. Hence, the name "rotating regulator" applied to the Amplidyne by Mr. Formhals seems inappropriate.

The terminal markings shown in Figure 5 of the paper apply to the use of the Amplidyne generator as it has developed up to the present time. They were included in the paper so as to give as clear as possible an understanding of the Amplidyne circuits and avoid confusion in describing their applications to a great variety of industrial and other uses.

Design Relationships for D-C Generators for Use in Aircraft

Discussion and author's closure of paper 43-126 by S. R. Bergman, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 613–16.

A. Fisher and F. M. Potter (General Electric Company, Lynn, Mass.): The design relationships for aircraft-type generators follow the fundamental principles of d-c machine design, in that the performance improves in the following order:

1. Noncommutating-pole generator.
2. Commutating-pole generator.
3. Compensating (and commutating-pole) generator.

Normally, for small conventional constant-speed generators, the costs increase in the same order with the commutating-pole type, the most economical from the standpoint of material. However, for the same performance, the compensated machine proves to be the most economical from both the weight and cost standpoint. For equal performance, it is our experience that the other types must be larger and never, within practical limitations, quite approaching the same performance.

The design described in this paper lends itself to quantity production so that the type of winding affects the over-all costs to a relatively small degree, the time, labor, and materials required for manufacture being determined mostly by necessary mechanical considerations. Although the compensating winding would seemingly increase the copper weight somewhat, it does perform a double function in that it surrounds the commutating poles, the net effect being a somewhat greater mean length of turn for the commutating-pole winding. Inasmuch as the overcompensation required by a compensated machine is considerably reduced, and also since it is possible to use small air gaps and reduced weight of shunt-field copper, the total net weight of copper and watts copper loss are actually reduced.

The many problems connected with aircraft-generator design are simplified greatly by the following machine advantages:

1. Ability to carry heavy overloads with good commutation over the entire speed range.
2. Good stability when operated with a voltage regulator with wide and sudden changes in load and speed.
3. Lower temperature rises resulting from
   - (*a*). Good distribution of windings.
   - (*b*). Lower load losses.
   - (*c*). Lower commutator and brush losses.
   - (*d*). Improved methods of ventilation.

The progress made in aircraft-generator design will be of considerable use in future industrial-machine design. We have built a number of compensated-type machines which have shown up to advantage, particularly in regard to low time constants and linear relationship of torque and current. The greatest contribution will be from the standpoint of improved insulations and lightweight design.

N. R. Schultz (General Electric Company, Schenectady, N. Y.): In general, the load-voltage drop of a self-excited shunt generator is caused by

(*a*). Armature resistance drop.

(*b*). Demagnetizing effect of armature reaction.

(*c*). Decrease in excitation because of *a* and *b*; that is, with a given value of shunt-field circuit resistance.

In the case of a compensated generator, the function of the compensating windings is to neutralize wholly or partially the effects of armature reaction. Since the demagnetizing effect of armature reaction increases directly (neglecting saturation) with armature current, better overload characteristics are obtained with the compensated generator than with the uncompensated generator.

Laboratory tests were made on compensated and uncompensated air-corps-type *P*-1 generators rated 200 amperes, 2,500/4,500 rpm to determine the relative characteristics of compensated and uncompensated machines. The following summarizes the general results obtained:

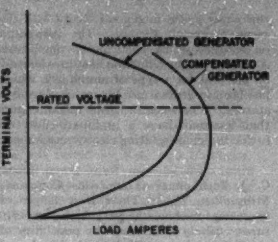


Figure 1

1. A comparison of the volt-ampere characteristics of the compensated and uncompensated generators, as obtained by test, indicates that at a given speed the compensated generator is able to produce more overload power than the uncompensated generator. This may be important during emergency conditions when one or more generators of a multiengined aircraft are out of service. Typical volt-ampere characteristics of the compensated and uncompensated generators are shown in Figure 1 of this discussion. These curves are representative of generator operation at a speed approximately midway in the rated speed range.

2. Oscillographic records of laboratory tests show that the compensated generator is capable of supplying more current at higher voltages to heavy shock loads than is the uncompensated generator under the same conditions. On actual aircraft electric systems, this characteristic of compensated generators has the following advantages:

   (*a*). Smaller system voltage dips due to sudden applications of load.

   (*b*). More rapid acceleration of motor loads.

   (*c*). Lesser dependence upon batteries for assistance in supplying heavy shock loads.

3. Commutation characteristics of the compensated and uncompensated generators were observed while the machines were operated in an altitude chamber which simulated atmospheric pressure and temperature conditions found at 35,000 feet. It was observed that sudden application of approximately full load upon the uncompensated generator resulted in severe sparking at the brushes. Moderate sparking persisted as long as full load was held on the generator. When this test was repeated at sea-level conditions, the results obtained were quite similar except that the intensity of the sparking was somewhat reduced.

The high-altitude and sea-level tests were also made upon the compensated generator. Perfect "black" commutation was observed under all conditions, no sparking being perceptible upon sudden application or removal of full load.

It may be of interest to point out that in the course of our laboratory studies of the interrupting ability of reverse current contactors and small circuit breakers, we have applied several hundred severe short circuits to several compensated-type *P*-1 generators. These short circuits have been varied over the whole range of current and voltage conditions by varying the fault resistance. The generators have withstood these faults without any perceptible difficulty or resultant damage, and we have yet failed to flash over a compensated machine.

C. B. Mirick (Naval Research Laboratory, Washington, D. C.): The writer of this discussion has had opportunity to examine and test most of the machines described in this paper and is in general accord with the views expressed therein. The paragraph on weights seems particularly significant. A sudden weight reduction of approximately 90 per cent for a type of mechanism which has been in general use for many years is a startling achievement. One might expect that it would have a profound effect on future design of rotating electric machinery.

C. J. Fechheimer (Louis Allis Company, Milwaukee, Wis.): There is a danger of operating machines at very high temperatures, entirely aside from the possibility of insulation failure. The reader is referred to my discussion of Henry M. Hobart's paper.[1]

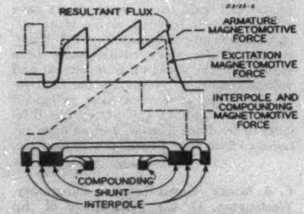
Reference

1. Critical-Material Conservation in Induction-Motor Manufacture, Henry M. Hobart. AIEE Transactions, volume 62, 1943, August section, pages 549–52.

J. D. Miner (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Doctor Bergman is to be commended for the excellent job he has done in demonstrating that armature compensation can be provided in compact high-capacity machines. It is obvious that the variable-speed weak-field requirements for aircraft generators indicate the use of armature compensation if it can possibly be provided. The writer designed compensated high-capacity generators when the first tentative specifications for 100-ampere and 200-ampere 30-volt generators were issued in 1940. It was considerably later that we built any compensated generators, because the calculations were unfavorable from weight considerations, since two factors in these early designs were insufficiently appreciated:

1. The amount of benefit which could be obtained from pole-face windings concentrated in a single coil per pole.
2. The amount of compounding effect which could be obtained by overcompensation.

Generators without commutating windings usually require advancing of the brushes so that commutation takes place under the main field. Advancement of the brushes results in a demagnetizing component of the armature magnetomotive force, in other words, a differential compounding effect. In addition, the resultant field in which the coils are commutated is usually insufficient to prevent circulating currents which also cause a differential compounding effect. It is common practice to attempt to balance demagnetizing effects by a weak cumulative series field, but the amount of series field that can be used is limited by the requirements for stability at high speeds. Moreover, in case of accidental closure of the reverse-current relay with the generator at standstill, the reverse current through a strong series field might well reverse the polarization of the machine.



D3126-2
RESULTANT FLUX
ARMATURE MAGNETOMOTIVE FORCE
EXCITATION MAGNETOMOTIVE FORCE
INTERPOLE AND COMPOUNDING MAGNETOMOTIVE FORCE
COMPOUNDING
SHUNT
INTERPOLE

Figure 2. Effect of compensation with one coil (two coil sides) per pole

Generators with commutating poles may be designed so that an excess of commutating flux is obtained at normal load, and it will then be found that circulating currents will be produced which have the effect of a cumulative series field. Usually a satisfactory amount of compounding can be obtained without much difficulty with commutation, and it is well recognized that overcompensation gives good commutation at excessive loads by reducing the effect of saturation of the commutating poles. The compounding effect must be limited to a small amount, or instability will result at high speeds.

It has been found that an uncompensated machine requiring about six pounds of copper for excitation would require only $2^1/_2$ pounds of field copper if compensated, because of the saving in ampere turns required to overcome distortion and demagnetizing effects. However, such a machine will require poles which are about one pound heavier, interpoles and interpole windings weighing a total of about two pounds, pole-face windings weighing about one pound, and additional connections probably weighing one-fourth pound so that the net result is an increase in weight of nearly one pound. This additional pound must be balanced against the improvement in performance and overload capacity. If no improvement in output is desired, the armature can be reduced in size because of the lower losses, and a saving in weight can be realized in machines of high rating.

Figure 3. Brazing commutators

It must not be assumed that all aircraft generators should be compensated because, in the smaller sizes, the weight of a compensated machine will be greater than that of a satisfactory uncompensated machine. Military loads are of brief duration, and the average load with the turrets and similar equipment out of action may be but 15 or 20 per cent of the load when the aircraft is in battle. Very satisfactory performance has been obtained without compensating windings in generators as large as 200 amperes at 28.5 volts. Service records are available showing less than $^1/_{16}$-inch brush wear in well over 1,000 hours of flying with loads encountered under training conditions.

Mechanical considerations are important as well as is electrical design. Much of the success of any generator lies in the construction and design of the brush rigging and commutator. This is particularly true at the higher speeds, and the so-called high-speed (4,000/10,000-rpm) generators cannot possibly give satisfactory commutation and brush life unless the commutator remains mechanically stable at all operating speeds and temperatures. Rigid control of dimensions, materials, pressures, and processing are essential, as is, of course, an adequate mechanical design with which to start.

Figure 4. Fatigue-test setup

Doctor Bergman recommends brush temperatures below those corresponding to a 100-degree centigrade rise, and brush densities below 120 amperes per square inch. We believe that these values must be exceeded to obtain acceptable weight. Ventilation conditions are rarely as good as indicated by the specifications, and conditions have been encountered where the total head from the blast-tube cooling system was only one fifth the specification figure. Under these conditions we have obtained reasonably good brush life at 33,000 feet with brush temperatures in excess of 200 degrees centigrade. At these temperatures it is necessary to braze the commutator leads to the bars. A process was developed for doing this without damage to the hard copper bars when we started quantity production of high-capacity generators.

It is also believed that the quill-shaft and resilient-spindle construction is unnecessarily complicated. Two simpler types of resilient couplings have been developed. One of these uses bonded synthetic rubber. Although it will withstand operation for 30 minutes under this extreme condition, this coupling is not employed when operation at its critical speed is essential. In all practical cases it has been possible to cover the operation range without encountering critical speeds. A nonlinear spring coupling is also available; this does not have any critical speed but is somewhat heavier than the bonded synthetic coupling.

We also have developed apparatus for

fatigue testing of generator flange and flange screws. The development of a failureproof flange was relatively simple, but the elimination of flange screw failures was more difficult. The solution was found by means of the laboratory fatigue tests to lie in control of the yielding of the flange and not in the screws themselves. An endurance limit of not less than 10,000,000 cycles at 60g (where g is the acceleration due to gravity) has been found sufficient for mounting on any engine yet encountered.

Doctor Bergman failed to mention one feature of the compensated generator which is of considerable importance. This is that a generator of the compensated type is a very satisfactory motor and may be used for starting purposes when the generator is mounted on an auxiliary engine for ground power. A generator with brush shift or with a series field is not nearly so satisfactory when operated as a motor, since the brush shift is in the wrong direction, and the series field is differential.

S. R. Bergman: J. D. Miner points out that the compensated generator also may serve as a very good motor. That seemed so self-evident that it did not occur to me to discuss it. We are building compensated motors quite successfully for aircraft in sizes from one horsepower up to and including 15 horsepower.

One very important consideration lies in the fact that in compensated motors the starting current may be reduced considerably. The explanation is as follows:

Because of the neutralization of the armature reaction, we are able to design compensated motors with less number of poles than in the uncompensated type. The frequency and, therefore, the core loss are reduced. In addition, since there is no field distortion, the core loss is further reduced and runs usually less than one half of the core loss in uncompensated machines. Furthermore, because of the smaller number of poles, the excitation losses are reduced. Finally, the extra commutation losses are eliminated. Because of these reduced losses we can allow a much higher resistance in the armature circuit (armature in series with the compensated winding and the commutating poles) with a corresponding reduction in the starting current. In the smaller sizes we have actually cut the starting current in two, and in the larger sizes the starting current has been reduced from 30 to 40 per cent. This reduction in the starting current enables us in a great many cases to eliminate automatic starting devices, and we can start "directly on the line" without detrimental effect to the stability of the system.

Mr. Miner also brings up the question of brazing the armature leads to the commutator. This process is very attractive and has been used by us only in sizes over 300 amperes. We are, however, now tooling up to include both the 300- and 200-ampere generators.

Mr. Miner expresses the opinion that in smaller generators (200 amperes and below) compensation is unnecessary. We are building machines with compensation down to 50 amperes for generators and one horsepower for motors in quite acceptable weights.

Operation of Nonsalient-Pole-Type Generators Supplying a Rectifier Load

Discussion and author's closure of paper 43-114 by M. D. Ross and J. W. Batchelor, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 667–70.

R. D. Evans (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The heating effects in a generator carrying rectifier load have for some time been the subject of considerable speculation. Progress in solving this problem has come in two steps involving:

1. Determination of harmonics in the a-c system of rectifiers including generators.
2. Determination of machine heating that results from specific harmonic loading.

Harmonics in the a-c circuits of plain rectifiers were analyzed by Brown and Smith (reference 5 of the paper). The corresponding problems for rectifiers with phase control and for inverters were analyzed by Evans and Muller (reference 7 of the paper).

The increased use of rectifiers and the pessimistic report in European technical papers by Robert Pohl prompted the investigation reported in the paper by Ross and Batchelor. They have presented the results of harmonic tests on a typical generator supplying a rectifier under various conditions of phase control varying from zero degrees to 30 degrees delay. They have shown that the principal effect of the harmonics is an increase of the rotor temperature. They have described a method for calculating temperature rise, which is based on specific harmonic currents, the negative-sequence resistance of the machine, and the machine constants usually available for temperature-rise calculations.

The conclusions of this investigation are reassuring from the standpoint of heating in generators for the usual applications. Generators supplying rectifier load will have negligible additional temperature rise from harmonics if the rectifier load is only a small part of the total or if the rectifier is of 12 or more phases. The particular cases that deserve special study are those in which the generators are fully loaded with six-phase rectifiers or where the rectifiers are operated with phase control.

It may be of interest to describe an alternative method for determining harmonic loss in the rotor and estimating the additional temperature rise in the rotor that results from harmonic currents produced in an external source, such as a rectifier. In this method the harmonic-power inputs to the machine are measured and then reduced by the estimated harmonic losses in the stator. The resulting harmonic-power input to the rotor is then divided into components that produce rotor heating and shaft torque. This division is readily made by considering the machine operating as an induction motor at appropriate slips for the various harmonic frequencies. In this calculation the harmonics should be considered of different phase sequence, as mentioned by the authors, and should make use of equivalent circuits. The harmonic losses in the rotor are then used to calculate additional temperature rise in the usual manner, as illustrated by the authors. For the case of the generator supplying the six-phase rectifier, which was tested by Ross and Batchelor, harmonic-power-input measurements were made for the various amounts of phase control. The corresponding additional rotor temperature rises resulting from rectifier harmonics were estimated by the alternative method to be 17.2 degrees, 18.4 degrees, 20.9 degrees, and 27.2 degrees which may be compared with the test values of 19.1 degrees, 22.0 degrees, 22.1 degrees, and 25.5 degrees, listed in Table III of the paper, for the angles of phase delay from zero degrees to 30.4 degrees. It may be observed that this alternative method requires measurements of the harmonic-power input to the machine under load conditions or a corresponding estimate, whereas the method described by the author uses negative-sequence resistance which is generally available for the particular machine or from tests on similar units.

C. C. Herskind (General Electric Company, Schenectady, N. Y.): This paper is a valuable contribution in that it presents some data on generator heating caused by rectifiers. Past work and discussion on this problem have been handicapped by the lack of such data.

Since the penalty of generator overheating from this source is severe, engineers have been conservative. The authors' conclusions, that with a 12-phase rectifier additional heating will be slight and with more than 12 phases additional heating will be negligible, indicate that previous estimates of this effect may have been pessimistic. However, it should be recognized that each machine must be considered as a case by itself in determining the probable heating.

One installation has been made where a 200-kw rectifier is fed from a 350-kva generator. This rectifier transformer has a Scott-*T* primary and quarter-phase secondary connection. A-c filters were provided for harmonics 3, 5, and 7, and no trouble has been experienced caused by generator overheating.

When a 6- or 12-phase rectifier is supplied from a generator of approximately equal rating, the magnitude of the harmonics will generally be lower than in the case where the rectifier is supplied from a power system. This is due to the fact that the generator reactance is usually of the order of 10 per cent (on generator rating) while the power-system reactance may be one half or one quarter of this value. Since the magnitude of the harmonics is reduced as the total reactance of rectifier and a-c system is increased, the a-c harmonics will be lower when the rectifier is supplied from a generator.

The increase in total reactance, incurred when the rectifier is supplied from a generator of approximately equal rating, affects the rectifier characteristics. The high regulation drop and low power factor obtained on a six-phase rectifier under these conditions may mitigate against such applications and lead to a choice of a rectifier having 12 or more phases, thereby greatly reducing the probability of generator overheating.

M. D. Ross: Mr. Herskind points out that the harmonics are greater in magnitude when the rectifier is connected to a large system than when they operate on a single generator. The tests covered by this paper are, therefore, not representative as to the magnitude of the harmonics, but the increased harmonic values on a large system usually cause no concern, as they are spread over a number of generators and the increased heating of any one generator is small.

Mr. Evans' approach to this problem is very interesting and shows what can be done by careful measurement of the harmonics and their respective power factors. However, as he has pointed out, we have considered the problem from the standpoint of the machine designer who must predetermine the performance of a wide range of machines without benefit of test data.

After the paper was written some further study was made to determine the point of maximum loss on the rotor surface for each harmonic. The points of maximum loss were found to lie in a band covering 65 degrees to 75 degrees ahead of the pole center. There is no obvious explanation as to why these maximum points should lie so close together.

The Testing of Mercury-Arc Rectifiers

Discussion of paper 43-113 by H. L. Kellogg and C. C. Herskind, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, December section, pages 765–73.

J. H. Cox (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): I am going to point out a few differences between the practices of my associates and myself, and those described by Kellogg and Herskind. In so doing, I believe that I shall emphasize the value of the paper. Because of the number of still unknowns in rectifier design, it is highly desirable to publish testing procedures so that they may be compared, and through that process ultimately to establish correct methods and to enable the writing of Standards.

In the determination of the quality of a rectifier, the usual limitation is arc-back, and arc-backs are random phenomena. This means that the determination becomes the collection of statistics. The collection of statistics is usually a long and tedious process, and some from of accelerated test is attractive. We are experimenting with forms of accelerated tests, and I am most happy to know that others are doing likewise. However, I question whether the factors are well enough known at present so that any interpretation of the result of the accelerated test described by the authors could be considered very reliable. It may be noted that the rating applied to a particular design by Kellogg and Herskind is roughly onehalf of the continuous load limit termined by theed accelerated test, and that such a test required two or three days. It has been my experience that a test that uses the normal duty cycle, includes the overloads specified for heavy-duty railway service, and extends for the order of three days, will provide a quite reliable basis for rating determination, and a basis which I think is as reliable as any accelerated test yet established. This would correspond most closely with the duty-cycle test described in the paper. The large amount of experience gained during the past three years indicates that the base load of the railway duty cycle is a satisfactory value for the continuous rating for electrochemical service.

Referring to the specific test described, I believe that a percentage of expected rating would be preferable to 100 or 200 amperes per step in order to make the test more consistent for various sizes of rectifiers.

I question the value of the current-limit test. It is somewhat complicated, and I do not see that it indicates anything not shown by the simpler short-circuit and simulated arc-back tests which are closer to service conditions. Simulated arc-back tests can be made to duplicate actual arc-backs more closely by the use of an ignitron tube connected backwards in place of the short-circuiting switch and series resistance, since a short-circuiting ignitron interposes an arc drop in the short circuit. The ignitron-tube method also provides a simple means of placing the arc-back at any desired point in the cycle.

I do not agree with the sampling method of testing in which all loading tests are omitted on all but a small percentage of a number of rectifiers of the same design. No physical or chemical tests have been established that will predict when an individual rectifier will arc back. I have found that a short duty-cycle type of commercial test applied to every unit is invaluable in maintaining quality of both workmanship and material in the factory.

I was happy to note that the authors favor the oscillograph method of measuring arc drop, as we have found this to be the only really satisfactory method. However, rather than the pool-type rectifier and battery, we favor the use of dry-plate rectifiers in both the oscillograph and by-pass circuits as described by W. B. Batten.[1] By the use of a not too sensitive oscillograph element a value of resistance can be used in series with the oscillograph which will pass about 0.1 ampere at arc-drop voltage and thus avoid the high forward-resistance of the Rectox rectifiers at extremely low currents. With this value of series resistance the reverse voltage of a 600-volt rectifier will pass about five amperes which requires a larger Rectox in the by-pass circuit. However, such a Rectox is not objectionably large for short-time duty.

The authors mention the building of rectifiers for an extremely low arc-back rate for critical applications with the necessary sacrifice in efficiency caused by higher arc drop. I question if this will be necessary or desirable, since with modern anode breakers an arc-back can be cleared by opening only the pole connected to the anode which arcs back, and this pole can be reclosed with no loss of service. I want to emphasize the authors' statement that rectifiers in general do not arc back with increasing frequency with time in service but rather settle down to a uniform rate. The recent extensive experience establishes this characteristic, at least with the ignitron type of rectifiers in service up to six years.

REFERENCE

1. OXIDE DISKS AID IN OBTAINING ARC-DROP WAVE, W. B. Batten. *Electric Journal*, April 1930, page 240.

Theory of Rectifier—D-C Motor Drive

Discussion and author's closure of paper 43-90 by E. H. Vedder and K. P. Puchlowski, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, pages 863–70.

D. E. Marshall (Westinghouse Electric and Manufacturing Company, Bloomfield, N. J.): The equations derived in this paper point the way to the use of a very convenient graphical analysis by which the wave form of the current can be obtained without the use of tedious calculation.

Equation 12 of the authors' paper can be written as

$$\frac{iZ}{E_m}=\left[\sin\,(x_f-\theta+\omega t)+C\epsilon^{-\frac{\omega t}{\tan\theta}}-\frac{a}{\cos\theta}\right]$$

where the symbols have the same meaning as given in the paper.

The sine term can be laid out lagging the voltage wave by the usual power-factor angle θ. The term $a/\cos\theta$ is laid out to the same scale as the sine term, and, since it is a constant, it can be added to the sine term as a shift in the zero line.

The exponential term can be laid out on a template to any convenient scale, the important thing being that it have a time constant equal to tan θ. Another convenient method is to draw the curves on tracing paper and superimpose through the transparency.

It should be noticed that it is not necessary to calculate the constant C, as the effective value of this constant can be changed by changing the origin of the time scale. This is due to the fact that

$$C\epsilon^{-\frac{\omega(t+t')}{\tan\theta}}=C\epsilon^{-\frac{\omega t}{\tan\theta}}\epsilon^{-\frac{\omega t'}{\tan\theta}}=C'\epsilon^{-\frac{\omega t'}{\tan\theta}}$$

A little thought will show that when the sine and constant term are added, their sum will not, in general, be zero at the time desired, that is, when the conduction starts. If the exponential template is based on the displaced zero line of the sum of the sine and constant term in such a manner that the curves intersect at the starting angle, they will again intersect at the stopping angle. The vertical distance between the two curves at each point from starting to stopping angle will generate a curve proportional to the current as a function of time.

Two examples are shown—Figure 1 for rectification and Figure 2 for regeneration (both of this discussion). Each curve is marked to indicate which portion of the

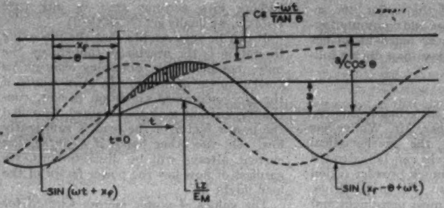


Figure 1. Graphical calculation — rectification

a = 0.6
cos θ = 0.4
θ = 66.4 degrees
x_f = 75 degrees

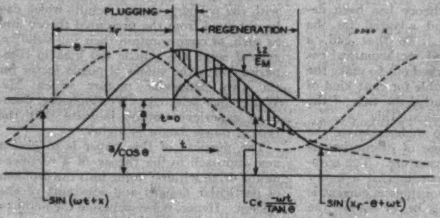


Figure 2. Graphical calculation — regeneration

a = −0.6
cos θ = 0.4
θ = 66.4 degrees
x_f = 142.5 degrees

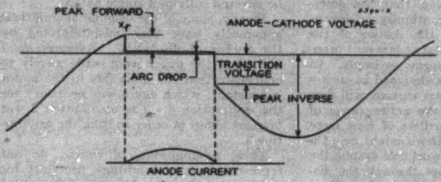


Figure 3. Tube loading—rectifier operation

a = −0.6
cos θ = 0.4
x_f = 75 degrees

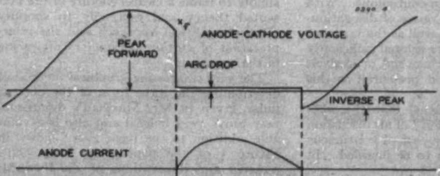


Figure 4. Tube loading — inverter operation

a = −0.6
cos θ = 0.4
x_f = 142.5 degrees

equation it represents. Note that in rectification the value of a is positive and in inversion it is negative, making the displaced zero line positive in one case and negative in the other case and thus inverting the exponential term in respect to the sine term.

Figure 3 and Figure 4 of this discussion show the duty on the main power tubes as anode current and anode–cathode voltage. Note that rectifier operation requires ability to withstand inverse voltage, and that inverter operation requires ability to withstand forward voltage.

The point of end of conduction in rectification stresses the tubes with suddenly applied voltage. The duty here depends on the magnitude of the suddenly applied transition voltage and the previous rate of decrease of anode current.

The end of conduction in inversion service should find the anode negative, as shown, with sufficient time elapsing to allow for sufficient deionization of the tube to enable it to regain control against the following forward voltage. This is controlled by the angle of firing the thyratrons. Earlier angles are more favorable to deionization but conversely result in a larger percentage of plugging versus regeneration.

It should also be noticed that the ratio of peak to average current is high and during starting periods may approach the peak rating of hot cathode tubes.

If inertia loads are accelerated, attention must be paid to the maximum averaging time rating of these tubes so that their heat capacity is not exceeded.

G. E. Walter (General Electric Company, Schenectady, N. Y.): One immediate reaction to this useful paper is that its title is rather inappropriate. The data, curves, and basic principles that it presents are not necessarily restricted to motor applications, but may be applied with equal facility to rectifier loads containing such sources of counter electromotive force as batteries or large capacitors. In this sense, the title is too narrow. It is too broad, however, in presuming to discuss the entire theory of rectifier–d-c motor drives from the initial assumption of discontinuous current flow.

Operation of a motor with continuous current delivered from a rectifier is frequently more normal and more important than running it with discontinuous current. Despite the implication of this paper that operation beyond the authors' calculations is not feasible, the point in an actual drive at which individual current pulses merge to produce continuous current is all but indistinguishable. Both the rectifier and motor make the transition smoothly. The important effect of having continuous current in the motor armature is that the familiar d-c motor equation can be used once more, since

$$V_t=(E_g+E_t)+I_a r_a \tag{1}$$

where V_t is the average voltage output of the rectifier unit, and I_a is the average value of armature current as indicated by a D'arsonval-type ammeter.

It is relatively simple to extend the material in this paper into the region of continuous current by using the expression for the average voltage output of the rectifier as

$$V_t=\sqrt{2}E_{rms}\frac{P}{\pi}\sin\frac{\pi}{P}\cos\delta \tag{2}$$

where

E_{rms} = voltage to neutral of the anode transformer in the rectifier unit
P = number of rectifier phases
δ = angle by which the firing of the rectifier tubes is delayed beyond the point at which firing would occur if there were no grid control

This angle of retard, δ, is not the firing angle, X_f, mentioned in the paper, except in the case of the biphase single-way (or half-wave) rectifier, identified by the authors as a single-phase full-wave rectifier.

If this relation (equation 2) for V_t is substituted in the motor equation 1, and both sides are divided by $\sqrt{2}E_{rms}$, the result may be expressed in the terminology of the paper as

$$\frac{P}{\pi}\sin\frac{\pi}{P}\cos\delta=\alpha+(\text{average torque factor}) \tag{3}$$

In the biphase single-way rectifier, on which the majority of their data are based, this equation may be reduced further to

$$\text{Average torque factor}=0.637\cos X_f-\alpha \tag{4}$$

when it is remembered that armature current must be continuous if this relation is to be valid.

Equation 4 shows directly the average output torque of the motor for all values of the speed factor, α, from zero to 0.637, which is the highest speed factor at which the motor will operate with continuous current. If α should exceed 0.637, as it can if the motor load is light, armature current will become discontinuous, and the average motor torque will remain positive.

The paper unfortunately minimizes the

importance of operation with continuous current by basing its characteristic curves on values of armature-circuit power-factor angle, Θ, that are not altogether typical. Measurements on a General Electric 5B204A56 1,150-rpm 230-volt one-horsepower shunt motor have indicated that the armature and commutating field in series have a resistance in the order of 6.6 ohms and a minimum inductance of about 0.05 henry. The power factor, cos Θ, is thus 0.33, definitely less than the 0.4 and 0.6 the authors have used. With the shunt field weakened for higher speed operation, armature-circuit inductance rose to about 0.07 henry, further reducing the power factor.

On another model, a General Electric 5B254A23 1,150-rpm 230-volt three-horsepower motor, armature circuit resistance is 1.85 ohms, and inductance is 0.019 henry, yielding a power factor of 0.251. If, as is possible, an external reactor is connected in series with the motor armature to improve commutation or to produce a more uniform torque, the power factor is extremely low. A reactor available for the one-horsepower motor already mentioned has an inductance varying from 0.2 to 0.6 henry and drops the power factor to the neighborhood of 0.01.

This means that discontinuous conduction will exist in a more narrow operating region than the typical curves in the paper would indicate. Specifically, Figures 5, 6, 7a, 7b, 10, and 11b of the paper are affected. Figure 4 of the paper can be used, with the proper power factor, to locate the boundary between continuous and discontinuous conduction.

One feature of Figure 4, however, was not mentioned by the authors. Although the curves from which X_s can be found as a function of X_f have no significance once they reach the line BD (or $B'D'$ or $B''D''$) that corresponds to the type of rectifier used, the relation between the two angles can still be found by following the appropriate line BD (or $B'D'$ or $B''D''$) downward as an extension of the particular curve that was used in arriving at the line. This follows from the obvious relation for continuous conduction that

$$X_s = X_f + \frac{2\pi}{P} \tag{5}$$

Finally, the original assumptions, some of which the authors admit are inexact, can be examined to locate the approximations. Shunt machines are equipped normally with a series winding of some sort, which a casual observer of the assumptions might believe would constitute a disturbing influence on the operation of the drive. Actually, the action of a series field remains substantially the same whether the motor is operated from a rectifying or a rotating source of direct current. The field, moreover, need not be saturated for useful operation. Motor speed will be increased and shaft torque decreased if the field is weakened.

There have been indications that armature reaction is not negligible in motors operated from rectifiers because of the relatively high peak currents produced, but its effect is ordinarily undisturbing. Armature inductance, however, is not constant. It depends on the instantaneous values of armature and field currents; and for field-weakened operation of the motor, the inductance of the armature and commutating-field windings may be as much as 30 per cent or 40 per cent higher than during full-field operation.

K. P. Puchlowski: In his remark concerning the title of the paper, Mr. Walter has stated correctly that the mathematical analysis presented therein can be extended directly to apply to a much broader region of electrical phenomena. The motor drive, however, is one of the most prominent and immediately useful applications of the presented theory. The theory has been developed specifically and adapted for the rectifier–motor system. To that end, a number of concepts and mathematical coefficients have been worked out to make the analysis directly adaptable for that kind of system, and, consequently, the title of the paper was chosen to indicate this.

Mr. Walter seems to underestimate the importance of the range of discontinuous current in the motor armature. The question of whether the operation of a motor with continuous or discontinuous current is more advantageous may be, of course, the subject of a separate discussion. It should be remembered, however, that the determining factor for the continuity or discontinuity of current is the design of the system and not its normal or inherent properties, as Mr. Walter seems to imply. In fact, any system can be designed in such a way that a discontinuous current will flow in the armature over the entire range of loads and speeds, regardless of how large the inductance of the circuit might be. On the other hand, in any workable system the current will usually pass through the discontinuous stage at low loads even if at higher loads it becomes continuous. With this importance of the range of discontinuous current in mind, as well as the fact that this range had not been explored sufficiently mathematically, a new method of analysis has been developed and presented in this paper.

Mr. Walter's statement, that the well-known d-c motor equation 1 of his discussion can be used only in the case of continuous current, does not seem to be justified. In fact, the equation

$$V_t = (E_g + E_t) + I_a r_a$$

is valid also in the case of a discontinuous current with a sole correction that V_t is not the average rectifier output voltage but the voltage across the armature of the motor. It should be emphasized that only for continuous current the rectifier output voltage is equal to the armature voltage of the motor, whereas in the region of discontinuous current these two quantities are entirely different ones. Thus, if the definition of the symbol V_t is changed to represent the armature voltage, the validity of equation 1 is extended over the entire range of current, both continuous and discontinuous.

The general equation 2, presented by Mr. Walter, is valid only in conjunction with certain important limitations which, unfortunately, have not been clarified. To be valid from the electrical point of view the equation 2 must fulfill the following condition:

$V_t > 0$ which means $\cos\delta > 0$

Taking into account that in any case $0 < \delta < \pi$, we finally get $0 < \delta < (\pi/2)$.

It can be seen readily that the preceding limitation is the necessary, though not the only, condition for obtaining continuous current in the armature. In other words, if the aforementioned condition is met, continuous current may be obtained, provided the power factor of the load is sufficiently low. On the other hand, if $\delta > (\pi/2)$, the current always will be discontinuous, regardless of how low the power factor of the load might be.

The values of power factors selected for some curves in the paper have been chosen with the primary view of results which can be obtained from the presented theory and as such, of course, retain their full validity, even if some are a little too high in comparison with the values of power factor for the two particular motors which Mr. Walter has selected. Westinghouse Company experience would indicate that the typical power factor is higher.

The purpose of this paper is to give a new approach to the theory of a rectifier–motor drive without going into the problem of particular designs and constants of the system. Hence, it seems beside the point to discuss details which, in any case, cannot be covered fully in a single paper.

Finally, some of the original assumptions apparently have been misinterpreted by the author of discussion. Assumptions 1 and 10 of the paper were made merely to emphasize that the main flux in the motor is constant, which again was necessary for the assumption 6. The assumption 2 was brought about in order to back the assumption 3.

These assumptions, of course, do not represent a set of conditions necessary for proper operation, but they were assembled simply to make a clearer picture of the presented theory and especially to simplify the mathematical approach and the interpretation of certain parameters appearing in the equations.

The very interesting graphical method of determining the wave form of the current pulse, given in Mr. Marshall's discussion, may prove very helpful and also is a valuable addition to the basic chart shown in Figure 4 of the paper. Mr. Marshall's remarks and illustrations of the forward-peak and inverse-peak voltages for both rectifying and inverting operations of the tubes, though not directly connected with the paper, represent an information valuable to anyone concerned with the operation of thyratron tubes.

Thyratron Motor Control

Discussion of paper 43-116 by E. E. Moyer and H. L. Palmer, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 706–12.

E. H. Vedder (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The development described in this paper is an important one, and the presentation is timely. The electronic circuits are described thoroughly, and reasons are given for the various functions and features.

Greater use of supporting technical data could have made the paper more valuable to the profession. For instance, the curves in Figure 5A of the paper are limited apparently to the armature control range with no information on the regulation with field weakening. Some of the type of data I have in mind were included by one of the authors of this paper and printed in a trade magazine. Also, the type of information used by Mr. Moyer in his presentation would have been useful in the formal paper.

The description in the paper leaves the impression that the complete circuits shown are used for all applications. A short analysis of applications would have clarified this point. Many applications do not need extremely precise control, and such features as regulated standard voltages and armature-voltage limit, when decelerating, may not be required. For instance, it would seem unnecessary to limit armature decelerating voltage on a 230-volt motor, unless the field-weakening range is more than 2:1. The use of regulated standard voltages is not necessary for most applications and is required only when very precise speed regulation is desired.

Many users are quite satisfied with the characteristics of various adjustable speed drives using rotating machines but will change to the electronic drive if it is not too complicated and has advantages of limiting noise, vibration, and mounting problems. A large portion of jobs will be solved with only a portion of the complication of the complete circuit shown. It is my hope that such a basically good scheme will be kept as simple as possible, so that it will be accepted rapidly.

K. P. Puchlowski (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The presentation of the paper was very interesting. Unfortunately, none of the valuable information presented has been included in the paper, which gives only a description of a particular electronic circuit providing adequate control of a d-c motor driven from a controllable rectifier. The paper would have been improved, however, by the inclusion of some information regarding special phenomena and operating conditions which characterize a rectifier-motor system.

Considerable emphasis is put on the fact that a constant reference voltage, independent of line-voltage variations, is obtained and is used for regulating purposes. Since no further explanation of what these regulating purposes actually are is forthcoming, it may seem that the reason for a complicated network constituting a feedback from the armature voltage and consisting of electronic tubes *D* and *C* is solely for the purpose of maintaining the armature voltage independent of line-voltage variations. Consequently, many may ask whether, in the case where changes in speed caused by normal line-voltage variations are acceptable (and this is true for most industrial applications), the feed-back circuit cannot be eliminated, and a direct control of grid-voltage phase shift is used instead, resulting in a basic simplification of the circuit.

The answer is that the feedback from armature voltage is needed for something else, namely, to prevent the voltage across the brushes from dropping rapidly with increasing torque applied to the shaft of the motor, a phenomenon which is unknown in conventional d-c motor practice.

With reference to Figure 5A of the paper, it is stated that the dotted curves represent the normal uncompensated motor-regulation characteristics. It is not known, however, whether these characteristics refer to a conventional d-c drive, to an uncompensated rectifier drive with regulated armature voltage, or to an uncompensated and unregulated rectifier drive. In that way, from the engineering point of view, the situation is not clear.

The regulation curves shown in Figure 5A terminate abruptly at about 145 per cent load, and the vertical line which follows thereafter seems to indicate the current or torque limit. This seems to be in line with the statement that the speed will be maintained up to the point where the current reaches the current-limit setting. However, the form of these regulation curves, although very desirable, would seem rather unusual, on account of that sharp break, as the 145 per cent value of load is reached. The speed would probably decrease gradually as the current limiting tube *E* comes into action.

The necessity of a feedback from field voltage to make it independent of line-voltage variations can be questioned also. Even if the field voltage is regulated and maintained constant for a given setting of the speed-control potentiometer, the field current will vary with temperature of the field winding, and, in addition to that, the flux will vary with load on account of armature reaction. In that way, it is apparent that the field voltage is not an adequate indication of the magnetic flux, and, it would seem, therefore, that a much simpler scheme of field control by direct phase displacement of the grid voltage of field thyratron tubes would be justified fully.

Load Pickup by a Group of Ignitron Rectifiers

Discussion and author's closure of paper 43-115 by L. W. Morton and D. I. Bohn, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 679–84.

J. H. Cox (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Morton and Bohn have reported the results of both an interesting and a valuable series of tests that disclosed the basic starting up and dropping characteristics of aluminum pot lines with mercury-arc rectifiers. With this information, schemes can be applied with much greater assurance than has been possible in the past. They have also described a perfect method of master starting which is not prohibitively complicated. The method does add some complication and additional apparatus over the method of master excitation, and any impression that all systems of master excitation have proved unsatisfactory is not justified.

The tests showed that, with most systems of master excitation applied with contactors having entirely normal and practical adjustments, no breaker or rectifier was subjected to more than a few per cent over its continuous load rating because of the existing pot-line inductance. Therefore, if the rectifier breakers are free of rate-of-rise influence in the forward direction, all rectifiers should stay on in this method of pot-line starting. Even the tripping of one or two rectifiers in starting should not be considered too serious, since ignitron rectifiers have ample short-time overload capacity to enable the pot line to stick, and the rectifiers that tripped can be returned to the bus in a leisurely manner.

Also, I believe that in order to provide reliable excitation under other abnormal conditions which are encountered, an excitation system must have sufficient output energy to provide reasonably satisfactory excitation with cold ignitors. In view of these factors there seems some question whether the advantages justify even the relatively nominal addition of complication required by the master blocking system over the master excitation system.

Where phase-shifting networks are used instead of mechanical phase shifters, the transients are so long that a simple procedure of simultaneously energizing the circuits is not permissible. In this case the master excitation system must take some form of releasing excitation, such as removing short circuits around the ignitors.

C. C. Herskind (General Electric Company, Schenectady, N. Y.): The starting method described in this paper is based on the ability to block the firing of an ignitron rectifier by the application of a negative voltage to the grid.

The primary function of the grid (or anode baffle, as it is frequently called) in the ignitron rectifier is to deionize the space around the anode. The grid is designed to do this with a minimum increase in arc loss. Structurally, it consists of a graphite basket surrounding the anode and having a wall of approximately one-inch thickness with three-fourths-inch or one-inch holes. These large holes and the arrangement in the tank limit the ability of the grid to control the firing of the anodes.

The blocking characteristics of the grid in a commercial ignitron rectifier are shown in Figure 1 of this discussion. These characteristics were determined by applying a positive voltage to the anode and a negative voltage to the grid with the ignitor firing. With sufficient negative voltage on the grid,

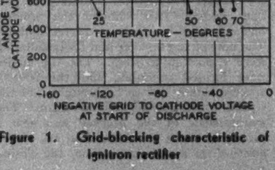


Figure 1. Grid-blocking characteristic of ignitron rectifier

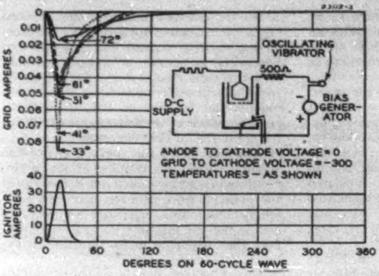


Figure 2. Grid-current wave shape during blocking test

the anode is prevented from firing. By gradually decreasing the negative voltage on the grid, a point is reached at which the blocking action fails, and the anode fires.

These curves show that a greater negative voltage is required on the grid at the lower temperatures in order to block. This is due to the larger amount of ionization reaching the anode at the lower temperatures. The grid-current wave shape under blocking conditions is shown in Figure 2 of this discussion. It may be noted that more grid current is obtained at the lower temperatures.

The slow deionization of the arc space in an ignitron rectifier is also shown by these grid-current wave shapes. At 72 degrees centigrade the grid current persists for over 120 degrees after the ignitor-current wave. At the lower temperatures the decay of ionization takes place more rapidly.

Such a slow deionization of the arc space renders it incapable of controlling the firing of the anode under load, that is, phase control. In the usual ignitron rectifier, phase-control operation is obtained when the firing of the ignitor is controlled. However, it is possible to provide phase control by means of the grids, the same as in a multianode rectifier, when a suitable grid and baffle arrangement are provided.

H. Winograd (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): In March 1939, the writer of this discussion made tests at the Alcoa plant of the Aluminum Company of America on switching of an aluminum pot-line circuit supplied by rectifiers with continuous excitation and control grids. These tests were made in co-operation with D. I. Bohn, coauthor of this paper. The writer thought it would be of interest to describe these tests and their background, in view of their similarity to the tests described in the paper.

In the first rectifier station for an aluminum reduction plant, which was put into operation at Alcoa in 1938, a 50,000-ampere d-c breaker was used for switching the pot-line circuit. This followed the practice formerly used in rotary converter stations. The cost of the breaker and its installation were a substantial item. The d-c busses were located outdoors, and the 50,000-ampere circuit had to be brought into the station to the breaker and out again. The maintenance of the breaker was also a problem. Since it remained in the closed position for months at a time, the closing and tripping mechanisms became sluggish and sometimes failed to function properly.

In view of these objections, other methods of switching the circuit were explored. As the control grids of the rectifiers can control the pickup of the anodes and can keep them blocked, it was decided to utilize this property for switching the load circuit.

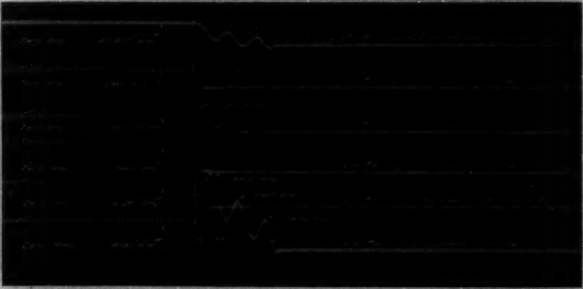
Each pot-line circuit was supplied by ten rectifiers operating in parallel. The units were displaced in phase from each other by means of wye and delta transformer primary connection and phase shifters ahead of the transformers to constitute a 30-phase system. The grids of each rectifier were controlled by means of an a-c voltage from a six-phase grid transformer and a variable d-c bias connected between the neutral of the grid transformer and the cathode of the rectifier. When the bias is negative, the a-c component of the grid voltage causes an anode to fire at a point in the cycle where the net grid-to-cathode voltage changes from negative to positive. With the grid transformer de-energized, the anodes remain blocked.

For testing the pickup of the pot-line load, the primary windings of the grid transformers of all the rectifiers were connected to a common three-phase supply through a three-pole contactor. The grid bias on the rectifiers was set at about 50 volts negative. With the load off, the grid contactor was opened. The pot-line breaker was closed, the rectifiers were connected to the a-c supply and the d-c bus, and the excitation arcs were established. The power circuits were, therefore, complete, and the rectifiers were ready to take load but were kept blocked by the negative voltage on the grids. The grid-supply contactor was then closed, and the rectifiers picked up the load, as shown in the oscillogram of Figure 3 of this discussion. The top trace of this oscillogram shows the current of the pot-line circuit. The other traces show the currents of five individual rectifiers. This test was repeated several times with the same results. This solved the problem of switching on the load.

For dropping the load, two methods were considered and tested. One method was to block the rectifiers by means of the control grids. The second method was to gang-trip the cathode breakers of the rectifiers.

Figure 3 (above)

Figure 4 (below)



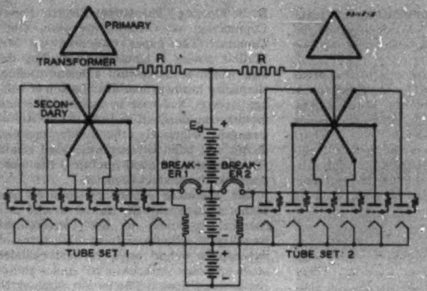


Figure 5

The grid-blocking method was tested by opening the contactor supplying the grid transformers, which left a negative bias on the grids of all the rectifiers. The results are shown in the oscillogram of Figure 4 of this discussion. The explanation of the oscillogram is as follows:

When the a-c grid supply was interrupted, applying blocking potential to the grids, the anodes which were not firing remained blocked. Some of the anodes which were firing dropped their current at the end of their firing period and remained blocked, the load current being transferred to other firing anodes in the same rectifier or another rectifier. The transfer of current between the rectifiers was facilitated by the fact that the rectifier units are displaced from each other in the 30-phase system. The result was that the anodes connected to one phase in several rectifier units continued to carry the current, because the energy stored in the inductance of the pot-line circuit maintained the flow of current over the entire cycle of the a-c voltage, until the energy was dissipated. This accounts for the 60-cycle a-c components in the pot-room and rectifier currents. The test was repeated several times, with similar results. The ripples in the current waves, prior to dropping the load, are exaggerated considerably by induction in the connections between the ammeter shunts and the oscillograph.

The effect of the pot-line inductance on the currents of the rectifiers was anticipated when this method of dropping the load was considered. However, it was decided to make the test. The results were somewhat better than expected, as it was expected that only one rectifier unit would carry the load until the energy of the pot-line inductance was dissipated. This method of dropping the load was considered unsatisfactory, on account of the overloading for several cycles of one rectifier-transformer phase and the anodes connected to it. It was therefore decided to drop the load by gang-tripping of the cathode breakers, which was tested and found satisfactory.

The method of switching the pot-line load described previously was applied in the next rectifier station at Alcoa, which was put in operation in May 1940, and in subsequent installations. It was also adopted in the first rectifier station, and the pot-line breakers were removed. The switching operations for picking up and dropping the load are performed by one control switch. When turned to the close position, the control switch energizes the three-pole contactor supplying the grid transformers. When turned to the trip position, it trips the d-c bus-tie breakers to the adjacent pot lines and operates a multi-pole relay for gang-tripping of the cathode breakers.

In the oscillogram of Figure 6 in the paper, showing the gang-tripping of the cathode breakers, there is a considerable time difference in the opening of the cathode breakers. This caused overloading of several rectifiers, the current on rectifier $5B$ attaining a value of 27,800 amperes. Such overloading is undesirable and is likely to cause arc-backs. This could be avoided if the contacts of the gang-tripping contactor and the shunt trips of the breakers were adjusted to get simultaneous opening of all the breakers. Such adjustments were made in the installations with which the writer was connected. A fractional-cycle electronic timer, connected as shown in Figure 5 of this discussion, was used for this purpose. It was determined by means of an oscillograph that the mechanical opening time of the cathode breaker was about $2^{1}/_{3}$ cycles from the instant the shunt-trip coil was energized. It was also found that the difference in the opening time of individual breakers was less than one cycle, if the air gaps of the shunt-trip armatures were approximately the same.

The timer consists of two tube sets. Each set consists of six thyratron tubes connected to the six-phase secondary winding of a small transformer. A source of d-c voltage, shown in the figure as a battery, is connected between the cathode of the tubes and the neutral point of the transformer in series with a current-limiting resistance R. The voltage E_d of the d-c source, measured between the positive terminal and the cathode connection of the tubes, is higher than the peak value of the phase-to-neutral voltage of the transformer, so that if only one phase were firing, the current through the tube will remain above zero continuously.

The contacts of the breaker are connected to the grid circuit of the tube set so that, when the breaker is closed, the grids are at a positive potential to the cathode, and when it is opened they are made negative. When the breaker is closed and the grids are positive, the thyratron tubes fire in sequence for one-sixth-cycle periods, the commutation being effected by the transformer secondary voltages. When the breaker is opened and the grids of the tubes are made negative, the tubes which were not firing at that instant remain blocked.

However, the tube which was firing continues to fire, as its current does not reach zero, and its grid is ineffective. The phase position of the tube which remains lit indicates the part of the cycle when the breaker contacts opened.

When two breakers, which are to be compared, are connected to the two tube sets and are tripped by means of the gang-tripping switch, the difference in the phase position of the lit tubes will indicate the difference in the opening time of the breakers. The backstop of the shunt-trip armature of one breaker is then adjusted until their opening time is the same, as indicated by the tubes. The breaker adjustment was considered satisfactory if the corresponding tubes in the two sets were lit in three out of four trials. One breaker was used as a reference breaker, and each of the other breakers was adjusted in turn to agree with it.

REFERENCE

1. ALCOA RECTIFIER INSTALLATION. J. E. Housley, H. Winograd. AIEE TRANSACTIONS, volume 60, 1941, pages 1266–74.

L. W. Morton: The authors agree with Mr. Cox that, provided the excitation equipment has sufficient energy to fire cold ignitors, the master excitation system can be operated successfully. Under the circumstances, while the grid-blocking method causes perfect pick up, the additional complication of grid blocking is not justified where the rectifiers are equipped with up-to-date excitation circuits, all of which have sufficient output energy to fire cold ignitors.

It should be kept in mind, however, that before the tests were made, the cause of improper pick up and over-all behavior of the equipment during starting up and dropping was not fully understood. Use of grid blocking provides perfect starting for the earliest group of 12 ignitron rectifiers placed in Aluminum Company service. The alternative of replacing 12 excitation circuits with higher-voltage circuits would have been too costly in critical materials to be justified at that time.

By means of the tests the authors were able to solve their immediate problems. If, as Mr. Cox says, "With this information schemes can be applied with much greater assurance than has been possible in the past," the authors' efforts have been worth while.

Mr. Herskind and Mr. Winograd have added valuable supplementary data pertaining to the application, installation, and operation of rectifiers for cell-line service. Mr. Winograd's suggestions for adjusting the opening time of cathode breakers, to facilitate disconnecting cell lines, are particularly interesting and should be of benefit to operators of large rectifier installations.

High-Voltage Power-Transformer Design

Discussion and author's closure of paper 43-88 by M. B. Mallett, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–24, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 526–31.

Hamilton Treadway (Rural Electrification Administration, St. Louis, Mo.): This paper is a timely contribution in the field of transformer design, since it comes during a period when the need for innovation in the design of transformers is most pressing if a reduction in the materials required in their manufacture is to be effected. The writer has been studying this problem for several years with the object of reducing the weight and cost of the transformer while at the same time improving its characteristics. The design features discussed by the author are of particular interest in this connection, and the opinion of the author on several points is desired.

The temperature at which oil deteriorates has long been recognized as determining the capacity limits of the apparatus. The limiting temperature imposed by oil is quite low. Yet, as the operating temperature is increased, over-all weight per kilovolt-ampere of capacity is decreased. As a solution to this problem, the writer has in mind the use of compressed gas as a cooling medium.

Compressed gas offers many advantages, not the least of which is its high dielectric strength. Coupled with the use of class-*C* insulating materials, the insulation level of the equipment can be increased greatly. Furthermore, it does not deteriorate when subjected to high temperatures, and, in combination with mica, fiber glass, porcelain, and other solid materials not affected adversely by heat, the operating temperature of the transformer may be increased considerably.

Compressed gas provides a superior cooling medium because of the ease with which it can be circulated. If the transformer is operated well above 100 degrees centigrade water can be used to cool the gas, and thus advantage may be taken of its great latent heat and low cost in most localities.

The author's opinion on the adaptability of the distributed concentric construction to the use of compressed gas as a cooling medium is desired. The writer has in mind the substitution of mica for the foundation cylinder with fiber glass used to provide the channel spacer. Porcelain would be used to provide spacing between coils, and mica and fiber glass or mica would provide the turn insulation. This construction should facilitate greatly the circulation of the gas and should permit greatly increased heat flow across each unit of surface between the heated parts and the circulating medium, since the outer surface of the coils will be exposed directly to the flow of the gas.

The reduced reactance of the distributed concentric construction offers a further advantage. The upper limit of the operating temperature of transformers, deliberately designed for high-temperature operation, would be fixed by the limits to which the resistance of the winding may be increased and still maintain satisfactory regulation characteristics. A design which decreases the inherent reactances increases this limit.

The extension of service into rural areas has demonstrated a need for improved transformer designs combining lower losses, higher insulation levels, greater capacity per unit of weight, and lower cost. Whatever direction is taken in the further development of the rural-distribution and substation transformers, the finished product must provide these essential features in its design.

E. V. Leipoldt (Shawinigan Water and Power Company, Montreal, Que., Canada): There are several features in the design and construction of the distributed concentric type of transformer, described by the author, which are of special interest to operating engineers. The method of shielding, and, particularly, the structural support of the line end shield are to be commended. The fact, that each coil, or group of coils, is rigidly supported on hard-walled cylinders without the aid of an impregnating varnish, is an advantage which the maintenance engineers will appreciate.

The most appealing feature in the general design is, to my mind, the adaptability of this construction to forced circulation of oil through the windings.

The Shawinigan Water and Power Company for many years has advocated the application of forced-oil circulation through the windings of the transformers as well as in the external coolers. Unfortunately, our appeals to the transformer designers have not heretofore been taken very seriously, and it has required the combined pressure of a conservation program and increasing technical interest in the subject to bring about these worth-while economies

Our company took the first step in this direction in 1936 by the purchase of two 50,000-kva three-phase transformers with external forced-oil and air-cooled heat exchangers. These transformers were of the shell type, and the first stage in directed flow through the windings was taken by enclosing the base of the core structure and leading the return oil into the enclosed area below the windings. This arrangement was made with the reluctant approval of the manufacturers and has been operating successfully to date.

No real economy was anticipated from this installation, as the oil circulated through the winding was governed by the requirements of the external cooler, in this case, 450 imperial gallons per minute. A subsequent installation of two similar transformers was made in 1939–40, and steps were taken to provide not only excess cooling capacity in the heat exchangers in view of possible increased loading of the transformers, but also additional oil-circulation facilities to regulate the flow of oil through the windings of the transformer. Unfortunately, the war in which our country has been engaged since installation of the new transformers has prevented a program of testing which has been under consideration.

New transformer capacity has been added recently to this substation, and we hope to carry out some tests to determine the extent of increased loading with increased circulation of oil.

D. B. Fleming (The Hydro-Electric Power Commission of Ontario, Toronto, Ont., Canada): The paper presented by Mr. Mallett covers a transformer-winding design which tends toward efficient cooling, simplicity in the proper distribution of voltage stresses, and ease in maintenance and repairs, combined with adequate mechanical strength. Naturally, the simpler one can design and build a transformer and retain the maximum of good qualities, the more desirable the product becomes.

Our operating experience confirms the author's statements in regard to the mechanical weakness of the conventional barrel-type cylindrical coil. The turns of flat copper wound on a vertical cylinder often telescope on each other under short-circuit conditions. The author appears to have overcome this possibility by enveloping each turn in an insulating channel, which provides a double layer of insulation to act as a mechanical support over the whole area of the under and top side of the turn and distributes the axial stresses evenly. It occurs to me that the column could be made even more solid and secure if the parallel conductors were wound on edge so that the wider surface would distribute the axial stresses better, and the transfer of heat to the oil would be mostly through copper, rather than layers of copper and insulation. The change in symmetry would not then be necessary at the ends where additional turn insulation is provided. This, however, might not be practicable for mechanical reasons.

It has been found that, in the disk-coil radial-spacer type of construction, because of the concentration of stresses at the spacers, there is a tendency for the edges to cut through. This is true especially at the end coils, where the insulation is reinforced. Furthermore, the spacers become embedded in the insulation, so that the intervening spaces between supports become partially, and sometimes wholly, blocked with resultant heating and deterioration of the insulation and, finally, failure. Similar types of failures have occurred on shell-type transformers, where the unit stress on the insulating spacers at the inside corners is increased because of lack of room for providing both adequate supports and oil ducts for cooling. The insulation crushes, allowing the turns to become displaced and short-circuited. Experience during operation will show whether or not this new construction the author describes will overcome these troubles.

In Figure 4 of the paper it may be noted that the ends of the cylinders are cut back, so that the axial support comes on the copper column and not on the cylinders. This is important and is often not appreciated by the workmen in the factory assembly. Inspection of transformers during factory construction often shows that careless assembly or lack of attention to small details of this kind vitiate a meticulous design.

It is not clear from the description or illustrations how an even distribution of pressure is effected by the end supports and clamps. Where the number of stacks of coils increases, the problem of supporting them evenly also increases. In the construction using a disk-coil primary winding and a cylindrical secondary winding, where there is vastly more insulation in one than the other, more shrinkage will occur in the former, and separate pressure blocks with accompanying means of adjustment are essential. The practice of rolling or precompressing the insulation, however, may overcome much of this shrinkage. Periodic inspection and maintenance have disclosed that as much as an inch thickness of insulating spacers must be added at the ends of the disk coils to tighten down the whole assembly securely on the clamps. In the shell-type transformer, the insulation shrinks, and the spacing strips often fall to the bottom of the tank, requiring major and costly repairs.

It may be noted that the tap leads are all carried down the face of the coil to the bottom, but the illustrations do not show where these are terminated nor the location of the ratio adjuster. There will be a considerable voltage difference between the tap lead and the bottom of the coil which introduces hazards. This might be overcome to a great extent by carrying the top taps out the top of the winding and the bottom taps out the bottom.

Simplicity of repairs of a faulty winding is an important feature of this type of construction, although the cost of a whole cylindrical coil may be considerably more than that of a couple of disk-type coils. Happily, however, the percentage of failures in the modern transformer is rare compared with the failures experienced in the older designs of 15 years ago and earlier.

The very effective cooling, by means of the straight vertical oil ducts, is a very important feature. It tends toward smaller and lighter transformers for the same kilovolt-ampere capacity. The Hydro-Electric Power Commission of Ontario is extending its practice of operating transformers on a thermal-capacity basis and adding additional cooling by means of fans to normal self-cooled units. The construction the author describes in his paper lends itself to forced-oil cooling. This is very interesting, as it permits the transformer to be purchased as a nominally rated self-cooled unit with certain provisions on it, so that, as the load grows, fans or blowers can be added and, finally, forced-oil cooling to obtain the desired increases in capacity.

H. H. Wagner (Pennsylvania Transformer Company, Pittsburgh, Pa.): There is a present tendency toward the use of cylindrical layer windings in power transformers, as indicated by this paper and other recent transformer technical articles.

For the past 15 years our company has built concentric cylindrical layer windings for voltages up to 66 kv and in sizes up to 20,000 kva. The design principles of this type of winding have been developed with the aid of a large amount of laboratory research, including 60-cycle and impulse tests and mechanical-pressure tests in our 300-ton hydraulic press.

This experience has indicated the applicability of this type of design, not only to the high-voltage ratings described in the paper, but also to medium- and lower-voltage classes.

Both our service record and laboratory testing confirm, in general, the conclusions of the paper as to the desirable electrical, thermal, and mechanical characteristics of this type of winding.

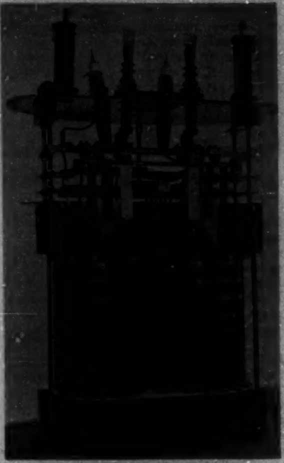


Figure 1. 5,000-kva core-type layer-winding transformer

This type of winding is also applicable to fully insulated transformers as well as to graded insulation. With full insulation it is desirable in the higher-voltage classes to use a shield at both line ends of the winding.

In the lower-voltage classes we have found that a good internal-impulse voltage distribution can be obtained without the use of shields. This is partially due to the higher layer to layer capacitance resulting from the use of thinner-layer insulation for the lower voltages.

Our experience has shown that this type of winding, together with a baffle as described for forced-oil cooling, also gives excellent results for forced-air cooling on dry-type air-cooled transformers.

Figure 2. Double-magnetic circuit core—partially assembled

Our practice has been to use square or nearly square conductors for layer-type windings instead of the thin flat strap conductors shown in the paper. Of course, the use of square wire requires a larger number of conductors for a given total cross section, since the permissible thickness of the conductors is limited by the copper eddy-current losses.

A reliable mechanical construction is assured, by supporting each conductor on all four sides radially and axially, by the use of vertical spacers in close contact with each layer of conductors instead of supporting only three sides of each conductor as shown in the paper, and by thoroughly impregnating each winding in varnish.

The construction of a cylindrical-layer winding-core-type transformer is illustrated in Figure 1 of this discussion which shows the core and coil assembly of a single-phase transformer rated 5,000 kva, 34,500 volts.

While the core-type single magnetic circuit is suitable for many ratings using layer windings, wide use has been made of a double-magnetic-circuit-core design which has several important mechanical advantages wherever applicable. Figure 2 of this discussion shows a partially assembled core of this type, which illustrates also the method of supporting the windings.

M. B. Mallett: The author thanks contributors of discussion for their comments and interest.

Mr. Leipoldt's present advocacy of directed forced flow, after his seven years of actual experience with this type of cooling in large transformers, indicates to the author that directed flow in such machines must be entirely practical from an operating point of view.

Mr. Fleming suggests the desirability of not splitting the coil conductor radially. This is often feasible in 25-cycle designs, where the reduced frequency ordinarily permits some $^5/_{16}$-inch radial thickness of solid copper without excessive eddy loss. In connection with this point, the author emphasizes the fact that the basic requirements for barrel coil stability, as stated in the paper, are considered essential, irrespective of the form of coil conductor used, whether flat wound strands, edge wound strands, square strands, a single strand of any proportions, or a single cable comprising a number of continuously transposed strands. Because of their importance, these basic requirements for coil stability are reiterated here as follows:

1. Positive axial separation of conductors.
2. Firm coil clamping.
3. Freedom of coil-diameter expansion.

In regard to clamping of the final structure, the required pressure distribution on the various high-voltage and low-voltage coils is obtained by means of an adjustable plate located between the end insulation and fixed top clamp member. After final vacuum shrinkage, this plate is backed off, necessary adjustments are made readily in the various progressively disposed radial end spacers, and pressure is reapplied with the adjustable plate.

With reference to tap-lead arrangement, all straps are usually brought to one end of the tap coil to minimize the potential from leads to adjacent outer coil. Such an arrangement increases the potential difference between straps and the tap coil itself, but ordinarily this difference is relatively small, being approximately $7^{1}/_{2}$ per cent, for example, in the particular design illustrated by Figure 5 of the paper.

Mr. Wagner indicates the opinion that conventional-type barrel-coil construction is suitable for application in large power transformers. This opinion, however, conflicts with that of the author for specific reasons stated in the paper.

Mr. Treadway asks about the adaptability of the distributed concentric construction to the use of high-pressure gas in combination with class-*C* insulation. The general construction is uniquely suited for forced gas cooling, but the author believes that at present there are two basic obstacles to the practical application of compressed gas and extreme operating temperatures.

1. The erratic behavior of creepage surfaces under gas pressure in respect to impulse dielectric strength, as indicated by present knowledge of the subject.
2. The mechanical limitations of class-*C* materials as are now available.

Interim Report on Overloading Current-Limiting Reactors

Discussion of paper 43-109 by the transformer subcommittee of the AIEE committee on electrical machinery, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 611–12.

A. F. Phillips (Duquesne Light Company, Pittsburgh, Pa.): The Interim Report on Overloading Current-Limiting Reactors is a very good guide for usual conditions experienced in service. However, rather than attempt to follow any general rule in loading equipment, it is believed that the urgency of wartime needs amply justifies the effort required to place individual ratings on equipment. In most cases, a general method of rating leads to a conservative loading practice. However, there may be instances when this is not true. For example, we have some four-kilovolt dry-type reactors enclosed in concrete compartments with Transite doors. When full load is carried, the temperature rise above the ambient temperature was 79 degrees centigrade, whereas the rise in open air was only 39 degrees centigrade. This condition was corrected by providing adequate ventilation for the compartment, which reduced the compartment rise over ambient temperature from 40 degrees centigrade to 9 degrees centigrade.

In another case when the loading was increased above the name-plate rating of the reactor, considerable heat developed in the steel compartments. Currents up to 420 amperes were measured in the supporting angles for the compartment. The cubicle was modified by cutting both the upper and lower angles in the middle, breaking up the path for the circulating current. This reduced the compartment rise to a satisfactory value.

Interim Report on Overloading Distribution Transformers

Discussion of paper 43-87 by the transformer subcommittee of the AIEE committee on electrical machinery, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 516–17.

E. F. Dissmeyer (Commonwealth and Southern Corporation, Jackson, Mich.): The Interim Report on Overloading Distribution Transformers is somewhat disappointing in that it does not present any specific data. It merely states: "It is difficult to fix specific limitations for operation of distribution transformers," and "It seems necessary therefore that those users seeking the near ultimate in loading of distribution transformers base their thoughts and conclusions on the particular characteristics of the equipment and loads peculiar to their own systems." The reference list which has been provided will undoubtedly be appreciated by the industry.

The transformer subcommittee has made many fine contributions to the industry, and their possible failure to provide complete and usable data on the loading of distribution transformers might seem to indicate that no problem exists. It would seem, however, that the subcommittee could have made greater use of surveys of distribution-transformer loading practices which provide valuable data for industry use. If a single practice cannot be established, possibly distribution systems can be divided into two or more classifications and a loading practice provided for each type of system. Such type of information should be of considerable value to those interested in the operation and design of distribution systems.

It is the writer's opinion that individual studies can be justified for the larger sizes of distribution transformers and that the principles outlined in the "Interim Report on Guides for Overloading Transformers and Voltage Regulators"[1] can be utilized. In regard to the pole-type sizes, past experience seems to indicate that, with well-balanced distribution-system design, the thermal loading does not present a serious problem. Burnouts due to overloading have been rare. Where burnouts have been experienced, it has usually been found that they were due to some unexpected load and that they have often occurred in cases where the service to a single customer has been involved. The design of a distribution system requires proper allocation of voltage drop in the overhead primary, transformer, and secondary circuits, and generally it will be found that excessive voltage drop will result before a pole-type transformer is either burned out or before an excessive amount of insulation life has been consumed.

Economic considerations usually require that the transformer voltage drop be large compared to that in the overhead primary and secondary circuits. As a result transformers either are loaded to rating or, in the case of smaller transformers, carry overloads during peak periods. Since transformer loading has a major influence on the customer's voltage, complaints will usually result prior to transformer damage or burnouts.

During the present emergency, most power-supply companies are permitting a greater voltage variation on their distribution system, and a resultant greater loading of distribution transformers is being experienced. A review of past experience seems to indicate that the quality of service to a customer will not be reduced to such an extent that an excessive number of transformer burnouts will be experienced. A moderate increase in life consumption and an occasional transformer failure would be consistent with the wartime practices being used in the operation of other equipment.

REFERENCE

1. INTERIM REPORT ON GUIDES FOR OVERLOADING TRANSFORMERS AND VOLTAGE REGULATORS, AIEE transformer subcommittee. AIEE TRANSACTIONS, volume 60, 1941, September section, pages 602–4.

W. A. Sumner (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): One of the references in the paper is the Putman–Dann paper, "Loading Transformers by Copper Temperature."[1] In this paper was included a report on a program of temperature-cycle tests started on four five-kilovolt-ampere 2,400-volt *CSP* (completely self protected) transformers June 1, 1936. As reported in that paper, a total of 867 cycles had been completed by June 15, 1939. These tests have been continued and now after seven years total 1,911 cycles, without failure. Because of the bearing of these tests on the present interim report, it is believed that an up-to-date progress report will be of interest to this group.

For the last 1,811 cycles, two of these units (*A* and *B*) have been operated at 390 per cent load for an average of 48 minutes for each overload and in that time attained a maximum copper temperature by resistance of 142 degrees centigrade. Two other units (*C* and *D*) operated at 450 per cent load for an average duration of 34 minutes, attaining a maximum copper temperature by resistance of 135 degrees centigrade.

During the course of these runs, the standard dielectric tests for a new 2,400-volt transformer have been applied 17 times, the last being made on the 1,533rd run. Surge tests, with a 56-kv $1^{1}/_{2}$–40-microsecond positive wave have been applied 18 times, the last of the surge tests having been made on the 1,810th run. No sign of distress was disclosed by these tests.

The dielectric strength of the oil, which has not been renewed, has been tested frequently and at the 1,810th run averaged 29 kv on the four units.

These units have not been torn down for examination of the insulation, as it is the intention to continue the runs until failure.

However, power-factor tests have been made and indicated 1.5 per cent or lower at 20 degrees centigrade, which is as good as or better than the average new transformer. This appears to be a very good indication that, in spite of this large number of cycles, the insulation is in satisfactory condition. Since these winding temperatures are substantially in excess of those contemplated by the interim report, it is felt that the recommendations of the committee can be considered as conservative.

REFERENCE

1. LOADING TRANSFORMERS BY COPPER TEMPERATURE, H. V. Putman, W. M. Dann. AIEE TRANSACTIONS, volume 58, 1939, October section, pages 504-09.

Hamilton Treadway (Rural Electrification Administration, St. Louis, Mo.): The war emergency has focused attention on the problem of overloading transformers as well as other electric equipment. This interest is prompted by the necessity for saving critical materials, and perhaps a great deal could be accomplished in this direction by the adoption of new theories in the design of such equipment. Still much can be accomplished in the direction of conservation of critical materials by utilizing the maximum capacity of existing equipment.

In the distribution-transformer field much can be gained by rearrangement of system layouts and by relocating transformers to attain the maximum use of existing capacity. This requires regular load surveys and a means of readily interpreting the results. The methods developed by Beavers[1] and Clark[2] are easily applied to substations where recording demand meters are used, but no accurate means has been developed for small installations where load curves are not available. It is desirable that a means be developed to permit evaluation of the overload capabilities of these smaller transformers in terms of the type of load served, the indicated maximum demand, and load factor. Probability curves for each type of load may be worked out which will permit the experienced operator to secure a maximum from his present installed transformer capacity. To accomplish this, thousands of load curves must be assembled, grouped, analyzed, and evaluated. This undertaking is outside the scope of any one group. It is suggested that the subcommittee should work out procedures to be followed and assign the task of accumulating and preparing the data on each individual type of load to that group in the industry best situated to do the work.

Whatever data of this nature are supplied by the subcommittee in future reports will prove of inestimable value to the industry.

REFERENCES

1. SELECTING DISTRIBUTION-TRANSFORMER SIZE, M. F. Beavers. ELECTRICAL ENGINEERING, volume 59, October 1940, pages 407–12.

2. SUBSTATION-TRANSFORMER EMERGENCY LOADING PRACTICE, L. W. Clark. AIEE TRANSACTIONS, volume 62, 1943, March section, pages 126–32.

F. E. Andrews (Public Service Company of Northern Illinois, Chicago, Ill.): Much discussion and consideration of the problem of overloading distribution transformers has been directed along the lines of determining the value and kind of overload which a transformer will safely stand, the implication usually being that there should be sufficient margin in any practices adopted to avoid much risk of transformer failure.

We, as engineers, are inclined to take credit for establishing a good operating record as measured by a very low rate of transformer failures due to overloads. I wish to raise the question whether it would not be better engineering, from the standpoint of economics, to carry a considerably higher average overloading on our transformers, thereby requiring less installed transformer capacity at the expense of a higher failure rate.

To state this in another way, it would appear to be a good business judgment, at least for the average run of smaller distribution transformers, to use design loadings representing fairly high overload values. General experience indicates that in a large number of instances design loadings are not attained under actual operating conditions. This, together with other marginal considerations, results in the duty on the average transformer installation frequently being less than the maximum design duty. Furthermore, with the smaller transformers the financial risk and importance of a service interruption are much less than with power-transformer installations or with large distribution-transformer installations. Therefore, it is reasonable that the smaller transformers be applied to the system under a somewhat less conservative loading basis than the others. It would accordingly appear that the engineers and operators of any system, in order to obtain maximum economic benefits from their distribution-transformer installations, should, upon their own responsibility, be willing to go beyond the conservative recommendations for loading established by general industry standards, and by manufacturers for design purposes.

It is also recognized that in considering the economics of transformer overloading, the cost of the higher losses and of higher over-all voltage regulation must be taken into account. In some cases, these factors, rather than temperature and life, should determine the overload limits.

Relay Protection of Tapped Transmission Lines

Discussion and authors' closure of paper 43-118 by M. A. Bostwick and E. L. Harder, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 645–50.

W. R. Brownlee (The Commonwealth and Southern Corporation, Jackson, Mich.): Some caution must be exercised in thinking of the two-wire a-c pilot-wire scheme proposed by the authors as a true ratio-differential scheme in the ordinary sense of common three-winding-transformer differential relays, where the sum of all three currents is available for restraint. Even the two-wire a-c pilot-wire scheme used on two-terminal lines is only partly "ratio differential" in that restraint is proportional to local current, which represents either through current or the contribution of one terminal to an internal fault. However, in the case of the three-terminal application, consider a heavy through fault *in* on terminal 1 and *out* on terminal 2 with practically no current in terminal 3. In this case terminal 3 has no restraint but is subject to an operating voltage corresponding to a small *per cent* difference of two large values. Thus the careful balancing of pilot-wire voltages, excellent current-transformer characteristics, and the accurate matching of saturating and cutoff devices which are used to limit the pilot-wire voltage are relied upon to prevent false operation, instead of the "ratio-differential" principle.

The application of a-c pilot-wire schemes to three-terminal lines may be limited on account of the relative values of load and short-circuit current. Overcurrent relays are sometimes set to operate with as little as $1^{1}/_{2}$ times maximum load current, and it is expected that the minimum short-circuit current shall be at least $1^{1}/_{2}$ times the relay setting, so that a ratio of minimum short-circuit current to maximum load current of at least $2^{1}/_{4}$ is expected for satisfactory operation; since the three-phase fault current is about 15 per cent greater than the current for a phase-to-phase fault, the required ratio may be about $2^{1}/_{2}$ to 1. In contrast, the two-wire a-c pilot-wire scheme, operating on sequence currents or variable current-transformer ratio principles, requires a 2:1 ratio just to take account of different response to three-phase load and single-phase-to-phase faults. Furthermore, if incorrect operations on a two-terminal line are to be avoided during momentary interruptions of the pilot-wire circuit in the presence of load current, an additional factor of 2 is introduced because only one half as much load current is required to cause tripping with the pilot wire open as is necessary under normal fault-clearing conditions with the pilot-wire circuit closed. This gives an over-all "phase factor" of 4 instead of 1.15, or, if the critical ratios of pickup load current to minimum fault current to relay pickup are to be maintained, then the minimum short-circuit current (from the terminal of the weakest source) must be nine times the maximum load current. In the case of "*T* taps," it is expected that tripping energy would be divided among the three terminals (when the lines are energized from one terminal), so that the required ratio of minimum short-circuit to maximum load current becomes about 13, which would exclude many applications.

Carrier-current schemes combined with distance relays are subject to other difficulties than merely a possible reversal of current at one terminal of a three-terminal line, and one specific case was analyzed where such an arrangement would not provide even second-zone sequential tripping after the terminal nearest the fault had opened.

The authors have mentioned parallel operation with industrial generating plants where certain nonessential loads would be "dumped." It is not necessary that an industrial customer should have "nonessential" load which can be "dumped" in order to enjoy the advantages of parallel operation while a part of the load is being carried from a utility system. Frequently a sectionalizing switch is arranged so that when open it leaves a load of approximately that desired for local generation on the industrial generators and the remainder on the utility line. The operation of such a switch on a fault beyond the line terminals does not "dump" any load and therefore may be allowed without serious inconvenience. Thus the sectionalizing breaker may be operated by high-speed fault detectors which are sensitive enough to reach faults *within* the protected section under all conditions. This permits instantaneous reclosing on the utility tie line to provide uninterrupted service to the sectionalized load in case of a flashover on the tap line. Sometimes inertia relays are used for sectionalizing in case of ground faults where the transformer supplying the industrial customer is not grounded.

One method not mentioned by the authors is that of transferred tripping over pilot-wire circuits. The performance of a specific installation made in 1931 was described in 1935[1] and a special application for network service was described in 1938.[2] While carrier-current equipment has been utilized for transferred tripping to protect transformers operating as part of a two-terminal line, it is unfortunately not applicable to the protection of a three-terminal line where the carrier impulse must be transmitted over the line which is faulted.

References

1. Pilot-Wire Relay Protection, E. E. George, W. R. Brownlee. AIEE Transactions, volume 54, 1935, November section, page 1262.

2. Remote Control of Network Protectors, W. R. Brownlee, W. E. Dent, Jr. AIEE Transactions, volume 57, 1938, December section, page 685.

A. J. McConnell (General Electric Company, Schenectady, N. Y.): In the past, there has been little formal discussion in regard to the general problem of relaying three-terminal lines. Bostwick and Harder have analyzed admirably this general problem.

In regard to the protection of three-terminal lines by means of the *HCB* relay, it may be that the problem has been somewhat oversimplified. For example, it is stated that the "*HCB* two-wire a-c pilot-wire scheme circumvents all of these problems . . . ."

From an inspection of Figures 8 and 9, it appears safe to assume that a current of as little as six amperes leaving one terminal (m) will block all three terminals, regardless of the distribution of the currents at the other two terminals (p and n). This means that for an internal fault near terminal n a current of six amperes or greater leaving terminal m will result in a failure to trip. A current of six amperes leaving one terminal for an internal fault is not at all uncommon, especially in those cases in which the impedance from the junction point to n via m is equal to or less than the impedance internally from the junction to n.

It would also appear that there is a possibility for a relay at a terminal which has no infeed to trip on a heavy fault external to one of the other terminals. For example, if there should be a difference between the output currents of the two relays carrying the through fault current, because of either the main current transformers or the saturating transformers in the *HCB* relays, a current would appear in the operating coil of the relay at the third terminal. Since there would be no corresponding restraining current, the relay would trip incorrectly if this operating coil current were above the relay pickup for that condition.

G. B. Dodds (Duquesne Light Company, Pittsburgh, Pa.): The authors have brought out the difficulties of relaying tapped transmission circuits, have pointed out the possible use of a-c pilot-wire protection in solving some of the problems, and have encouraged presentation of specific examples of solutions to particular problems.

The example shown in Figure 1 of this discussion is typical of the manner of serving a number of war load customers. The original condition was two 68-kv transmission circuits between stations *A* and *B* in the middle of an important transmission system. A new war load customer is being tapped to one of these circuits as shown. The circuits between stations *A* and *B* had existing *HCB* pilot-wire protection; the connection of the new customer as a tap nullified this protection on the one circuit. The resistance of the pilot conductors was already up to the limit of standard two-terminal protection, being approximately 2,000 loop ohms resistance and 1.4 microfarads capacitances.

It appeared hopeless to apply standard three-terminal pilot-wire protection because of the high resistance necessary for each of the legs of the pilot conductors. The scheme shown on Figure 1 was finally worked out and tests made to prove its satisfactory operation. This scheme consists of installing a standard *HCB* installation at the new tapped customer and having it normally disconnected from the pilot-wire circuits. It is connected to the pilot-wire circuits only in event of a fault on the low-voltage bus at the new station. The impedance of the transformer bank limits the fault current to such a magnitude that satisfactory operation is obtained on the three-terminal pilot wire for this fault location.

For other fault locations, such as through faults in either direction from stations *A* or *B* or inside faults on the protected circuit where the fault currents are much higher, the installation operates as a standard two-terminal arrangement.

The third terminal at the new station *C* is connected and disconnected from the pilot wire by means of an instantaneous overcurrent relay which picks up only for faults at station *C*. In order to co-ordinate the operation of the pilot-wire relays at stations *A* and *B* with the cutting in of the relays at station *C*, a slight amount of time delay was added to the pilot-wire relays at stations *A* and *B*. A time in the neighborhood of four cycles was found satisfactory.

The single-phase grounding switch shown on the sketch is closed by operation of the transformer-bank differential relays. Its purpose is to provide sufficient current to operate the relays at adjacent stations. The 68-kv system is grounded through two 63-ohm resistors in parallel, and as a result there is little voltage disturbance associated with a line-to-ground fault.

M. A. Bostwick and **E. L. Harder:** Mr. Brownlee made the statement that ". . . even the two-wire a-c pilot-wire scheme used on two-terminal lines is only partly ratio differential in that restraint is proportional to local current which represents either through current or the contribution of one terminal to an internal fault." This is not true of the type-*HCB* two-wire a-c scheme referred to throughout the paper, although it may apply to another scheme. For the *HCB* relay, the operating equation is derived in this discussion. Equation 6 shows that there is a restraining force pro-

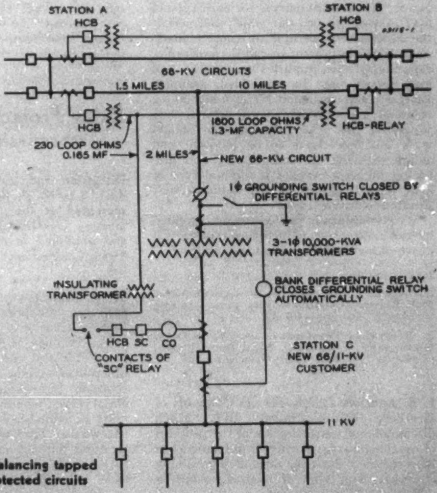


Figure 1. Method of balancing tapped load on pilot-wire protected circuits

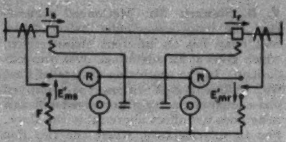


Figure 2. Ratio differential—circulating current

portional to the sum of the currents at the two ends of the line

As the limiting devices of the relay take effect at higher currents, the effect is the same as though the currents at the two ends remained constant at the values that operate the limiting device. Thus the operation then depends only on the phase position between these two currents and the relay takes on a "directional-comparison" characteristic. This results in large restraint for through faults and tripping for internal faults.

On the three-terminal line Mr. Brownlee's point is correct that a terminal with no current has no restraint. However, it should not be concluded that "careful balancing of pilot-wire voltages, excellent current-transformer characteristics, and accurate matching of saturating cutoff devices" and so forth are depended upon for prevention of false operation. At high currents which operate the saturating devices, two voltages of about 60 volts act in series to circulate current over the pilot wires with a resulting zero voltage at the center point of the pilot wires. One of the limiting devices would have to be 50 per cent off, giving only 30 volts to produce 15 volts across the pilot wires at the mid-point which would be necessary to operate falsely the third terminal. They are, of course, easily held to much smaller tolerances than this. Also the limiting devices in the relay make the scheme relatively insensitive to wide differences in current-transformer characteristics. The scheme was designed originally to operate with one current transformer that saturated at 30 volts and another that was unsaturated at 120 volts. This represents a 4-to-1 ratio in currents required to saturate these two transformers. Thus, while ratio characteristics do not help the third terminal for the case cited, still ordinary care in design and application provide ample margins of safe operation because of the fundamental design of the scheme. The limiting devices play a big part in this, because they prevent ever getting a difference of two *large* quantities.

Mr. Brownlee's discussion of operating limits reflects a condition which is sometimes encountered although not so severely as indicated, for several reasons. In the first place it is not absolutely necessary to set above load currents. The largest single user of pilot-wire currents[1] purposely sets below load current instead of using supervision equipment to detect outages of the pilot wire. With good pilot wires the infrequent tripping from this source under normal load conditions is not objectionable on the types of systems where pilot-wire relays are frequently used, that is, where load busses are dual fed. Secondly, if there is backup protection, it is not necessary that the pilot-wire relay trip for the smallest possible current from the weakest terminal with all other terminals open. Also it should be borne in mind that the ground-current setting can be made sensitive independent of load currents.

In the carrier-current application mentioned, where second-zone sequential tripping was not obtained, after the first terminal was opened, the mutual effect was evidently not considered in the setting. The proper method is given in the paper.

The industrial tie scheme described is commnedable. Weak ties particularly should be arranged to open promptly during disturbances to avoid the more serious consequences of out-of-step operation.

The arrangement described by G. B. Dodds for applying the *HCB* relay to three-terminal lines of high pilot-wire resistance represents a valuable contribution to the science of applying this relay. It is also a partial answer to the load-fault ratio problem mentioned by Mr. Brownlee, since it reduces the operation to two-terminal-line operation, except for external faults on the third terminal.

Shunt reactors to cancel pilot-wire capacitance and higher-ratio insulating transformers have also been useful in applications involving pilot-wire capacitance and resistance, respectively, beyond the normal range for application.

OPERATING CHARACTERISTICS OF PILOT-WIRE RELAY

Ratio Differential—Circulating Current—Figure 2. The voltages E_{ms}' and E_{mr}', produced in the relay network by currents I_s and I_r, respectively, act normally in series to circulate a restraint current proportional to the load over the line. The voltage between pilot wires midway between stations is zero, and, if the pilot-wire impedance r is made relatively small compared with R plus F, the voltage across the operating coils is negligible. Through current, therefore, produces current in restraining coils only.

If equal currents flow in from the two ends to a fault, the voltages E_{ms}' and E_{mr}' will be equal and opposed, so that no current will circulate over the pilot wires. While equal currents into the line produce the same current in both R and O coils, there is a net operating effect due to the very much larger number of turns on the operating coil.

The currents I_s and I_r may in general be considered the superposition of two sets of currents:

I_{sm} and I_{rm} equal and in phase.

I_{sn} and I_{rn} equal and 180 degrees out of phase.

These are the two types of current for which relay action has been stated. Under normal load conditions the first type only is present. The superposition stated mathematically is:

$$I_s = I_{sm} + I_{sn} \tag{1}$$

$$I_r = I_{rm} + I_{rn} = I_{sm} - I_{sn} \tag{2}$$

The amount of each type of current present for any particular pair of currents I_s and I_r is determined by solving these equations, giving

$$I_{sm} = I_{rm} = (I_s + I_r)/2 \tag{3}$$

$$I_{sn} = -I_{rn} = (I_s - I_r)/2 = I_f/2 \tag{4}$$

where I_f is the fault current.

The restraint coil current is proportional* to I_{sm} and the operating coil current to I_{sn}. In a polar relay forces are proportional to the first power of coil currents. Therefore, the equation expressing the balance of forces at the minimum operating point is as follows, absolute values being effective due to rectification of the quantities for use in operating coils:

$$|I_{sn}| = C|I_{rm}| + B/2 \tag{5}$$

Operating force = restraining force + spring and magnetic bias force

or

$$|I_s - I_r| = C|I_s + I_r| + B \tag{6}$$

or

$$|I_f| = C|I_s + I_r| + B \tag{7}$$

The vertical bars indicate that the absolute value is referred to.

The relay, therefore, operates whenever the fault-current magnitude exceeds a fixed proportion of the average through fault current, plus a constant. This characteristic is a circle of diameter equal to $C|I_s + I_r| + B$. I_s and I_r vectors terminate on the circle at opposite ends of any diameter. Tests and

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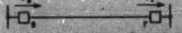
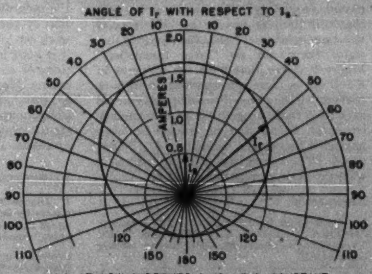
* The slight approximation of neglecting the I_{sn} component in the restraint coils is permissible with the relay constants used.



(a)—CURRENT REFERENCE DIRECTIONS



(b)—AT LOW CURRENTS, I_s IS HELD CONSTANT

Figure 3

theory also indicate the tripping locus of I_r to lie on a circle as shown in Figure 3 for a fixed I_s value and relay setting.

This characteristic is the same as is used in the ratio differential protection of generators and transformers. As both through current and fault current become very large, the two terms of equation 7 become large compared with Figure 3. The tripping point then approaches the pure ratio:

$$I_f = C|I_s + I_r| \tag{8}$$

Limiting Devices. If the filter outputs are passed through limiting devices such as saturating transformers before application to the pilot-wire and relay-circuit combination, the characteristics may be understood by consideration of the limit. For values of I_r and I_s above the limit the relay action is the same as though each had the limiting magnitude but retained its phase position. If $\bar{I}_s = \bar{I}_r =$ a constant limiting value,** then if θ is the angle between I_s and I_r we may write:

$$I_s = \bar{I}_s; \quad I_r = \bar{I}_s \epsilon^{j\theta}$$

and equation 6 becomes

$$\bar{I}_s|1-\epsilon^{j\theta}| = C\bar{I}_s|1+\epsilon^{j\theta}| + B$$

and, since $\bar{I}_s$ is a constant (the limiting value)

$$|1-\epsilon^{j\theta}| = C|1+\epsilon^{j\theta}| + D \tag{9}$$

The balance thus depends solely on θ, D being a constant, and the relay has pure directional characteristics.

If C is 1 and $D=0$, the equation is satisfied for $\theta = \pm 90$ degrees, and the relay would trip, with large line currents, only when I_r is over 90 degrees out of phase with I_s, either leading or lagging.

For other values of C and D other angular limits between I_s and I_r are established as the threshold of tripping.

Mr. McConnell points out the blocking due to six amperes flowing out one terminal for an internal fault in Figures 8 and 9 of the paper. These curves and Figure 7 are all based on a 0.5-ampere ground-current setting as mentioned in the text. Thus six amperes represent 12 times the relay setting. The curves also apply to phase currents, but the scales must then be increased in the ratio of phase-current setting to ground-current setting. Experience to date has not shown cases beyond the limits of correct tripping mapped out by the curves of Figures 8 and 9.

The question in regard to possible false tripping due to large through current with no current at the third terminal was discussed previously. The main point was that with limiting devices the "difference of two large quantities" is never obtained, because there are no "large quantities." Figure 9 shows that with ten amperes in n and out m there is a very wide margin to prevent tripping even if two amperes entered at p (this could be possible with secondary currents). The margin would be even greater if no current entered at p. This is shown by a comparison of Figures 8 and 9 with the same currents at n and m.

Reference

1. Pilot-Wire Relaying on a Metropolitan System, T. G. LeClair, E. L. Michelson. AIEE Transactions, volume 62, 1943, August section, pages 511–15.

** Bars over characters represent scalar quantities.

A New High-Speed Balanced-Current Relay

Discussion and author's closure of paper 43-117 by V. N. Stewart, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 553–5.

A. J. McConnell (General Electric Company, Schenectady, N. Y.): As a former designer of induction-cylinder relays, it occurs to this discusser that the author might have pointed out certain advantages that accrue simply because of the use of that type of relay element. For example, high-speed relays of the magnetic-attraction type, as well as other designs, have a double-frequency force or torque which naturally results in a certain amount of vibration that may affect adversely the relay characteristics.

Such an adverse effect is particularly evident in relays in which one force or torque is balanced against another. For instance, if the peaks of the double-frequency-torque waves do not occur at the same time, it logically follows that operation will occur at a lower rms operating current than if the peaks occurred at the same time. Translated into relay nomenclature, this means that the relay will have a phase-angle error, in many cases sufficient to nullify the safety margin (slope) normally associated with balanced-current relays. Of course, the balanced-current relay is subjected to currents of different phase angles whenever the parallel lines do not have the same R/X ratio.

Because of the steady torque of the induction-cylinder-relay element, the induction-cylinder balanced-current relay should have a very small phase-angle error. Perhaps the author can provide this information.

Experimental proof that the induction-cylinder relay has substantially no net double-frequency torque has been presented in the form of oscillograms.[1] Presentation in that manner was believed to be more satisfactory than the already available analytical proof. However, a statement made before the Institute,[1] that inertia is the reason for the relay's practical freedom from vibration, indicates that a reference to the analytical proof is necessary.

The theory governing the operation of the induction-cylinder relay is the same as that of the induction motor, and previous work on induction motors may be used, if it is kept in mind that the standstill equations of the induction motor apply to the relay. Morrill's equation 109[2] shows that, at standstill, all double-frequency-torque components disappear, leaving only a constant-torque term. This is also illustrated by Figure 12 of the same paper.

References

1. Discussion by A. J. McConnell and author's closure by Bert V. Hoard of An Improved Polyphase Directional Relay. AIEE Transactions, volume 60, 1941, page 682.

2. The Revolving-Field Theory of the Capacitor Motor, Wayne J. Morrill. AIEE Transactions, volume 48, 1929, pages 614–29.

V. N. Stewart: Mr. McConnell suggests that the induction-cylinder balanced-current relay has an inherent characteristic, which gives it an advantage over previous designs. In the protection of parallel lines with different R/X ratios, the improved phase-angle characteristic obtained with this relay does not affect appreciably the slope. For an extreme case, where the two line currents might differ as much as 45 degrees, the 125 per cent slope of this relay is changed four or five per cent.

Although the reference to Mr. Morrill's paper by Mr. McConnell serves as sufficient analytical proof to the relay's freedom from vibration, additional proof has been developed by C. D. Hayward in which he compares the performance of both the induction-cylinder and induction-ring devices. The following paragraphs present his calculations with several conclusions.

Induction-Cylinder Dynamometer Torque

Assume a four-pole induction-cylinder-type dynamometer with coils having N_1 and N_2 turns, respectively, on the poles of the two axes.

For the applied coil currents let

$$i_1 = I_1 \sin(\omega t + \theta_1) - I_1 \sin\theta_1 \epsilon^{-\frac{t}{T_1}} \tag{1}$$

$$I_2 = I_2 \sin(\omega t + \theta_2) - I_2 \sin\theta_2 \epsilon^{-\frac{t}{T_2}} \tag{2}$$

For the currents induced in the cylinder let

${}_ci_1$ = current induced by i_1
${}_ci_2$ = current induced by i_2

Also let

M_1 = mutual-inductance coil 1 to cylinder
M_2 = mutual-inductance coil 2 to cylinder

Coil 1 is considered as having N_1 turns divided equally between the left and right poles in Figure 1 of this discussion, whereas coil 2 has N_2 turns divided equally between the front and back poles.

L_1 and L_2 = self-inductance of the paths followed by the induced-cylinder currents ${}_ci_1$ and ${}_ci_2$.

$$L_1 = \frac{M_1}{N_1} + {}_LL_1 \tag{3}$$

$$L_2 = \frac{M_2}{N_2} + {}_LL_2 \tag{4}$$

${}_LL_1$ and ${}_LL_2$ = leakage inductance of paths followed by ${}_ci_1$ and ${}_ci_2$
R_1 and R_2 = resistance of paths followed by ${}_ci_1$ and ${}_ci_2$

Operational-voltage equations for circuits of induced currents

$$M_1pi_1 - L_1p{}_ci_1 - R_{1c}i_1 = 0$$
$$M_2pi_2 - L_2p{}_ci_2 - R_{2c}i_2 = 0$$

from which

$${}_ci_1 = \frac{M_1p}{L_1p + R_1} i_1 \tag{5}$$

$${}_ci_2 = \frac{M_2p}{L_2p + R_2} i_2 \tag{6}$$

These are both of the same form. The corresponding indicial admittance for either may be obtained from[1]

$$A(t)=\frac{Mp}{Lp+R}1=\frac{M}{L}\frac{p}{p+\frac{R}{L}}1=\frac{M}{L}\epsilon^{-\frac{R}{L}t} \qquad (7)$$

The applied currents, equations 1 and 2, are both of the general form

$$i(t)=\underbrace{I\sin(\omega t+\theta)}_{\substack{\text{steady-state}\\ \text{a-c term}}}-\underbrace{I\sin\theta\epsilon^{-\frac{t}{T}}}_{\substack{\text{transient}\\ \text{d-c term}}} \qquad (8)$$

The steady-state term will induce both steady-state and transient currents in the cylinder. These are calculated conveniently by use of a special form of the superposition theorem[2] for an applied force of the form $I\sin(\omega t+\theta)$.

$$i_s(t)=I\omega\cos(\omega t+\theta)\int_0^\infty\cos\omega\lambda A(\lambda)d\lambda+$$
$$I\omega\sin(\omega t+\theta)\int^\infty\sin\omega\lambda A(\lambda)d\lambda \qquad (9)$$

$$i_t(t)=I\sin\theta A(t)-I\omega\times$$
$$\cos(\omega t+\theta)\int_t^\infty\cos\omega\lambda A(\lambda)d\lambda-I\omega\times$$
$$\sin(\omega t+\theta)\int_t^\infty\sin\omega\lambda A(\lambda)d\lambda \qquad (10)$$

If the value of $A(t)$ is substituted for the circuit from equation 7 and solved, we obtain

$${}_ci_s(t)=I\frac{M}{L}\sin\alpha\cos(\omega t+\theta-\alpha) \qquad (11)$$

$${}_ci_t(t)=I\frac{M}{L}\sin\theta\epsilon^{-\frac{R}{L}t}-I\frac{M}{L}\times$$
$$\sin\alpha\cos(\theta-\alpha)\epsilon^{-\frac{R}{L}t} \qquad (12)$$

where

$$\left.\begin{aligned}\alpha&=\tan^{-1}\frac{\omega L}{R}\\ Z&=\sqrt{R^2+\omega^2L^2}\end{aligned}\right\}$$

The phase angle and impedance of the path of the induced current in the cylinder with the coils on the poles open-circuited.

The transient term of the applied current of equation 8 will induce several kinds of transient currents in the cylinder. These are calculated readily from the general form of the superposition theorem[3]

$${}_ci(t)=i(0)A(t)+\int_0^t A(t-\lambda)i'(\lambda)d\lambda \qquad (13)$$

If the transient term, $I\sin\theta\epsilon^{\frac{-t}{T}}$ is substituted from equation 8 for $i(t)$ and the indicial admittance,

$$A(t)=\frac{M}{L}\epsilon^{-\frac{R}{L}t}$$

from equation 7, we obtain

$${}_ci_D(t)=I\sin\theta\frac{M}{L}\epsilon^{-\frac{R}{L}t}+$$
$$\int_0^t\frac{M}{L}\epsilon^{-\frac{R}{L}(t-\lambda)}I\sin\theta\left(-\frac{1}{T}\right)\epsilon^{-\frac{\lambda}{T}}d\lambda$$

and, if the integration is carried out

$${}_ci_D(t)=I\frac{M}{L}\sin\theta\epsilon^{-\frac{R}{L}t}+I\frac{M}{L}\times$$
$$\sin\theta\frac{\left(\epsilon^{-\frac{t}{T}}-\epsilon^{-\frac{R}{L}t}\right)}{1-\frac{R}{L}T} \qquad (14)$$

If equations 11 and 12 are added, and equation 14 is subtracted, we obtain an expression for the total current induced in the cylinder caused by the applied coil current, equation 8

$${}_ci(t)=I\frac{M}{L}\sin\alpha\cos(\omega t+\theta-\alpha)-I\frac{M}{L}\times$$
$$\left[\sin\alpha\cos(\theta-\alpha)-\frac{\sin\theta}{\left(1-\frac{R}{L}T\right)}\right]\epsilon^{-\frac{R}{L}t}-$$
$$I\frac{M}{L}\frac{\sin\theta}{\left(1-\frac{R}{L}T\right)}\epsilon^{-\frac{t}{T}} \qquad (15)$$

Corresponding with the two applied coil currents, equations 1 and 2, the two induced currents will be

$${}_ci_1=I_1\frac{M_1}{L_1}\sin\alpha_1\cos(\omega t+\theta_1-\alpha_1)-I_1\frac{M_1}{L_1}\times$$
$$\left[\sin\alpha_1\cos(\theta_1-\alpha_1)-\frac{\sin\theta_1}{\left(1-\frac{R_1}{L_1}T_1\right)}\right]\epsilon^{-\frac{R_1}{L_1}t}-$$
$$I_1\frac{M_1}{L_1}\frac{\sin\theta_1}{\left(1-\frac{R_1}{L_1}T_1\right)}\epsilon^{-\frac{t}{T_1}} \qquad (16)$$

$${}_ci_2=I_2\frac{M_2}{L_2}\sin\alpha_2\cos(\omega t+\theta_2-\alpha_2)-I_2\frac{M_2}{L_2}\times$$
$$\left[\sin\alpha_2\cos(\theta_2-\alpha_2)-\frac{\sin\theta_2}{\left(1-\frac{R_2}{L_2}T_2\right)}\right]\epsilon^{-\frac{R_2}{L_2}t}-$$
$$I_2\frac{M_2}{L_2}\frac{\sin\theta_2}{\left(1-\frac{R_2}{L_2}T_2\right)}\epsilon^{-\frac{t}{T_2}} \qquad (17)$$

The mutual flux,* common to the exciting coil and the path of the induced current in the cylinder, may be calculated by use of the relationship

$$\phi=\frac{4\pi}{10}(\text{net ampere turns})(\text{permeance of the mutual magnetic circuit})$$

but $\frac{4\pi}{10}(\text{permeance})=\frac{M}{N}10^8$

Hence, since the net ampere turns is the ampere turns of the exciting coil less the ampere turns of the induced current in the cylinder

$$\phi_1=(i_1N_1-{}_ci_1)\frac{M_1}{N_1}10^8 \qquad (18)$$

$$\phi_2=(i_2N_2-{}_ci_2)\frac{M_2}{N_2}10^8 \qquad (19)$$

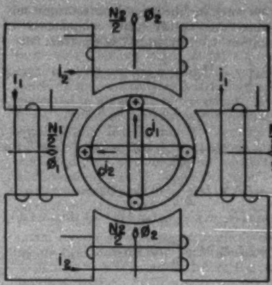


Figure 1

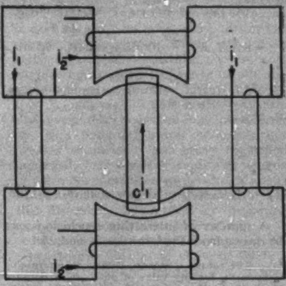


Figure 2

There will be two torques on the relay, which, when clockwise rotation in Figure 1 is considered as positive, are as follows

$$T_{12}=+{}_ci_1\cdot\phi_2\cdot\frac{2l_1r_1}{a_2}\text{ dyne centimeter} \qquad (20)$$

$$T_{21}=-{}_ci_2\cdot\phi_1\cdot\frac{2l_2r_2}{a_1}\text{ dyne centimeter} \qquad (21)$$

where

l = length of induced-current path in field of mutual flux—centimeters
r = radius of cylinder—centimeters
a = area of pole face through which mutual flux passes—square centimeters

The subscripts 1 and 2 refer to values associated with the two pole axes.

The total torque is the sum of these two torques. Hence, substituting equations 18 and 19 in equations 20 and 21, and adding

$$T={}_ci_1(i_2N_2-{}_ci_2)\frac{M_2}{N_2}\cdot\frac{2l_2r_2}{a_2}10^8-$$
$${}_ci_2(i_1N_1-{}_ci_1)\frac{M_1}{N_1}\cdot\frac{2l_1r_1}{a_1}10^8 \qquad (22)$$

If we assume the dynamometer is mechanically symmetrical

$l_1=l_2 \quad R_1=R_2 \quad \frac{M_1}{N_1}=\frac{M_2}{N_2}=\frac{4\pi}{10}\mathcal{P}$
$r_1=r_2 \quad L_1=L_2 \quad \mathcal{P}$ = permeance of mutual flux circuit
$a_1=a_2 \quad \alpha_1=\alpha_2$

$$T=({}_ci_1\cdot i_2N_2-{}_ci_2\cdot i_1N_1)\frac{4\pi}{10}\mathcal{P}\frac{2lr}{a}10^8 \qquad (23)$$

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* There will also be leakage fluxes, but these need not be considered, since it can be shown that they do not produce any net torque on the cylinder.

To solve for the steady-state torque substitute the steady-state components of equations 1, 2, 16, and 17 in equation 23

$$T_s=\left[I_1N_1\frac{4\pi}{10}\mathcal{P}\frac{1}{L}I_2N_2\sin\alpha\cos(\omega t+\theta_1-\alpha)\times\right.$$

$$\sin(\omega t+\theta_2)-I_2N_2\frac{4\pi}{10}\mathcal{P}\frac{1}{L}I_1N_1\times$$

$$\left.\sin\alpha\cos(\omega t+\theta_2-\alpha)\sin(\omega t+\theta_1)\right]\cdot\frac{4\pi}{10}\mathcal{P}\frac{2lr}{a}10^{8}$$

$$T_s=I_1I_2M_1M_2\frac{1}{L}\frac{2lr}{a}10^{8}\sin\alpha\cdot[\cos(\omega t+\theta_1-\alpha)\times$$

$$\sin(\omega t+\theta_2)-\cos(\omega t+\theta_2-\alpha)\sin(\omega t+\theta_1)]$$

$$=I_1I_2M_1M_2\frac{1}{L}\frac{2lr}{a}10^{8}\sin\alpha\times$$

$$\frac{1}{2}\left[\sin(2\omega t+\theta_1+\theta_2-\alpha)+\sin(\theta_2-\theta_1+\alpha)-\right.$$

$$\left.\sin(2\omega t+\theta_1+\theta_2-\alpha)-\sin(\theta_1-\theta_2+\alpha)\right]$$

$$=I_1I_2M_1M_2\frac{1}{L}\frac{2lr}{a}10^{8}\sin\alpha\cdot\sin(\theta_2-\theta_1)\cos\alpha$$

$$T_s=I_1I_2M_1M_2\frac{1}{L}\frac{lr}{a}10^{8}\cdot\sin 2\alpha\sin(\theta_2-\theta_1)\quad(24)$$

or, since

$$\frac{\sin\alpha}{L}=\frac{\omega}{Z}$$

$$T_s=I_1I_2M_1M_2\frac{\omega}{Z}\frac{2lr}{a}10^{8}\cdot\cos\alpha\sin(\theta_2-\theta_1)\quad(25)$$

A number of interesting conclusions can be drawn from the formulas 24 and 25:

(*a*). The torque is steady, having no alternating component, for any value of phase angle between the two applied currents or for any relative magnitude of the applied currents.

(*b*). Maximum torque occurs at 90 degrees phase angle between the two applied currents, regardless of the phase angle of the induction cylinder.

(*c*). From equation 25 it is seen that maximum torque occurs for $\alpha=0$. This would require a capacitor in the induction-cylinder circuit to tune the exciting and leakage reactances and, hence, reduce L to zero. From equation 24 it is seen that if the circuit is not tuned, that is, L is not zero, then maximum torque occurs for $\alpha=45$ degrees; L in the denominator should be minimum, hence, leakage reactance should be zero. The cylinder impedance should equal the exciting impedance for maximum power transfer; hence, the cylinder should be pure resistance equal to the exciting reactance $R=\frac{M_1}{N_1}=\frac{M_2}{N_2}$.

INDUCTION-RING DYNAMOMETER

The induction-ring dynamometer in Figure 2 of this discussion is not a symmetrical structure; so, the foregoing results do not apply. An expression for the torque can be obtained from equation 22 by setting ${}_ei_2=0$.

$$T={}_ei_1\cdot i_2\cdot M_2\cdot\frac{2l_2r_2}{a_2}\cdot 10^{8}\qquad(26)$$

which has a steady-state value obtained by substituting the steady-state components of ${}_ei_1$ and i_2 from equations 16 and 2

$$T_s=I_1I_2M_1M_2\cdot\frac{2l_2r_2}{a_2}10^{8}\frac{\sin\alpha_1}{L_1}\times$$

$$\cos(\omega t+\theta_1-\alpha_1)\sin(\omega t+\theta_2)$$

$$T_s=I_1I_2M_1M_2\frac{l_2r_2}{a_2}\frac{\omega}{Z_1}10^{8}\times$$

$$[\underbrace{\sin(2\omega t+\theta_1+\theta_2-\alpha_1)}_{\text{alternating}}+\underbrace{\sin(\theta_2-\theta_1+\alpha_1)}_{\text{steady}}]\qquad(27)$$

From this we can draw the following conclusions:

(*a*). The torque is pulsating, having a double-frequency alternating component which has the same amplitude for any phase angle between the two applied currents. It also has a steady component, which, at the angle of maximum torque, is equal to the crest value of the alternating component. At all other angles the steady component is smaller, so that the torque reverses during part of the cycle.

(*b*). Maximum torque occurs when $\theta_2-\theta_1+\alpha_1=90$ degrees; that is, it occurs when the phase angle between the two applied currents is the complement of the phase angle of the ring.

REFERENCES

1. OPERATIONAL CIRCUIT ANALYSIS, Bush. Appendix C, formula 3.
2. Reference 1, page 71, formulas 107 and 108.
3. Reference 1, page 56, formula 77.

Pilot-Wire Relaying on a Metropolitan System

Discussion and authors' closure of paper 43-79 by T. G. LeClair and E. L. Michelson, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, August section, pages 511–15.

W. K. Sonnemann (Westinghouse Electric and Manufacturing Company, Newark, N. J.): I am very much interested in this straightforward exposition of a pilot-wire system for one reason in particular. This is because I believe the authors have shown again that a well-designed chain has all the links of equal strength. Here they have chosen a pilot-wire channel with electrical and mechanical characteristics of a degree of reliability matched to the reliability of the relays with which it is used. The over-all result is an operating record which is highly satisfactory.

The authors have stated that correct operation of the systems they described depends on a definite division of the secondary currents, which in turn depends upon the relative impedance of the terminal equipment. It is presumed that they had in mind particularly the relays, pilot wires, and resistors. It should be noted, however, that since the relays are not of the percentage-differential type, the relative qualities of the current transformers at both ends of the line enter into the picture. No doubt the current transformers used have adequate capacity, since no trouble from this source has been mentioned. However, for the general application, schemes of this type require that closer attention be paid to the current-transformer characteristics than with fault-current restrained relays, particularly at the higher through-fault currents, and with the higher-resistance pilot-wire circuits.

One of the remote-trip schemes described indicates that contacts are used in the pilot-wire circuit. It seems to me that these contacts should have considerable pressure or wipe or both because of their use in an important, relatively low-voltage low-current a-c circuit.

The statement is made that the pilot wires are checked semiannually as part of the routine relay test. I believe it would be in order if the authors would give us a few words concerning the frequency and nature of the troubles on the pilot wire that are uncovered by this test.

S. C. Leyland (Westinghouse Electric and Manufacturing Company, Newark, N. J.): This paper, by presenting details of a large amount of field experience, has contributed some very worthwhile additions to the information already available on pilot-wire relaying as presented in the relay subcommittee's report "Pilot-Wire Circuits for Protective Relaying—Experience and Practice" at the 1943 winter technical meeting.[1]

The paper states, "The principal advantage of using four pilot wires and three relays instead of two pilot wires and one relay is that the relay target will identify which cable has failed in a transmission line using single-conductor cables." I would like to suggest that to obtain the feature of indicating the phase which is in trouble, a phase selector relay be used. Such a device was presented before the Institute in the Goldsborough-Hill paper, "Relays and Breakers for High-Speed Single-Pole Tripping and Reclosing."[2] By use of such a device, indication of the faulted phase can be obtained readily and the pilot-wire scheme simplified to the extent of the elimination of two pilot wires and two relays at each terminal and the addition of a phase-selector relay at each terminal. It is possible to eliminate one of the phase-selector relays, providing it is permissible to forego the requirement of indicating the faulted phase when the circuit is energized from one end only.

Mention is made in the paper of a method of protecting a transformer bank and a line when no high-voltage breaker is available. This method is to protect the line as a unit and the transformer as a unit and to send a signal over the pilot wire to remote trip for trouble in the transformer bank. There have been installations made where the bank and the line were covered by a single differential pilot-wire relaying scheme. In most cases, it was necessary to provide tripping suppressors to prevent operation of the scheme because of magnetizing inrush currents. In some cases this may not be necessary because of the location and size of the bank limiting the inrush current to a value lower than minimum pickup of the relays.

In reporting the cases of trouble, it is significant that only one case was a result of failure of the terminal equipment, and this was due to a damaged resistor resulting from overload applied during test. The other cases of trouble were with the pilot-wire circuits and are what might be termed "external troubles." In no case did inherent weakness of the pilot-wire scheme contribute to the failure to operate correctly. Since so-called "external troubles" were the cause of the majority of failures in the pilot-wire circuit, it would be interesting to know what percentage of external troubles are included in the 7.2 per cent incorrect operations for relay systems used on the higher voltages.

The statement is made in the paper that the pilot-wire schemes should provide a more reliable form of protection than carrier-current relaying. While this statement, no doubt, is true for the system described, there is some question whether such is the case where all types of pilot-wire channels are considered. A privately owned underground pilot cable, such as is described in this paper, is less subject to trouble than are overhead pilot wires either open wire or cable. The paper also points out that leased telephone circuits have been subject to more outages and for longer times than privately owned pilot circuits. In making a general comparison, therefore, between carrier relaying and pilot relaying, it would be desirable to include all types of pilot-wire circuits. It would also be desirable to make this comparison on a circuit-mile basis since carrier-relaying installations generally are applied to much longer lines than is pilot-wire relaying.

REFERENCES

1. PILOT-WIRE CIRCUITS FOR PROTECTIVE RELAYING—EXPERIENCE AND PRACTICE, AIEE committee on protective devices. AIEE TRANSACTIONS, volume 62, 1943, May section, pages 210–14.

2. RELAYS AND BREAKERS FOR HIGH-SPEED SINGLE-POLE TRIPPING AND RECLOSING, S. L. Goldsborough, A. W. Hill. AIEE TRANSACTIONS, volume 61, 1942, February section, pages 77–81.

Philip Sporn (American Gas and Electric Service Corporation, New York, N. Y.): In their comparison of pilot-wire systems with carrier-current relay systems, the authors state that the pilot-wire systems described in their paper are competitive in cost to approximately 20 miles in length. I do not know how they arrived at their conclusions. For example, they state that the pilot cables used cost up to approximately $1,500 per mile installed exclusive of terminal-relay equipment. On our system where we have approximately 200 carrier-current relay terminals we find that for 132-kv lines the cost per terminal for carrier-current relaying plus backup protection amounts to slightly under $5,000 installed. On this basis the pilot-wire systems are competitive with carrier-current relay systems only on lines less than seven miles in length and on lower voltage systems to line less than six miles in length.

The authors are of the opinion that pilot-wiring relaying systems provide a more reliable form of relay protection than carrier-current relaying. In our repeated and cumulative experience this definitely has not been the case. Pilot-wire relaying is no more reliable than the pilot wire itself, and during severe lightning storms the failure of the pilot wire on overhead lines is very likely to occur just at the time when it is needed most for reliable operation. Not only that, we have found that under severe storm conditions the transmission-line structure will stand up physically when the pilot-wire supports will fail to do so. Of course this would not be the case with underground pilot wires. But the latter is so atypical that one can safely say that, from a reliability standpoint, carrier-current relaying is by far the superior method.

E. L. Harder (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The paper by Mr. LeClair and Mr. Michelson is a particularly valuable contribution to the industry in that it represents experience on a large number of circuits extending over many years. There are a number of interesting points which bear emphasis.

It is noted that the cost of pilot cable in duct as given in the paper is less than the costs usually obtained for aerial cable. This seems paradoxical, as overhead wiring is usually cheaper, but is apparently due to the fact that the duct space is free and the expense of pulling the pilot cable into it is less than the cost of messenger and applying supporting clips on aerial cable.

The idea of setting the protective relays below maximum load current to obtain supervision by tripping in event of pilot-wire outage is a worth-while simplification in those cases where parallel cables make such operation permissible.

In the scheme described in the paper, remote tripping for transformer faults is secured by opening the pilot circuit. This results in tripping when the current entering the far end of the line equals the pilot-wire relay setting. This provides adequate sensitivity on systems where the pilot relays can be set below maximum load currents. However, it appears preferable to trip the remote breaker by a signal sent over the pilot wires in cases where the protective relays are set for larger values.

The conclusion reached by the authors that pilot-wire equipment is competitive with carrier current up to 20 to 35 miles is extremely interesting, because it is a longer distance than generally has been considered the dividing line. The costs upon which these figures are based would be well worth presenting.

The pilot circuit described in this paper meets the problem of induced voltages and station ground potentials entirely by adequate insulation coupled with the fact that these voltages are low in the first place on the particular system under consideration. It would be of interest and value if other papers could be presented describing the experience with pilot-wire equipment on systems where the induced voltages and station ground potentials are larger and must be provided for by suitable protective and drainage equipment. There are many systems where it would be impractical simply to provide the necessary insulation in pilot cables to stand these voltages, and yet on which entirely satisfactory operation is being obtained through the use of proper protective and voltage-gradient equipment, such as neutralizing transformers and insulating transformers.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): This paper presents a very interesting summary on a large number of pilot-wire-relay installations installed on a single system and presumably planned and maintained by one organization. This paper supplements the relay subcommittee report[1] which was presented last winter and is perhaps more interesting because of the more detailed information which is included.

There are a number of points brought out in this paper, some of which I feel deserve further discussion and some of which perhaps require clarification from the authors.

It is very interesting to me to note that on the loop systems it has been considered adequate to provide backup protection at the source ends of the loop and at the middle only. This means that, in case of a failure of some of the protective equipment to operate correctly, service will be interrupted to approximately half of the customers served by the loop. In some cases we find that very elaborate attempts are made to provide a 100 per cent selective backup system which on this installation would require directional overcurrent relays set as indicated in Figure 1a of the paper. It has never seemed to me that we were justified generally in attempting to provide the 100 per cent selective backup since this seems to be "pyramiding of protection" and naturally brings about the next question as to whether another line of protective equipment should be put in, in case the backup fails. It has always seemed to be much more desirable to select these equipments best suited for a particular job and to plan to keep it in correct operating condition by means of an adequate maintenance schedule. While we all can concede that even with these precautions we will never quite achieve 100 per cent correct operation, it does seem to be good sense to put in a simple backup system which will involve neither too much apparatus nor too much engineering and maintenance to keep it operating correctly. Another factor tending to this thought on my part is the realization that every relay, whether first line or backup, is a potential source of unnecessary breaker operation. Too many backup relays easily may result in conditions whereby undesirable operations occur when there are no faults on the system whatsoever. Further discussion of this point by the authors and others should prove interesting.

From the text of the paper it appears that separate pilot cables of three or five conductors are installed for each line. I would like to ask the authors if this practice is always followed, or do they use a single cable with more conductors when they are protecting the parallel power circuit?

I have been interested for many years in the application of the carrier-current form of pilot protection and hence appreciate any information, based on experience, relative to the minimum length of line at which carrier becomes competitive in cost with pilot wires. The authors have given some information along this line which indicates that carrier-current relaying is very costly.

The authors state that the cost of the pilot wire installed is up to approximately $1,500 per mile. If we use this figure for 20-mile lines as indicated, we come out with an installed cost of the pilot wire of $30,000. This neglects the terminal-relay equipment which admittedly would be very small in comparison with the $30,000 figure.

Carrier-relay prices, including the relays, necessary coupling capacitors, and line traps, and the carrier-current equipment itself, are well known today and span the following range. For simple carrier relaying, such as the type *GMB*, for use on 69 kv has a per-terminal price of about $3,750. The more complete relay equipment including the backup function, such as the type *GCX* combination with carrier, for service at 230 kv has a per-terminal price of about $5,500.

Thus we find that the selling price of the equipment to be purchased for the protection of both ends of a line section is between $7,500 and $11,000. Therefore, it appears that an installed cost of between three and four times the purchase price has been used. It would be of considerable value if other discussers would comment on what installation costs are, as indicated by their experience. It has been fairly common to consider carrier-current relaying as being economically competitive above ten miles, and there have been several installations made for shorter distances. The ten-mile figure would indicate an installed cost of $15,000 or an increase in cost of 50 to 100 per cent over the purchase price. This would seem to be at least adequate for average installations. Further discussion of this point should be of considerable help in determining the proper place of pilot-wire and carrier-current relaying.

In passing, it is in order to add that the carrier-current channel offers possibilities for voice or other communication needs not usually available with pilot wire.

Whatever may be said regarding the comparison between pilot wires and carrier or leased telephone circuits and carrier must, of course, rest upon similar economic comparisons, and local conditions will, of course, change these figures somewhat. The authors state that leased circuits on the basis of $60 per mile per year are competitive with carrier up to 35 miles. Here again the important figure is the one covering the installed cost. Then the comparison may be made against the leased rate and, of course, the leased circuits will be economical for longer distances where the rates are lower.

The relay system used by the authors is one of similarity to those commonly used today with the two-wire a-c pilot system. It is, however, somewhat simpler in its conception, but by reason of these simplifications it appears to have two limitations. I do not believe that with the amount of current which is circulated through the pilot-wire system that it could be applied generally over leased telephone circuits. From the information in the paper it would appear that the normal circulating current at normal load is of the general order of 0.5 amperes. I believe that most leased circuits are restricted to currents of 0.3 ampere or less under fault conditions. Furthermore, because of the fact that the relay system operated on a definite amount of unbalance between the terminal currents, it is necessary that current transformers be chosen which not only "match" but which actually do not saturate materially under the maximum through-fault condition. Current transformers which "match" but which are saturated may differ because of variations in the iron, by more than the amount of current required to cause relay operation. However, this limitation to me does not appear to be a very serious one. I could never see any good reason why it is not thoroughly sensible to require that adequate current transformers be put at the two ends of a series circuit to which differential protection is to be applied.

I was very much interested to note the large number of pilot-wire installations and the very appreciable investment which is represented thereby. When we consider the resistance that is sometimes encountered to even the addition of directional relays where necessary I feel that the authors would be doing a great service if they would amplify their paper by lists of the system benefits which have been realized by this improved protection. The relay engineer is apt to be satisfied that pilot-wire protection is fast and selective and that this is an end in itself. In order to justify an expenditure of several hundred thousand dollars over a period of several years there must be some more benefits obtained than merely the knowledge that the best protection possible is being applied. It must be that this best protection is reflected very definitely in improved performance of the system as a whole or in other kinds of saving made possible by this protection in order to justify economically the continued application of this type of equipment.

In conclusion, I wish to congratulate the authors on an interesting paper and also them and their associates on the excellent performance record of the installed relay protective system.

REFERENCE

1. PILOT-WIRE CIRCUITS FOR PROTECTIVE RELAYING—EXPERIENCE AND PRACTICE, AIEE committee on protective devices. AIEE TRANSACTIONS, volume 62, 1943, May section, pages 210–14.

T. G. LeClair and E. L. Michelson: As Mr. Kennedy points out, the backup relaying used for protection of loops is not 100 per cent selective. The primary pilot relaying clears nearly all cases of trouble. Therefore, it does not seem necessary to require 100 per cent selectivity for backup protection, as the number of service interruptions to customers will be extremely small even when selectivity is not complete.

In all cases separate pilot cables are used for each transmission line. This practice avoids the possibility of incorrect operation on a number of lines for one case of trouble on a pilot-wire cable.

The experience gained with the use of pilot-wire relaying has justified its wide application, because of the benefits gained through rapid and selective clearing of short circuits on the system. This rapid and selective clearing of faults has resulted in improved service and reduction in damage to equipment caused by fault conditions.

In regard to the discussion of relative costs of carrier and pilot-wire relaying, there appear to be three factors which explain the variations brought out in the discussions.

1. Type of carrier system employed affects cost to some extent. For example, some carrier systems include complete backup protection while others do not.

2. The amount of equipment included in the estimate varies considerably. For example, housing for the equipment, additional potential devices, and similar items are included in some cases, while in others they are either not required or not charged to the carrier installation.

3. Construction standards vary with different installations. For example, the number of test switches provided for relays vary in different installations.

The discussion of costs has been very helpful in bringing out the experience of various companies in regard to the cost of carrier installations. In the final analysis, each case must be decided on its own merits, which depend on the individual conditions relating to the installation. It is our opinion that in most cases the selection between pilot-wire relaying and carrier relaying is not made primarily on a cost basis. Carrier relaying is more adaptable for long overhead lines where the attenuation of the carrier signal on the open wire is of reasonable magnitude. Pilot-wire relaying is more adaptable on short underground lines, such as used in large cities where the protection of the pilot circuit is inherent because of underground construction.

In regard to Mr. Sonnemann's discussion, the current transformers and other equipment used for pilot-wire relaying are carefully matched. With regard to the contacts used to obtain remote tripping, in most cases these contacts merely short-circuit the pilot wires, so that the possibility of trouble is minimized. The semiannual tests on pilot cables result in the finding of about one case of trouble on approximately 200 pilot cables in service. This trouble generally is caused by moisture entering a cable joint.

The phase-selector relay that Mr. Leyland suggests is an interesting possibility. However, the additional cost of providing four pilot wires and three relays is very nominal. The external troubles which are experienced with all relays on the high-voltage system represent the large majority of relaying troubles experienced. These troubles include such items as loss of potential on impedance relays due to the blowing of fuses, faults on control cables, and so forth.

In regard to Mr. Sporn's comments on the reliability of pilot wires, it appears that his statements are based on the use of open-wire construction. His comparison would not be valid if the pilot wires are installed either in underground or aerial cable. The authors agree that carrier relaying provides a more reliable form of protection than pilot-wire relaying using open-wire pilots.

Distance Relay Protection for Subtransmission Lines Made Economical

Discussion and authors' closure of paper 43-92 by L. J. Audlin and A. R. vanC. Warrington, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 574–8.

J. H. Oliver (General Electric Company, Philadelphia, Pa.): There has been a continuously growing desire for the past five to ten years for a less expensive distance relay for application to less important transmission lines in the range of 11 to 66 kv. In a few cases the importance of these intermediate-voltage lines has warranted the application of high-grade distance relays, but in most cases other solutions have been employed. In general, relay specifications for these lines have the same requirements as for high-voltage transmission lines, namely, good sensitivity, accuracy, and reliability. However, in most cases it has been felt that the relays can be slightly slower in speed; there does not seem to be the same demand for one-cycle relaying as in the high-voltage transmission lines.

The paper by Audlin and Warrington describes a simple solution to this problem. They have met all the requirements by using a relaying combination employing a distance relay designed for high-voltage transmission lines; incorporating its inherent characteristics of good sensitivity, accuracy, and reliability at the sacrifice of only a few cycles time delay, thus reducing the cost of the relay combination, and thereby making it more attractive for intermediate-voltage lines.

E. L. Michelson (Commonwealth Edison Company, Chicago, Ill.): The authors are to be congratulated on their successful application of the switching arrangement which makes possible the use of one distance relay to protect a three-phase line. While this arrangement is more economical than the use of three distance relays, the increased maintenance work required by complicated relay schemes is an important factor. On a large power system the problems and costs created in maintaining special and complicated relay schemes are significant. In the Commonwealth Edison Company this factor has led to a trend of simplifying the schemes of distance relaying, even though additional equipment may be required. For example, in applying the three-step *GCX* relay we have used two separate ohm units instead of the standard arrangement, which consists of an ohm unit and a transfer switch to make it applicable for two steps.

Eric T. B. Gross (Cornell University, Ithaca, N. Y.): The authors are to be congratulated on their courage for diverting from standard practice and on their successful application of the new relay arrangement. The only practical difference between a set of six standard distance relays (three for line-to-line faults and three for line-to-ground faults) and the new distance relay, controlled in its current and potential connections by a selector relay, is the minimum operating time of five cycles. However, as shorter times are necessary at very high voltages only, the new scheme should deserve a wide field of application. In many cases, the circuit-breaker characteristics necessitate the use of relays with much more minimum time than one cycle, and the new scheme will do as well. Furthermore, the application of the new relay is unlimited for backup protection in connection with carrier-current protection.

A critical analysis reveals that distance relays in the six-relay arrangement may operate incorrectly at certain line-to-line faults, whereas the distance relay in the new scheme is always provided by its selector relay with such currents and voltages that the measurement of the impedance to the fault and the direction of the fault current will be correct; this is another advantage of selector relays.

As I am familiar with the development and application of selector relays and relay schemes in continental Europe, it seems to me that I should report on the experiences which were made abroad. Automatic potential switching was used in Europe since the beginning of the application of distance relays, some 20 years ago. This was possible because the relays were and still are—as we know from the literature—in general, slower than ours. The American standard six-relay scheme has hardly ever been used in Europe. Until about 1929 three distance relays were used and each potential coil automatically provided with line-to-line or line-to-ground voltage, depending on the kind of fault. Later, simpler schemes with two distance relays for all kinds of faults were developed, and finally in 1932 a one-distance selector-relay set for interphase and ground faults. These relay schemes have been applied in overhead-wire and underground-cable systems of the largest capacity and up to 66 kv, and the practical experiences in various European countries (for example, 35-kv overhead system near Warsaw, Poland, or the 35-kv underground-cable system in the city of Budapest, Hungary, and many others) and in other foreign countries (for example, South America) were very satisfactory; the success with the three-relay schemes used since 1923 encouraged the development of cheaper and simpler arrangements which were even more satisfactory from the point of view of exact measurement of the impedance or reactance to the point of the fault.

The work done by the authors is a big step forward as so well expressed in the discussions of Mr. Oliver and Mr. Pratt, the importance of which should be underlined. It is hoped and expected that it will pave the way for a wider application of distance protection in all kinds of systems.

M. H. Pratt (Central New York Power Corporation, Syracuse, N. Y.): It would seem appropriate to discuss briefly a specific problem for which the method outlined in the authors' paper furnishes a better solution than has heretofore been available. This problem occurred on the system of the Central New York Power Corporation.

The 44-kv sub- or area transmission system in the Mohawk Valley east of Utica consists of a double-circuit line mostly on wood poles, extending from Washington Street substation in Utica some 25 miles via East Utica, Ilion, Herkimer, and Little Falls (at each of which points the system supplies a distribution substation) to Inghams. The two circuits were formerly broken and connected through breakers to a bus at Herkimer, the halfway point. East Utica and Little Falls had a tap to each circuit through breakers to the station bus, and Ilion was fed back over a single radial circuit from Herkimer bus. Directional overcurrent phase relays were used with instantaneous overcurrent where possible. It is not pertinent to this brief outline to go into the ground-relay scheme which did not constitute our problem in this case, as one wire-to-ground fault operation was satisfactory.

The troubles with the preceding arrangement were basically three:

1. There was a high percentage of double-circuit multiphase faults during electrical storms which, with the connections outlined, gave frequent interruptions to the substations supplied, particularly East Utica and Little Falls and to a lesser extent, Ilion.

2. The long fault-clearing time on phase faults frequently resulted in conductors being burned down.

3. Good selective settings satisfying all operating conditions were impossible.

The problem was how to correct these conditions at the most reasonable cost. Any attempt to lightning-proof the circuits themselves would have been too expensive. Instantaneous overcurrent attachments, as mentioned, had been applied to the extent that this was feasible. It seemed that a change of connections, looping one circuit into East Utica and the other circuit into Ilion, leaving Herkimer bussed as before, and looping a circuit into Little Falls, was a good arrangement, but it was not possible to make this change and expect any improvement with the existing relay equipment. As a matter of fact, the results from a relay standpoint would have been even worse than before since it would have been even harder to approach proper selective operation under all conditions; furthermore, fault-clearing time would not have been improved. This forced the use of distance relays which were accordingly installed, three *GCX* relays being used at each line-breaker terminal. This, of course, was quite expensive but was still the cheapest possible way in which the problem could be solved, and even this expenditure was justified to correct the trouble. Results were entirely satisfactory.

The ability to apply one distance relay in combination with a phase-selector relay instead of three distance relays as described in the authors' paper gives almost all the advantages of full three-relay distance-relay protection; the only difference is that one-cycle relay operation is not attained, but a minimum time of about five cycles is realized. In any case where this difference is of no practical consequence, the two schemes can be considered to give exactly the same result. In the case of the problem just outlined, five cycles is entirely satisfactory, and it would probably be seldom, with the exception of some major transmission lines, that this would not be true. Such a possible alternate distance-relay scheme is only approximately 50 per cent or roughly $150 more per terminal installation than the widely used directional overcurrent arrangement. It would be seldom that the advantages to be gained by distance relaying if the directional overcurrent scheme is at all unsatisfactory, would not justify this small difference. On the other hand, it takes quite a lot of trouble to justify the great cost differential between directional overcurrent protection and a full three-unit distance-relay installation. The scheme outlined, therefore, truly makes distance-relay protection economical in many cases where formerly it could not have been justified. The new scheme has proved itself in service as entirely satisfactory in every way.

It might be well to point out that the distance relay used with the phase-selector scheme is slightly different in detail design from the standard distance relay. In the case of our installations previously described, where we now have one phase-selector installation, we will gradually replace the standard three distance-relay terminals with the phase-selector scheme as we have occasion to need phase *GCX* relaying on future main high-voltage line installations. It would be helpful to have the relays fully interchangeable for the two types of use.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): I feel that Audlin and Warrington have given their paper a title which is apt to convey an erroneous impression. To infer that distance relays are uneconomical for circuits in the subtransmission class is to indicate much closer control of costs than has yet been achieved. Many such circuits are 20 miles or more long and may carry several thousand kilovolt amperes of load. The total investment in transformers, line, circuit breaker, and substation required is so great that a few hundred dollars more or less for relays is relatively insignificant. Hence, where distance-relay performance is required, it should be possible to provide it without undue economic restriction.

All this, however, does not mean that it is not worth while to find less expensive ways of obtaining desirable performance. It is certainly true that many circuits in the subtransmission class can be protected adequately with distance relays having a minimum time of five cycles. And it is also true that improved performance can be obtained by the stepped-time distance characteristic in comparison with graded-time overcurrent protection. Hence, what Audlin and Warrington have done is to describe a relay system which is less expensive than the full distance relay and yet has characteristics adequate for a large field of application.

As indicated in the paper the idea of switching a single distance relay is not new. The authors have taken care of many of the objections which had been made to earlier suggestions by always indicating the same distance regardless of the combination of phases involved in the fault and by using a switching sequence which eliminates the possibility of incorrect operation when the number of phases involved in the fault are changed. The laboratory and field tests which have been made provide proof of the over-all correct indication under variable fault conditions.

W. E. Marter (Duquesne Light Company, Pittsburgh, Pa.): This paper describes laboratory tests made on the *GLA* relay in the General Electric Company laboratory. A description of a test made by Duquesne Light Company on the *GLA* relay might be of interest. In the Pittsburgh district silver relay contacts tarnish rapidly, as a result in part of sulphur fumes in the air caused by the combustion of soft coal. An attempt was made to accelerate this action by sprinkling the contacts of an *HGA* current-transfer relay in the *GLA* relay with powdered sulphur. The relay was then allowed to stand for two weeks, and at the end of this period the contacts were covered with a heavy black coating.

Without removing the powdered sulphur or cleaning the contacts, ohm meter readings were made on both front and back contacts, and comparative readings were also taken on clean contacts of a similar relay with the following results:

	Back Contacts	Front Contacts
Tarnished contacts	0.275 ohm	0.225 ohm
Clean contacts	0.010 ohm	0.010 ohm

The contact resistance had increased, but no coating had been formed which could not be broken down by the low d-c voltage impressed by the ohmmeter.

Low a-c voltage was applied to the contacts, and a current of 0.1 ampere was transferred without difficulty.

This test indicates that even under conditions of severe neglect the *GLA* relay can be expected to transfer current circuits satisfactorily.

An installation of *GLA* and *GCX* ground protection has been made on a 22-kv line on the Duquesne Light Company system. The relay was installed in one of two tie lines between 22-kv feeder stations which had previously been relayed with directional overcurrent ground relays. In an attempt to prevent the cross-feed operations the relay was installed at a point which had previously opened on cross-feed for any feeder fault on the adjacent station bus.

About four hours after the relays had been cut in service a ground fault occurred on the 22-kv winding of a transformer bank at the adjacent station. The fault was correctly cleared, and the line on which the reactance ground relay was installed remained in service. The other tie line which was still equipped with directional overcurrent ground protection tripped as usual.

This installation seems to indicate that these relays can be successfully applied to keep subtransmission lines in service which had previously tripped unnecessarily on cross-feed currents.

S. L. Goldsborough (Westinghouse Electric and Manufacturing Company, Newark, N. J.): A suggestion is made in the title that distance-relay protection for low-voltage transmission lines is made economical, but this point is not very well substantiated in the body of the paper. It is not obvious at all that the elimination of two high-speed distance relays and the substitution of ten auxiliary relays containing some 26 odd contacts would result in a combination substantially as economical as the standard directional-overcurrent-relay schemes. The paper admits that this ideal has not been attained but hints that a compromise was made so that the increase in cost over the directional overcurrent relay is only moderate. It would be interesting to know more definitely the degree of economy which has been allegedly obtained. The compilation of this degree of economy should be based not only upon the cost of the relays but upon the cost of maintenance of the 26 auxiliary contacts.

It is well known that one of the most important advantages of the distance-type relay is its ability to ignore changes in generating capacity and operate correctly over a wide range in capacity. Frequently the range of generating capacity is such that the relays are called upon to operate on currents near to or less than the full-load current. It is to be noted that in both the schemes described apparently this quite often very desirable characteristic of the distance-type relay is eliminated because of the type of phase selector used. If the overcurrent phase selectors are used, it is quite obvious that they must be set safely above full-load current and, therefore, the distance relay is not allowed to operate for fault currents near to or below full-load current. Also this would apply to the schemes using undervoltage fault detectors where fault current is limited by fault resistance, since any safe undervoltage setting would not allow the relays to respond. It appears that for ground faults the scheme described will only work when the system is impedance grounded. Whether or not the elimination of the feature of operation on faults of approximately load magnitude is worth while is a question.

Whether overcurrent or undervoltage phase-selector relays are employed, the degree of sensitivity is definitely limited. This means that in a good many cases the equipment would not be able to respond to ground faults containing even a moderate amount of fault resistance. This is especially true in the case of solidly grounded systems which would generally require the overcurrent type of phase selector. It is obvious that in the case of low generating capacity, where the fault current may be only moderately higher than the load current, it would not require very much fault resistance to reduce the ground current below the sensitivity of the scheme.

The ground-relay selector scheme utilizing undervoltage relays would seem to operate correctly on impedance-grounded systems, for here is a definite drop in voltage on the faulted phase and a definite rise in the unfaulted phases, thus not requiring an undervoltage setting very close to normal voltage. However, on solidly grounded systems, using either overcurrent or undervoltage, it is anticipated that trouble may be experienced because of the flow of fault current on the unfaulted phases during single phase-to-ground faults. On systems having several sources of power, either all of them grounded or only one of them grounded, there is the possibility that all three phase wires will carry fault current on single-phase grounds. It can frequently happen that the fault current on the unfaulted phases is as large as the fault current on the faulted phase. Under this condition, all three current phase-selector elements would pickup and lock out, thus preventing the distance relay from functioning.

The same condition definitely would also be encountered with the phase-relay selection scheme since it is possible to have fault magnitude of current on all three phases during two-phase-to-ground faults. Since the scheme must be made such that the distance relay is connected to one set of phase current and potentials for three-phase faults, which occasions the operation of the three overcurrent selector elements, it follows that if three selector elements are operated on two-phase-to-ground faults, the distance relay on two of the possible two-phase-to-ground combinations will be connected to the wrong set of voltage and current.

L. J. Audlin and A. R. vanC. Warrington: In his discussion Doctor Gross brought out several interesting points not mentioned in the paper. The success of foreign applications over many years is very encouraging. As regards application to carrier relays, in the present carrier schemes, the directional element of the distance relay is used for both carrier and backup tripping. It would be possible to separate the carrier circuit from the backup circuit by using the distance relays for backup only and, for this purpose, one distance relay with a selector relay usually would be adequate for the backup.

Mr. Marter's data on the behavior of the switching contacts when heavily corroded by contact with sulphur was very encouraging and confirmed our opinion that the switching of current circuits presents no hazard with overlapping high-pressure contacts.

Mr. Goldsborough points out there are 26 contacts in the selector relay; hence it is more complex. In typical impedance and reactance relays there are 12 to 16 contacts per relay. Hence the selector relay has no more contacts than the two relays it is replacing. The contacts of the selector relay, however, have much more pressure than those of the protective relays, because they always have full voltage across their coils whereas the protective relays may be near the balance point.

The number of contacts in any case has little to do with the reliability of the device. The distance-carrier relay equipment has 50 to 60 contacts but is still well thought of. The real factor is contact pressure. All the relay units described in this paper have exceptionally high contact pressure.

The preceding comments also apply to Mr. Michelson's preference for two ohm units in the GCX relay. We believe that relays with high-pressure contacts require less maintenance and are more reliable than protective relays; hence it is advantageous to replace the protective relays by simple auxiliary relays where possible.

Mr. Goldsborough is correct in saying that overcurrent selectors limit the scheme to faults above load current. The first paragraph of the paper explained that these relays were applicable to subtransmission lines hitherto protected by directional overcurrent relays, which obviously must be set above load current. For applications outside this category the standard arrangement of three distance relays is recommended.

We are indebted to Mr. Pratt for providing a good illustration of a typical application for these relays and to all the discussers for providing so many helpful comments.

A letter has just been received from the Electricity Commission of South Africa which contains the following sentence: "We have had nearly ten years of experience with the original *GAX* relays and are very satisfied with their performance so that the soundness of the principles of switching potential and current has been amply demonstrated for us in the field."

Radio-Noise Filters Applied to Aircraft

Discussion and authors' closure of paper 43-132 by C. W. Frick and S. W. Zimmerman, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 590–5.

R. H. Kaufmann (General Electric Company, Schenectady, N. Y.): It is only the final over-all result that is significant in judging the performance of a particular system of radio-interference suppression. Quiet operation of radio-receiving equipment is the objective. The presence of radio-interference voltages on electric circuits is not in itself objectionable and becomes so only if these voltages are communicated to the input circuits of radio receivers.

In all-metal planes, the metallic surfaces comprising the ship skin and sectionalizing walls constitute electrical conducting surfaces which may be used to good advantage in the solution of a radio-interference problem. These metallic surfaces possess the following useful qualities:

1. The exterior metallic skin tremendously reduces external radiation of radio-frequency energy from internal electric circuits containing radio-interference voltages and thus minimizes the induction of interference voltages directly in the radio antenna.

2. In the form of bulkheads, the metallic surfaces constitute an effective stop against radiant radio interference. (Insulated electrical conductors which pass through such barriers provide channels on which radio-interference voltages may be transferred to the opposite side of the bulkhead.)

3. Since electrical conductors possessing radio-interference voltages are run in close proximity to metallic surfaces of the plane, the radio-frequency energy is to a large extent concentrated in the small pace between the conductor and the metallic surface, and radio-interference field strength is elsewhere diminished.

Emphasis should be given to the full utilization of beneficial qualities of the metallic surfaces comprising the plane structure. Full advantage should be taken of the possible shielding effect of metal surfaces in screening the lead-in wire to the receiver from surrounding power-circuit conductors. Power-circuit-conductor runs should be kept as far removed from the radio receiving equipment as is practical, and preferably run in close proximity to metallic surfaces, particularly in the immediate vicinity of radio receiving equipment. As has been pointed out in the paper, the importance of a short direct connection between the receiver chassis and a neutral plane of radio-interference voltage, represented by the main body of the ship structure, cannot be overemphasized.

Consideration should next be given to the application of high-quality high-reduction filters effectively placed in power feeder circuits to the radio receiver. Consideration should next be given to effective filter application in nearby circuits and equipment until the desired noise-reduction ratio at the receiver is obtained. It is significant to note that a few high-quality high-reduction filters employed in the vicinity of radio receiving equipment may well produce far more noise reduction than an equal weight of smaller less effective filters scattered throughout the electrical system.

In summary:

1. The suppression of radio-interference voltage is of no significance unless such voltage were otherwise being communicated to the radio-receiver input circuit.

2. The inherent shielding effect of metallic surfaces should be utilised to best possible advantage in diminishing radio-interference voltages delivered to radio receiving equipment, as beneficial results so obtained involve no extra weight or additional devices.

3. It should be borne in mind that a few high-quality high-reduction filters properly applied in the immediate vicinity of radio receiving equipment may be far more effective than an equal-weight investment in numerous filters distributed throughout the ship.

F. O. Stebbins (General Electric Company, Schenectady, N. Y.): The excellent paper by Frick and Zimmerman describes the radio-noise problem on aircraft, one method for attacking this problem, and the final solution that was obtained. The important feature of this solution is the development of a line of small compact lightweight filters for the suppression of radio noise on aircraft.

Aircraft fall, in general, into three classifications as regards the radio-noise problem:

1. There is the all-metal airplane in which the enclosing structure is entirely of metal and the radio antenna (except for the antenna lead-in wire) is well shielded from radio noise developed inside of the airplane structure.

2. There is the airplane in which the enclosing structure is only partially of metal. With these aircraft, the nonmetallic portion covers the upper part of the structure and since the radio antenna is generally located above the airplane structure, the antenna is not well shielded from radio noise developed inside the structure.

3. With the third type of airplane the enclosing structure is entirely nonmetallic, and the radio antenna is again not well shielded from the radio noise inside the structure.

Since one important feature controlling the performance of radio equipment on the airplane is the amount of radio noise on the airplane antenna, the radio-noise problem should be most easily solved on the all-metal aircraft. In many cases the all-metal aircraft is equipped with a larger quantity of airplane accessories which develop high radio-noise levels, and these may counteract to a large extent the beneficial effect of shielding on the antenna.

The specifications covering the permissible radio noise developed by aircraft accessories are generally loose or in many instances do not exist. As a result, the same design of accessory built by two different manufacturers may develop radio-noise levels which differ by as much as 20 to 1 or more. Likewise, the same design of radio receiver built by two different manufacturers may differ widely in sensitivity to radio noise. As the result, radio-noise suppression methods which give successful radio communication on one airplane may fail completely to give successful radio communication on a second airplane of the same design. The radio-noise problem on aircraft has been solved only when successful radio communication could be maintained in flight. It has been found in certain radio-noise investigations that tests had to be carried forward on six or more airplanes of the same design simultaneously in order to be assured that the solution to the radio-noise problem which was obtained would apply to the entire production of that design. If the airplane manufacturers would specify to the accessory manufacturers the radio-noise levels which the accessory must meet, the radio-noise problem would be greatly simplified.

C. W. Frick and S. W. Zimmerman: The authors appreciate the discussions submitted by Mr. Kaufmann and Mr. Stebbins and are pleased to note that each gives consideration to interference control through both design of aircraft wiring and the use of high-quality filters to localize the sources of noise. Both are necessary.

As explained in the introduction, our paper deals with the control of radio interference through the reducing or suppressing of radio-influence voltage in the power circuit. This is accomplished by means of filters which serve to localize the effects of radio-influence voltages at their source. This method of interference control can be employed readily in cases where other methods such as shielding and aircraft wiring arrangements cannot be used successfully. It can be easily applied since a line of lightweight filters of high-noise-reduction ratio is available for ready application.

Aircraft Contactors

Discussion and authors' closure of paper 43-131 by F. J. Russell and A. P. Charbonneau, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, September section, pages 563–6.

M. J. DeLerno (General Electric Company, Schenectady, N. Y.): Two factors which were mentioned in the introduction of the authors' paper are quite important in aircraft contactor design. These are:

1. Changes in barometric pressure from 30 inches of mercury at sea level to about 5.54 inches at 40,000 feet.
2. Ambient temperatures from −50 to 200 degrees Fahrenheit.

The first factor, which is the more important, has a pronounced effect on the arc-interrupting ability of a contactor. Contact tips, which may satisfactorily interrupt a circuit at sea level, may fail at 40,000 feet. Observations on arcs at sea level and at low pressures seem to show that the contact area of the arc with the tips increases as the pressure is decreased. This effect is not noticeable at pressures slightly below atmospheric but becomes appreciable at pressures corresponding to 45,000 feet. It has been observed also that the color of the arc changes somewhat at the lower pressures. A possible explanation for these observations may be that the arc characteristics possessed by the discharge at sea level change to glow characteristics at low pressures, and the transition stage from one to the other occurs at pressures around five inches of mercury or at 42,000 feet elevation. It would be interesting to know if the authors have observed similar phenomena, and if they have found that tip surface area affects arc interruption to a greater extent at 40,000 feet than at sea level.

A second effect brought about by the reduction in pressure at high altitudes is the decrease in the high-potential-breakdown voltages between live parts on a contactor. The AIEE Standard specifies sufficient clearance between live parts to withstand a potential equal to twice-rated volts plus 1,000 volts. Curves showing the air-gap breakdown potential between sphere gaps and between sharp edges indicate that for a given gap the breakdown potential at sea level is from three to four times as great as at 45,000 feet. Similarly, this ratio for creepage breakdown along the surface of an insulating material ranges from about 2.5 to 4, depending on the material.

The reduced pressure of the air at high elevations decreases the heat dissipation of the magnet coil and the contact tips because of the lowered convection coefficient. The component of the heat dissipation caused by radiation is unaffected by pressure changes. However, the total lowered dissipation at high altitudes is offset by the lowered temperatures present at these altitudes.

F. J. Russell and A. P. Charbonneau: Mr. DeLerno's observations agree substantially with our own experience. Tests under reduced pressure equivalent to that obtained at altitudes up to 55,000 feet indicate greater tendency for arcs to persist with the arc taking on the characteristics of glow discharges at the lower pressures.

The tendency toward glow discharge reduces the breakdown voltage between live parts through air or across the surface of insulating materials.

These factors are taken into consideration in the design of the aircraft contactors, and the results are verified by actual tests at pressures corresponding to the maximum altitude at which the apparatus is to be used.

Arc-Furnace Control by Regulex Exciters

Discussion and authors' closure of paper 43-95 by T. B. Montgomery and T. H. Bloodworth, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, October section, pages 650–4.

C. E. Valentine (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It is stated in Montgomery's and Bloodworth's paper, "For a number of years prior to the present urgency for the production of high-quality steel, the development in control methods for electric-arc furnaces remained substantially stationary in this country." So far as the fundamental principle of measurement is concerned, this is true, but control methods include considerably more. As a matter of fact the fundamental method of measurement of the quantity to be controlled is the same as that which the Westinghouse company developed and has used successfully for some 20 years. This is the comparison or balance between the current flowing in the furnace electrode and the voltage between the electrode and the shell of the furnace. The circuits and apparatus, however, by means of which this principle of measurement is used and applied to the regulation of the furnace, have not remained at all stationary. Several years ago the master-electrode motor-control switch was made a part of the standard regulator equipment. This switch and its circuits provide for simultaneous rapid lowering and withdrawal of the electrodes, thus saving time at the beginning and completion of the melting operation. At the same time improvement was made in the arrangement of panel-mounted apparatus and also in detailed parts, such as the reversing switches. These now are spring-mounted in place of pin and sleeve bearings, thus wear and parts which ultimately require replacement are eliminated. More recently development has been started and is still progressing to provide spring and contact assemblies having longer life and requiring less maintenance.

With regard to circuit improvements, high-speed withdrawal of the electrodes in case of a cave-in has been provided on several installations. This is accomplished by means of an overcurrent relay and auxiliary contactor which increase the speed of the electrode motor. This minimizes the duration of high-current surges on the supply system and also lessens outages and loss of time which might otherwise occur because of tripping of the furnace breaker. The use of an electrically operated tap changer is another important improvement which has contributed to saving time in the operation of the furnace.

Reference is made by the authors to the important items of "sensitivity" and "stability." In general, any regulating device which is relatively insensitive requires no special stabilizing or antihunting means. However, as the sensitivity of a regulating device is increased, and, if high-speed action is secured at the same time, some special means of stabilizing is required usually. The balanced-beam contact-making arc-furnace regulator can be set readily for maximum or for minimum sensitivity by means of the contact gaps. Rotating-type regulating equipment can be made with proper design features to have a similar sensitivity, but the maximum sensitivity is fixed by the design of the regulating machine. It is our experience that, if maximum sensitivity and speed of operation of the rotating-type regulator are to be secured, some means for stabilizing it is required. It would be of interest to have the authors explain by what means the Regulex exciter is stabilized when it is adjusted for maximum sensitivity.

Having both types of regulators available, we have made tests comparing the performance of the balanced-beam type with the Rototrol on the same furnace, switching from one regulator to the other. In these tests the standard-design balanced-beam-type regulator was used with no special circuits for fast withdrawal of the electrode under cave-in conditions. In fact it was used as found after operation for about one year. As a result of these tests we found a slight over-all saving in time, when the rotating-type regulator was used, because of fewer interruptions of the a-c circuit by tripping of the furnace breaker. On the other hand, the rotating-type regulator showed little or no improvement over the balanced-beam-type during the refining period. Otherwise the performance of the two types of equipment was very much the same.

To summarize, either the balanced-beam constant-control voltage-type regulator or the Rototrol variable-voltage-type regulator can be used with assurance of satisfactory results. With regard to maintenance, the cost of reversing switch contacts and the time to replace them is very small. In the rotating-type regulator, commutator and brush maintenance take the place of contact maintenance. Should trouble develop in the rotating-type regulator or generator, the cost in money and time required for repair or replacement would be much greater than replacement of contacts on the older-type equipment. The over-all cost of maintenance remains to be determined on the basis of several years of service.

It should be noted that the machines and auxiliary equipment which comprise the rotating-type regulator require inherently more material and cost than the balanced-beam-type regulator. When initial cost and cost of maintenance are considered, it may be found that each type of equipment has its place, depending on the requirements of a given application.

G. E. Shaad (nonmember, General Electric Company, Schenectady, N. Y.): The paper by Montgomery and Bloodworth presents data on arc-furnace control by rotating exciters used as amplifiers of small control indications.

Amplification and speed of response are the two fundamental characteristics of any amplifier. Another type of rotating exciter successfully used in arc-furnace control and other applications gives an interesting emphasis to the range of amplification ratios and speeds of response which can be obtained in machines of this type.

The Amplidyne compresses two full-size exciters into one and gives two-stage performance by the use of armature excitation achieved by short-circuiting the output of the first stage and adding two additional brushes to collect the output of the second stage.

Amplifications of the order of 10,000/1 or higher and speeds of response of the order of one tenth of a second or less can be obtained. Worthy of note is the feature that leakage flux emanating from the armature gives added speed to the Amplidyne, since this flux closes through the air where it can establish itself "with the greatest of ease" and yet can produce useful voltage.

What is the specific value of this faster speed of response and greater amplification ratio in the control of arc furnaces? They mean the ability to respond to smaller departures from the ideal conditions and the ability to respond to faster and shorter changes throughout the furnace cycle. They mean the reduction of the number of machines required in the motor generator set as illustrated in Figure 6 of the paper, since the Amplidyne exciter is at the same time the Amplidyne generator, so that one machine gives two-stage performance.

The results obtained in arc-furnace operation with the Amplidyne control have been remarkable. In one installation the time per heat was cut ten per cent; at the same time the power consumption per ton was reduced.

B. M. Jones (Duquesne Light Company, Pittsburgh, Pa.): I was very interested in the Montgomery and Bloodworth paper. Although we have fair controls on electric-arc furnaces now, compared with what they were a number of years ago, we as engineers and technicians should not be satisfied and should be exploring means of improvement.

In this particular case I believe that the objective is to strike the arc, hold it, and never let it go out; thus, the time to melt down and, hence the cost would be reduced. This also will increase production.

I understand that some of these regulators have been in service for a year or two and have been giving satisfactory performance, and I hope the good work of improving them continues. As a power-company man, I do not care how large the load, so long as it is steady. If it is a fluctuating load I am concerned, even though it may not be large. Therefore, any attempt to steady the load certainly is interesting, for with this kind of load we can provide smaller lines at less expense to the customer than for a fluctuating load which will require a larger line.

E. W. Henderson (English Electric Company of Canada, Limited, St. Catharines, Ont., Can.): Arc-furnace control, as presented in papers by T. B. Montgomery and T. H. Bloodworth and by R. A. Geiselman, C. C. Levy, and W. R. Harris, is concerned with the action of the furnace itself or the circuit between the furnace transformers and the arc furnaces.

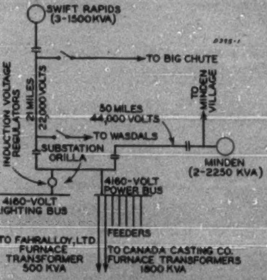

Figure 1

Figure 2

I would like to present a few oscillograms to show how arc-furnace transients affect a power-supply circuit and how in one case the effect of these transients on the lighting circuits was controlled.

The use of a synchronous condenser in parallel with an arc-furnace load is not new; howbeit, the number of installations are few, and these few are installations of fairly large units, for example, 10,000 to 20,000 kva. The system I shall describe is rather small, and the arc-furnace load formed a relatively large proportion of its total capacity. It was located in an Ontario town which has its privately owned utility.

Figure 1 of this discussion shows the original layout, and from this it may be noted that the furnace load of 2,300 kva formed 25.6 per cent of the total kilovolt-amperes of generating equipment, the latter of which totalled 9,000 kva. The power bus was regulated only through the automatic regulators at the generating stations located 50 and 21 miles from the station. The lighting bus was regulated at the distributing station in town by means of automatic motor-operated induction-type regulators. Trouble existed in that these latter regulators were inherently too slow to follow the transients reflected from the arc-furnace loads with consequent objectionable and irritating flicker of varying intensity occurring at irregular and intermittent frequency.

Some idea of the transients which are set up in the power-supply system-feeding arc furnaces may be obtained by an examination of Figure 2 of this discussion. These oscillograms were made with the bottom traces blanked off by a shield mounted within the oscillograph to permit the use of wider trace without interference on the film. The upper trace is that of the voltage variations in the regulated lighting bus caused by the arc-furnace transients shown by the trace immediately under it. The sudden and sustained voltage variations are very marked. Variations in the voltage of the power bus are indicated in the lower trace.

Figure 3 of this discussion shows the circuits as regrouped, with the addition of a 2,000-kva 4,000-volt three-phase 60-cycle 1,200-rpm synchronous condenser in parallel with the furnace loads, and with a buffer reactor inserted between this condenser-arc-furnace circuit and the rest of the system. The condenser was added for several reasons:

(*a*). To control the flicker in the lighting circuits.

(*b*). To improve the power factor of the arc-furnace load and the system as a whole.

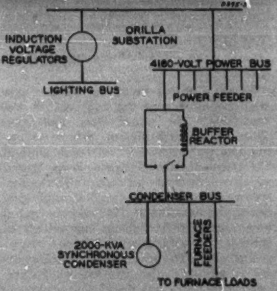

Figure 3

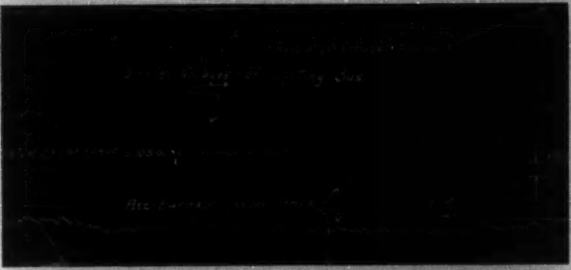

Figure 4 (above)

Figure 5 (below)

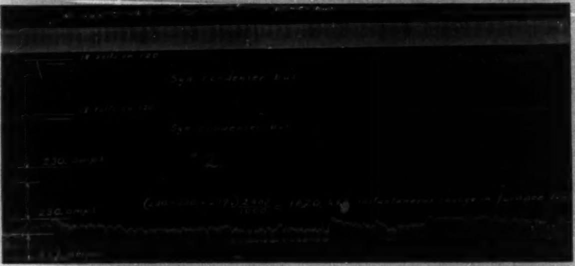

Figure 6

(*c*). To maintain a higher and more steady voltage at the furnaces.

(*d*). To permit additional load to be connected to the system by the power-factor correction.

Figure 4 of this discussion indicates how well the voltage of the lighting bus was controlled. The upper three traces are those of the lighting-bus circuits, showing practically a steady voltage in spite of the transients shown in the lower traces, which are those of the currents in the arc-furnace feeders. The oscillograms were taken simultaneously. It may be noted how steady is the lighting-bus voltage in spite of the arc-furnace transients.

The quick response of the synchronous condenser to transient conditions in the furnace feeders is illustrated in Figure 5. The upper three traces are those of the currents in the synchronous-condenser circuit, and the lower three are those of the currents in the combined arc-furnace load. If we number from the top, 1 corresponds to the same phase as 5, 2 to 6, and 3 to 4. An examination of these pairs will show how rapidly the synchronous-condenser currents respond to those of the arc furnaces. Single cycles are reflected in many cases, and general contour is the same in the different pairs.

The variation in the condenser voltage bus and the steady voltage of the power bus ahead of the reactor under conditions of breaking-down furnace loads are shown in Figure 6 of this discussion. This oscillogram indicates how well the voltage is maintained on the furnaces in spite of rapid and large transients inherent in such arc-furnace operations.

The combined effects of the control of the arc-furnace transients by this method resulted in:

1. Steady voltage with no flicker on the lighting circuits.
2. Steadier voltage on the general power bus.
3. Steadier and higher voltages on the arc-furnace circuits.
4. Extra heats per day on the larger furnace.
5. Reduced time for the heating of annealing furnaces on these arc-furnace circuits.
6. General improvement in power factor and operation of the whole system.

The methods of control by such means as the Rototrol, Regulex, or Amplidyne may result in improved control of the movement of the electrodes themselves, but any apparatus of this type cannot control the transients in these circuits as their response is inherently too slow. This discussion has been given simply to indicate how rapid and effective these troublesome transients may be and one method of successfully offsetting their reaction on the power-supply system. A synchronous condenser for such purposes must have inherently low transient and subtransient reactances, but otherwise is of normal design and is started and controlled simply and effectively.

T. B. Montgomery and **T. H. Bloodworth:** The point of stabilization of the control circuit when it is adjusted to maximum sensitivity was raised by Mr. Valentine. The system is inherently more stable than a contact-making system because that response is, for practical purposes at all times proportional to the deviation from normal, for the arc watts. Also, stability is a function of both sensitivity and speed of response. In the Regulex system both sensitivity and speed of response are adjustable to suit inertia constants of the furnace. External antihunt means are seldom required. Such means are available but, it was thought, have been used too seldom to warrant inclusion in the paper.

As to first cost and maintenance in comparison with other type of regulators, it appears that over-all power savings will be a determining factor.

The savings in time per heat, as well as power consumption given in Mr. Shaad's discussion, is of particular interest. In the system described the adjustment, both of sensitivity and ratio of amplification, has proved useful, particularly in stability of the system. The use of standard machines is considered of advantage in steel plants and foundries.

With reference to Mr. Jones's discussion, data of system disturbance caused by each, we believe, would be of value by way of comparing the new rotating regulators with the contact-making types. The experience so far emphasizes the fact that, with the new regulators, the arc is not broken normally except to shut down for tap changing or other operations. Since arc interruption causes most disturbance, a steadier load may be expected.

Rotating Regulator for Arc Furnaces

Discussion and authors' closure of paper 43-96 by R. A. Geiselman, C. C. Levy, and W. R. Harris, presented at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 671–4.

B. M. Jones (Duquesne Light Company, Pittsburgh, Pa.): Without casting reflections on the regulator controls that have been in service for a number of years, I believe that Geiselman and Levy have presented data to show considerable advancement in this important method of maintaining a more stable condition in the furnace during the early meltdown period, by holding the arc and preventing its extinction, to a greater extent than has been experienced in the past.

We have taken actual measurements of the kilowatt-hours consumed in a large modern electric furnace with violent frequently interrupted arcing during the early meltdown, as compared with a steady arcing at a somewhat lower tap setting, and found that, under the latter condition, appreciably more kilowatt-hours were consumed, which actually means more heat being put into the melting of the charge. These tests were made three or four years ago, and we have repeated them on other installations since, and these conditions have been confirmed. We also have discussed this matter with others, operators and furnace builders, and all have reached a similar conclusion. From the point of view of putting more heat into the melting of the charge, I believe that the improvements pointed out by this paper today on this subject are a good forward step.

I was struck particularly by the ruggedness of the regulators described by these authors, and I believe that this will lend itself very readily to the replacement of damaged parts in the usual hurry-up steel-mill manner. The complete replacement of the rotating unit with a duplicate is very attractive.

To repeat my statement—strike the arc, hold it, and never let it go out, and we can all melt steel electrically more satisfactorily and cheaply.

G. E. Shaad (nonmember, General Electric Company, Schenectady, N. Y.): Geiselman and Levy have presented an excellent paper of interest to everyone connected with arc-furnace operation. In the past year a good many rotating regulators have been put into operation and have shown excellent results. The system described by the authors will be a welcome addition.

Variable voltage control of electrode motors is not new, since it has been used in Europe for several years. However, the slow response and low amplification of standard d-c generators left these installations with little advantage over magnetic control. The development of the present-day highly sensitive generators with nearly instantaneous response has provided a control showing some remarkable savings in time and power.

To obtain the best results from these regulators we need a high sensitivity and the fastest response it is possible to obtain. As the authors have pointed out, the acceleration characteristics of the electrode motors must be considered, but this limiting value of current should be reached as fast as possible.

In our use of the Amplidyne, with its high amplification built into one machine, we have a unit with a nearly instantaneous response to small regulating impulses. At the same time it has a drooping characteristic that helps prevent injurious overloads to the electrode motor. In this way the electrode motor is not a limitation until large changes in furnace conditions are encountered.

Individual furnaces will have individual characteristics, even though they are the same size and make. It is important that the control be standardized but possess sufficient flexibility, so that it can be adjusted to give maximum performance on any furnace. Stabilization must be provided, so that no hunting can occur, there must be no residual voltage to cause creeping of the electrode motors, and a number of other small items that help save time and increase production.

It might be well to consider the possible savings that may be realized from improved control. On a small furnace producing 25,000 tons of steel a year, these new regulators have shown a saving of 50 kilowatt-hours per ton or with five-mill power a saving of over $6,000 a year. Should we take into account the increased production (ten per cent less time per heat), the figure mounts a good deal higher. Such figures are not proved and probably are exaggerated, but, even though no quantitative data are available, the rotating regulator offers tremendous possibilities.

C. C. Levy (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Claims that have been made for the rotating-type of regulator state that it will result in considerable savings in time per heat. Thus, production is increased if this regulator is compared with balanced-beam or other types of arc-furnace regulators.

Obviously, there are many factors which influence the time of a heat; only those for which the regulator is responsible should be charged against it.

These may be:

1. Frequent automatic interruptions due to inability to clear cave-ins and short circuits without the necessity of a circuit-breaker operation.
2. Inability to hold the arc consistently, making it necessary to restrike very frequently. This might be due to poor adjustment, and it is well known that a poorly adjusted regulator of any type always will increase the meltdown time.

However, tests have shown that the comparative performance of a properly adjusted balanced-beam regulator and a Rototrol arc-furnace regulator show no great difference in the results obtained. Some savings of time, we find, can be credited to the Rototrol because of fewer interruptions during meltdown and smoother operation, but it is only fair to point out that the inherent high speed of the Rototrol during cave-in and short circuit can be obtained also by special field-weakening motor-control schemes with the balanced-beam regulator. The results during the refining period should be quite comparable.

In regard to savings in kilowatt-hours per ton, the theoretical kilowatt-hour required to melt a ton of steel is approximately 335. Now, for medium-size furnace with balanced-beam regulator, we find that to meltdown the charge approximately 400 kilowatt-hours per ton would be a usual figure. To reduce this by 50 kilowatt-hours per ton appears to us to be too close to the theoretical value when radiation losses are considered.

E. W. Henderson: For discussion, see page 981.

R. A. Geiselman (nonmember), **C. C. Levy,** and **W. R. Harris:** As Mr. Levy has indicated, the Rototrol control system is well known and has been applied to a wide variety of industrial applications. Its application as an arc-furnace regulator is one of the latest of these developments. The Rototrol was applied first to an automatic welding head in 1928 and was arranged to maintain a constant-arc voltage, despite fluctuations in the rate of burn-off due to variations in the welding wire and irregularities in the surface to be welded. This application, which may be considered as a miniature arc furnace, was very successful, and, since this first installation, the advantages of the Rototrol control system have led to its introduction in the simplification and better control of many industrial processes. For instance, it has been applied to

1. Radio City elevators.
2. Reversing planer drives.
3. Electric shovels.
4. Mine sweepers.
5. Skip and mine hoists.
6. Bomb spinners.
7. Machine tools—120/1 range.

I would like to accent the simplicity of the Rototrol and its associated circuits. The Rototrol itself is nothing more than a standard d-c machine with several field windings, and it functions entirely through the interaction of these fields. Other than the several field windings it is exactly the same as a standard d-c machine, and any mill electrician familiar with d-c machines can maintain it. An important advantage of the Rototrol is that it contains no low-resistance paths in the armature circuit which may be affected adversely by brush drop.

I would like to point out also that the adjustments made to the Rototrol during installation do not require further attention. These adjustments are made in the field circuits which are static and contain nothing to get out of adjustment.

The rates of response and the practically infinite number of operating speeds provided by the variable-voltage system are suited admirably for application to arc furnaces. A close examination of the oscillograms presented by Mr. Levy in the paper will show that, during the meltdown period where the unbalances may be large, the response is at a high rate and high operating speeds are reached, which results in fast electrode positioning and quick withdrawal in case of a cave-in.

During the refining period, where the unbalances are small, the response is at a slower rate, and the speeds are a fraction of the maximum speed where only small adjustments are required. The oscillograms also show that sudden unsustained variations do not cause electrode movement.

We certainly would agree with Mr. Shaad's remarks on stability.

The factor of stability is very important in any regulated system.

With the Rototrol system the following factors affect stability:

1. Level of ampere turns in the control fields.
2. The degree of self-energization and amplification.
3. The time constants and mutual inductance of the various circuits.
4. Mechanical inertia and friction of the system.

Careful consideration of these factors in the regulator design, coupled with rates of response and electrode speeds proportional to deviation from the regulated value, has resulted in an inherently stable regulator capable of initial adjustment to meet operation best with any type of furnace.

Mr. Henderson is to be complimented on the excellent results he has obtained in eliminating light flicker due to furnace loads. Use of the buffer reactor, synchronous condenser, and associated switchgear, or apparatus of similar nature, is to be expected for problems of this type.

Electronic Control of D-C Motors

Discussion and author's closure of paper 43-144 by K. P. Puchlowski, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, pages 870–7.

G. A. Caldwell (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This paper gives a very clear and complete explanation of the features that must be added to an electronic rectifier to give it characteristics suitable as a power supply for a d-c motor. By referring to Figure 2 and Figure 8 of the paper, it is apparent that this modern unit has the necessary compensating devices actually to improve the inherent speed-torque characteristics of the motor itself, so that the combination of the rectifier and motor makes it possible to operate the d-c motor over a very wide range in speed with good speed regulation at all speeds.

There are many applications in industry where such a drive is desirable. On many machine tools it has been normal practice to use a constant-speed a-c motor with suitable change gears or a hydraulic mechanism to obtain the necessary speed range. It is also possible to obtain wide speed ranges by using a motor–generator set with a suitable rotating regulator to obtain the necessary speed range and speed regulation.

A majority of the wide-speed-range applications are constant torque in their nature. In other words, the horsepower output of the d-c motor must be proportional to the speed at which it runs. The electronic control described in this paper controls the motor speed by regulating the voltage applied to the motor armature. The motor horsepower output, neglecting the effect of ventilation and wave form, is, therefore, proportional to the speed at which it runs. Hence, the electronic drive is ideal for these constant-torque applications.

Other types of wide-speed-range applications are a combination of constant torque and constant horsepower; that is, the torque required at low speed may be three to four times that required at high speed. By introducing the feature of field control on the d-c motor, which is mentioned in the paper and is a standard feature, the electronic drive also can be applied satisfactorily to this application. The practical speed range that can be obtained by field control is usually considered 4 to 1. If we select a motor that has a base speed of 690 rpm and a maximum speed of 2,760 rpm by field control, the drive gives constant horsepower over this range, and then by using armature voltage control below 690 rpm, the drive can be extended down to approximately 50 rpm at constant torque. A number of such applications have been made, as well as those using motors without field control.

Some wide speed-range drives are almost constant horsepower in nature, which means that the torque requirements of the drive increase in proportion to the decrease in speed. When the speed range is wider than can be obtained by motor field control, then the entire drive must be oversize to obtain sufficient torque at low speeds. Consequently, the drive is larger than necessary for the upper speed range, just as is the case when a motor–generator set is used.

As previously indicated, the same type of characteristics can be obtained with adjustable voltage drives when a motor–generator set with rotating regulator is used, but on certain types of applications the electronic drive has advantages. An important advantage is that the electronic drive has no moving parts and, therefore, is not a source of vibration. This is very important on such applications as grinders, where the surface finish is affected by any source of vibration.

It is also usually simpler to mount the electronic drive than the motor–generator set required in the adjustable-voltage scheme of control.

With the electronic control, the speed-adjusting mechanism is a small potentiometer which is much easier to mount and also to operate than the speed-control rheostat used in the adjustable-voltage scheme of control. On certain machine-tool applications it is desirable to control this speed from a push-button station, and the potentiometer of the electronic scheme can be mounted in the same station as the push buttons. To accomplish similar results with the motor–generator-set scheme of control requires the use of a motor-operated rheostat.

This small potentiometer also is much easier to incorporate into any scheme of control in which the speed must be varied from some mechanical mechanism in going through an automatic sequence of operation.

This form of electronic control promises to be a very useful tool to an industry and an important member of the electronic family.

G. E. Walter and H. H. Leigh (General Electric Company, Schenectady, N. Y.): As an ingenious approach to the problem of obtaining accurate and useful control of d-c motors by electronic methods, the circuit arrangement discussed in this paper warrants close study. The author's list of characteristics of present-day rectifier drives is noteworthy, in particular, for its complete and detailed presentation of the advantages to be expected in such systems.

His circuits and the diagrams illustrating their operation contain certain features and apparent discrepancies, however, to which we would like to call attention.

Two of the circuits deserve careful functional scrutiny. Although the description of the IR compensating circuit's operation is quite clear in the text of the paper, its actual performance is rather obscured by Figures 2, 8, and 9 of the paper, in which a startling discrepancy occurs. Figure 2 clearly confirms the author's statement of the d-c motor equation by showing a constant slope of the speed-load line regardless of the no-load speed of the motor. If the same speed is desired at all loads, the motor equation can be rewritten as

$$nc\phi=E-IR=\text{constant}$$

indicating that the terminal voltage of the machine must be increased by an amount equal to the IR drop of the armature circuit, if the speed is to remain unaffected by the load. The parallel lines of Figure 2 of the paper show distinctly that the speed of a certain motor operated from a d-c source will drop by 125 rpm from one ampere to 4.3 amperes armature current at any setting of no-load speed.

Figure 8, with its flat speed-load lines, creates the impression that the author's electronic drive automatically increases the terminal voltage on the motor by an amount very nearly equal to the internal IR drop at any load. Since the IR drop depends only on armature current, the curves of Figure 8 should be produced by increases in terminal voltage which are the same from zero to full load at each speed. In other words, the compounding of a d-c motor with constant field should be independent of speed so long as armature reaction effects are negligible.

However, Figure 9, purporting to display the actual voltage characteristics that caused the flat speed-torque curves of Figure 8, claims that such curves can occur with voltage increases ranging from 13 volts at 90 rpm to 4 volts at 1,000 rpm. Our experience and test records on thyratron motor control indicate that the voltage characteristics of Figure 9 could not produce the speed characteristics of Figure 8. It is interesting to speculate on the possible disposition of this internal IR drop, particularly at 1,400 rpm, where a perfectly flat characteristic in Figure 8 is caused apparently by no increase at all in the voltage of Figure 9.

The current-limiting circuit is the other circuit deserving special attention. It operates fundamentally by placing on $R2$ a voltage signal in addition to the one already present as an indication of motor speed. Tube 3, therefore, responds to a grid voltage which is the algebraic sum of the signals from the motor armature, the current-limit circuit, the IR compensating circuit, and the setting of the speed-control potentiometer $P1$. To retard the firing point of the armature thyratrons, and thus limit the current, the signal on $R2$ produced by tube 5 (in the current-limit circuit) must increase at least at the same rate as the signal on $R2$, caused by the armature voltage, is decreasing. The only possible way for this to occur is for the armature current itself to increase at the same time.

In addition, the effect of the IR compensating circuit is to oppose the effect of the current-limiting circuit. Although the latter eventually overwhelms all its opposition, the net result is shown in Figure 11, where the current-limit circuit becomes operative at four or five amperes but allows eight amperes to flow when the motor is stalled. Between these limits the armature current is a complicated and subtle function of motor speed and the settings of four potentiometers—$P1$, $P2$, $P4$, and $P6$. A change in any one of these will affect the armature current. There is, as a result, no sharp limit to motor current and torque.

The particular effect of speed on the armature current shown in Figure 11 of the paper, where the current varies over a 2 to 1 range as the motor speed increases from zero to its preset value, also presents an interesting contrast with Figure 12. In this latter set of curves the armature current is constant allegedly for an appreciable period while the motor is accelerating; yet Figure 11 clearly states that if the speed changes while the current-limit circuit is operating, the current also will change. Obviously, Figure 12, to be true, must be based on a constant current limit independent of speed, which Figure 11 shows is not produced by the author's circuits.

Finally, although the paper mentions automatically controlled reversals, it does not describe the operation of the current-limit circuit for negative speeds. This is the situation confronting the electronic circuits at the instant the motor armature is reversed by contactors and reconnected to the rectifier. Study of the current-limit circuit discloses that it should become almost useless under this condition, since the signal from the armature voltage on $R2$ actually opposes the signal on $R2$ from the current-limit circuit. Instead, the "antiplugging" feature mentioned by the author may play an important part in the automatically controlled reversals to which he refers, conceivably in decelerating by dynamic braking or a similar method.

K. P. Puchlowski: The remarks of Mr. Walter and Mr. Leigh concerning the alleged discrepancy in characteristics shown in Figures 2, 8, and 9 of my paper seem to indicate that the authors of the discussion have analyzed the subject from a very general and conventional angle only. In fact, it may be stated that no discrepancy whatever exists if careful scrutiny is applied to the whole phenomenon. Obviously, in any drive the IR drop of the motor will be a function of load current only, and there is no need, therefore, to speculate on its disposition at different speeds of the motor. The authors of discussion, however, have overlooked the very important factor of armature reaction.

It is stated plainly in the paper that Figure 2 is a graphical illustration of the equation

$$n=\frac{E-IR}{c\phi} \tag{1}$$

under the following assumptions: E = constant, R = constant, and ϕ = constant; whereas, Figures 8 and 9 represent actual operating characteristics, where the effect of armature reaction is well pronounced.

In fact, equation 1 may be rewritten in the following form:

$$E=Cn\phi+IR \tag{2}$$

For ϕ = constant and n as a parameter equation 2 obviously will represent a family of straight lines parallel to one another, as shown in Figure 2 of the paper. Under actual conditions, however, ϕ is not constant because of the armature reaction but is itself a function of the armature current, $\phi = F(I)$, and equation 2 will become

$$E=CnF(I)+IR \tag{3}$$

Careful analysis of equation 3 will reveal that the armature voltage consists of the sum of two terms, the second of which represents a straight line independent of the speed n. The first term, however, which is also a function of the armature current I, is directly proportional to the speed, and for different speeds the sum of the two terms will represent a family of curves, $E = f(I)$, which are not parallel to one another. The curve, $\phi = F(I)$, has normally a negative slope as compared with the positive slope of the straight line representing the term IR, so that the voltage increase necessary to maintain constant speed will be reduced with respect to the case ϕ = constant. Furthermore, the negative slope of the curve representing the first term of equation 3 will vary with speed, namely, will be greater for higher value of n, and, consequently, the voltage increase necessary to maintain constant speed will be reduced more for higher speed. In other words, the voltage increase will decrease gradually with increasing speed, a phenomenon which can be observed very nicely in experimental curves plotted in Figure 9. Thus, Walter's and Leigh's statement, that voltage characteristics of Figure 9 could not produce the speed characteristics of Figure 8, is incorrect.

Walter's and Leigh's interpretation of the current-limiting circuit seems to be incorrect as well as confusing. The principle of any current-limiting action is based obviously on the fact that, when the rising current attains a definite value, the current-limiting device starts to operate, and its operation results in a more or less sharp decrease of voltage while current is still increasing, so that soon a final limit of current is reached. This current limit would correspond to the maximum possible torque, that is, locked armature with a low voltage applied to it. Hence, the implications that the current-limiting circuit being discussed has a disadvantage consisting of the fact that the armature current must increase during the process of current limitation is surprising.

Besides, the authors of the discussion seem to have overlooked the basic fact plainly stated in the paper that the amplifier consisting of tube 5 and associated circuits is characterized by a sharp cutoff and high gain. When the value of the resistance $R2$ is made sufficiently high, the sharpness of increase of voltage across $R2$ produced by tube 5 and the corresponding sharpness of decrease of armature voltage are high enough to produce a sharp current-limiting action. It is apparent that an infinite sharpness of the speed-current curves at the limit value of current is impossible, since this would be contrary to the principle of continuity of physical curves. In other words, the current-limiting action can be made more or less sharp but never infinitely sharp.

The fact that the action of the IR compensating circuit opposes that of the current-limiting circuit is of no consequence, since, as it is stated plainly in the paper, the ratio of change of the effective voltage produced by tube 5 to that produced by tube 4 is very high.

Thus, the shape of curves shown in Figure 11, and, in particular, the effect of current limitation shown thereon is contrary to what the authors of discussion seem to believe. It is not a result of the inherent properties of the circuit but of the selection of the circuit constants and, in particular, of resistances $R1$ and $R2$.

If the aforementioned is summarized, Walter's and Leigh's criticism concentrated around the sharpness of current limitation and the two particular graphs shown in Figure 11 seems to be immaterial, since, in the first place, such a thing as a "constant current limit independent of speed" does not exist; in the second place, the extent to which the actual conditions will approach this idealized situation has not been specified by the authors of discussion to constitute a reference for comparison; and, in the third place, characteristics shown in Figure 11 do not imply that a higher degree of sharpness of the current limitation cannot be obtained with the current-limiting circuit in Figure 10.

Curves shown in Figure 12 were intended to demonstrate graphically the principle of current limitation, and, rigorously speaking, no current-limiting device ever will produce a strictly "constant" starting current, but the flat top of curves 2 and 3 in Figure 12 always will have a slight slope, so that this so-called by the authors of discussion "constant current limit" will be decreasing actually with increasing time.

During the rapid reversing of contactors, when the rectifier system becomes for a limited time an inverter, and regenerative braking takes place, the voltage across $R2$ produced by the armature voltage of the motor actually changes its polarity and opposes the voltage produced by the current-limiting tube 5, as Walter and Leigh have stated rightly. However, it should be remembered that, although this signal voltage has an opposite sign with respect to the voltage produced across $R2$ by tube 5, the sign of the rate of change of the two voltages is the same, and, therefore, the current-limiting action during the rapid reversing remains fully effective. Thus, Walter's and Leigh's statement that "it should become almost useless" is incorrect.

A New-Type Carrier Relay Protection

Discussion and authors' closure of paper 43-147 by E. M. Wood, H. W. Haberl, and F. A. A. Baily, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, December section, pages 774–9.

M. J. Brown (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The departure from conventional carrier-current schemes described in this paper is interesting as an example of the type of system which can be developed to meet the requirements of a particular power system.

Of special interest is the transmission of carrier at time of fault to *initiate* tripping at the distant end. The transmission problems associated with this condition have made its general use restricted. However, it is used as a means of clearing transformer faults when a transformer is included with a line between a local breaker on the low side of the transformer and a high-voltage breaker at the distant end of the line. Should a fault develop in the transformer, its presence is indicated by differential relays which trip the breaker on the low side of the transformer and transmit coded carrier to a code receiver at the distant end of the line to trip the breaker there. A line trap is inserted between the line and the transformer in the phase conductor used for carrier.

It will be an interesting supplement to this paper to have an account of actual performance after operating experience with the system has been acquired. The conditions that appear to be most unfavorable for carrier transmission during fault are as follows:

1. Close-in fault. A minimum of coupling between phases will exist if the fault is near the coupling capacitor.

2. Fault at odd quarter-wave-length points on line. A line short-circuited at a quarter-wave-length point offers high impedance at its terminals. Line input current would be reduced, and transfer of energy through magnetic coupling with other phase wires would be reduced.

3. Fault at half-wave-length points on line. A line short-circuited at a half-wave-length point offers low impedance at its terminals. Line input watts would be reduced because of the mismatch, and the transfer of energy to other phase wires would be reduced. At 205 kilocycles the quarter-wave-length points are 1,200 feet apart. In the 20 per cent end zones of the longest line, a distance of 40 miles at each end, these will be a total of 352 quarter-wave-length points.

Offsetting the aforementioned conditions are:

1. That fault resistance will mitigate reflections when trouble occurs at quarter-wave-length points.

2. That the high-transmitter power can produce an appreciable signal at the receiver through considerable attenuation in addition to that offered by the normal line.

The high-transmitter power requirement will limit the application to a very small percentage of installations, because the cost of such equipment will handicap unfavorably its application in competition with the now widely used systems employing transmitters of moderate power output.

T. A. Cramer (General Electric Company, Schenectady, N. Y.): The authors of this paper have described an interesting application of carrier-current equipment which illustrates the flexibility of this tool for power-system engineers, whereas the standard blocking-type carrier equipment is a relay channel with a telephone attachment, this equipment is essentially a telephone channel with a relaying attachment.

I would like to ask the authors if the 400-watt carrier-output figure was determined by the results of the field tests mentioned in the paper?

A modern blocking-type carrier relay equipment has a power output of approximately 15 watts, employs about eight electron tubes, and operates directly from the 60-cell-station storage battery. It is obvious that the equipment described in the paper is operated from an a-c source. Is the station service considered satisfactory for this purpose, or is a motor generator operated from the station battery to supply power to the equipment?

Our specifications for the blocking-type carrier relaying have always contained the requirement that faults fed from only one end of the line section should be tripped at high speed to avoid unnecessary disturbance to the system when a faulted line is closed on. It appears that this specification is not met by the relay arrangement described in this paper. When the line is closed on a fault in the remote end-zone tripping will occur on the second-zone time delay, since the remote relays will not be energized because of the breaker being open at that point.

When high-speed reclosing is applied to a transmission line, it is important that both ends of the line be tripped simultaneously at high speed, and the blocking-type carrier always trips in this manner. With the transferred-trip type of carrier a fault on the coupling phase near the station can block the tripping impulse and will result in one breaker tripping in the second-zone time which usually is too long for a successful reclosure.

F. M. Rives (General Electric Company, Schenectady, N. Y.): Since its introduction approximately ten years ago the use of the blocking-signal-type of carrier relaying has become so widespread that the equipment designs and application technique associated with this system have become well standardized. As a result other possible carrier relay systems have not received the attention they may well deserve. When an art reaches this stage the stimulus of a new approach or new point of view is often very helpful. We are indebted to the authors of this paper for supplying this stimulus.

In the development design and application of carrier channels for relaying one of the fundamental axioms, heretofore rigidly observed, is that the carrier channel shall not be required to transmit the tripping or blocking signal through the line section in fault. In fact the primary purpose of the line traps, invariably used with relay channels, is to prevent any possibility of the fault interfering with the proper transmission of carrier on the sound sections. Here is a system the usefulness of which admittedly depends on the transmission of the carrier signal over the line section in fault. The statement of the paper referring to tests made to determine whether or not the required signal could be transmitted through a fault definitely is inconclusive. It would appear that the success or failure of transmission would depend on several factors, such as the nature and location of the fault and the carrier frequency used, as well as transmitter power. Simply increasing the transmitter power from 15 to 400 watts may not be sufficient to insure transmission; in fact, it is doubtful if several times this power would insure transmission.

It would be of interest to know why the equipment was designed for operation in the frequency range of from 145 to 205 kilocycles. The increase in line attenuation in this range of frequencies as compared with the 50- to 100-kilocycle range on the long sections may be greater than the gain obtained by increasing the transmitter output power from 15 to 400 watts. For example, the line attenuation of the 300-mile line from Leaside to Beauharnois figures to be approximately 14 decibels at 50 kilocycles, 20 decibels at 100 kilocycles, 30 decibels at 150 kilocycles, and 45 decibels at 200 kilocycles on an interphase basis. Phase-to-ground operation may be some 20 per cent higher. The gain of 14.26 decibels obtained by increasing the transmitter power thus is less than would be gained by changing frequency on the 300-mile line from 150 to 50 kilocycles.

From the description of the carrier equipment it would appear that it was designed primarily for the telephone-communication function with the relaying as a secondary function. It would be of interest to know what the operating experience has been with the telephone equipment. Offhand it would seem that the time required for the control functions involving tandem relays would result in an undesirable amount of clipping and confusion for certain users. In the development of standard types of single-frequency-duplex telephone equipment, employing voice-actuated control circuits, it has been found necessary to obtain higher and higher speeds for the control functions with a result that the over-all start time or the interval between the time a sound enters the microphone of the transmitter and the time it emerges from the receiver has been reduced to less than ten milliseconds. This high-speed control has been found necessary to minimize clipping, so that the system will function satisfactorily for all types of voices.

In the conclusions the authors state as one of the advantages provided by their system that only one carrier set is required at each station. While this may be true where the number of lines involving a single carrier channel is relatively small, there is a definite limit to the number of audio-frequency channels that can be accommodated above the voice-frequency band. It would be of interest to know what the authors consider to be the maximum number of audio channels or lines that can be handled on a single carrier-frequency channel. Using a single carrier set for several lines and for joint operation of relaying and communication has the operational disadvantage common to many joint or multiple-use schemes in that service interruptions for tube replacements or for normal maintenance will interrupt all of the functions associated with a single set. It would appear also that the complexity of equipment and perhaps the over-all cost required to provide this joint operation are somewhat greater than would have resulted if separate relaying and communication channels using standard commercially available equipment and conventional blocking-type relaying had been established.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): The authors' statement "If the carrier should fail . . . in no case would the incorrect operation or failure in the carrier system be detrimental to the relay system . . ." reminds me of the often criticized relay-operation report classification, "correct but undesired." We are interested, however, not in relay operation as planned but relay operation which will maintain service. If the relay operation is correct, but system stability is lost and load interrupted, then we seem to need a new definition of "correct."

The essential difference between the system described and most of the previously installed carrier-relay systems is that in this new system the carrier signal must be transmitted through or around a short circuit, whereas in the past the signal has been transmitted over an ideal carrier channel, namely, a sound section of power line isolated from external influence by a high impedance (line trap).

The paper indicates that carrier-transmission tests were made with one- and two-wire-to-ground faults, and that these were successful, but it fails to indicate what phases were involved in the faults and what phase was used for the carrier transmission. Neither does the paper indicate the fault location for these tests. It hardly seems possible that successful transmission of the tripping signal could be obtained even in the case of a single-wire-to-ground fault if the fault were on the coupling phase and close to the transmitter.

The authors have indicated that 0.25- to 0.30-second clearing is necessary for stability in the case of three-phase and two-phase-to-ground faults. But by using a tripping system it would not be obtained for 20 per cent of the three-phase faults and perhaps for 13.3 per cent of the two-phase-to-ground faults (two thirds of the 20 per cent end zone).

In the paper by A. H. Frampton and E. M. Woods the percentage of three-phase faults occurring on this system is given as 15 and the percentage of two-phase-to-ground faults as 26. Thus it would appear probable that the high-speed action would not be obtained on 6.6 per cent of all faults. However, since the high-speed action is required only for multiphase faults (in this case 41 per cent), we find that of these faults 16 per cent may fail to give the desired action. Hence, even if every relay element and carrier element operates correctly we can never achieve 100 per cent correct operation from a system viewpoint.

With modern carrier installations from about 1937 or 1938 there is no evidence that the blocking-type of carrier relaying has failed because of transmission difficulties. If the operating record with the new system should show that the tripping signal can be transmitted successfully through all kinds of faults, then we would have data which would show general parity with the blocking system.

If we concede that the transmission of the signal is of equal reliability, then we could compare the relative merits of the blocking system with fast but possibly incorrect tripping to the tripping system with correct but delayed tripping in case of random failure. There scarcely seems to be enough advantage in favor of the tripping system to justify the complications of a 400-watt instead of a 15-watt transmitter and a somewhat greater number of relay units.

Since there seem to be grounds for reasonable doubt as to the reliability of the carrier-tripping channel when most needed, it would be sensible to await more data showing the operating experience and particularly the record showing the success of the transmission with all kinds of faults near the transmitters. If the data indicate successful performance on this score, we then can determine properly whether the greater complications are justified.

Reference

1. The 220,000-Volt System of the Hydro-Electric Power Commission of Ontario—II, A. H. Frampton, E. M. Wood. AIEE Transactions, volume 60, 1941, pages 1215–21.

E. M. Wood, H. W. Haberl, and F. A. A. Baily: The authors thank the various contributors to the discussion. Their comments were informative and, we believe, to the point.

The following, in part, answers the questions raised.

As stated in the paper, we had primarily a communication problem and also a desire to improve the relaying time.

In trying out the trip-transfer scheme for relaying we had two points in mind.

The transfer-trip scheme, if it would work, was considered more sound fundamentally in that failure to transmit the carrier impulse does not cause incorrect tripping; it merely fails to give the desired improvement.

We have the disadvantage that we transmit our transfer-trip signal over the faulted line. However, on the job in question we have alternate route lines over which we can transmit the transfer-trip signal as shown in Figure 2 of our paper, and, although we have made tests to prove that the carrier signal will pass through grounded transmission circuits, it is not essential, as the carrier signal can operate through the other two sides of the triangle.

With the blocking scheme using low-power carrier sets, we doubted the reliability of transfer across the very long lines; failure to transmit actually would allow incorrect tripping. There also is a problem in contact racing inherent in the blocking schemes. Our own experience with that type on another line during the past season indicates that this is a real problem.

The frequency range assigned in Canada for power-line carrier, except for low-power sets, is 145–205 kilocycles. A basic design of high-power transmitter receiver in this range suitable for application of tones being available was used.

With the present available design the upper limit of the audio-frequency circuits without distortion is roughly 6,000 cycles, and to avoid using tones which might have second harmonics the number of usable tones with the present filter system has been designed so as not to exceed three. This is in no way to be taken as the maximum number of separate tones which may be used on some future job which would be designed specially for this purpose. With adequate filter design and adequate transformer design the number of tones is limited only by the harmonic content of the carrier system and the sharpness of the cutoff of the tone-acceptor circuits.

At some future time it might be possible to design a carrier set to operate in the 50–100-kilocycle range and superimpose on this carrier audio-frequency tones up to 10,000 cycles, which set would have a minimum of side-band cutting in the higher ranges.

We had hoped before this closure to make some tests on tone transmission through a line with faults of various types, as nearly as we can determine, at the worst location near a terminal. Operating conditions have caused these to be postponed.

The quality and reliability of voice communication through all kinds of weather have been first class; there is some speech clipping which a few of the users do not like, but through constant usage most of the objectors have found it to be not too unsatisfactory. Naturally, refinements in speech delay could be an added feature, and this would eliminate entirely the first syllable clipping.

The speed of operation is suitable for our system.

Power at 60 cycles for the set is supplied in some cases from existing separate 60-cycle-station services and in others from ten-kilowatt flywheel-type motor-generator sets driven from the 25-cycle-station service. Where the latter is used there is a stand-by supply for low-power communication, only from a smaller set fed from the station battery.

Dethermalizing Arc Quenchers

Discussion and authors' closure of paper 43-142 by Lloyd F. Hunt and Fred H. Cole, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 720–4.

H. E. Strang (General Electric Company, Philadelphia, Pa.): The authors of this paper are to be congratulated for presenting to the industry this record of logical and orderly development in connection with circuit-interrupting devices, particularly since it is supplemented by such interesting field-test and field-service data.

This contribution is noteworthy in that it provides additional recognition of the mobility of oil and the effectiveness of the movement of such a mass of cool oil across arcing contacts. This fundamental principle has already been discussed, and it is now evident that the desirable end result may be accomplished by a variety of physical arrangements of contacts and other parts.

Test performance and field experience seem to speak well for the designs discussed by the authors. However, it is doubtful if the typical drooping characteristic of arcing time against current justifies the unqualified assumption that "breaker performance will improve at higher interrupting capacities."

W. M. Leeds and **A. W. Hill** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It is with some surprise that we learn in this paper that a method has been found for interrupting arcs under oil with the formation of no carbon whatsoever! Our experience has been that there is a fairly close correlation between the amount of free carbon liberated and the energy dissipated in the arc.

It is stated in the paper that common operating practice requires the inspection of an oil circuit breaker after each automatic operation. Although this may be a justified procedure for high-voltage circuit breakers of very early design, there are many modern circuit breakers in service which are not looked into until after 10, 20, or in some cases as many as 50 automatic operations.

The dethermalizer assembly in an insulating tube, as illustrated in Figure 6 of the paper, shows the stationary contacts bridged by horizontal insulating surfaces. Have the authors noted any tendency for carbon deposits on these surfaces to affect the dielectric strength of the insulation?

In the companion paper by F. H. Cole

and C. P. Garman it is shown that the opening of the middle pressure-generating contact one-half cycle in advance of the end contacts gives a definite improvement in performance. Was this used on the 230-kv dethermalizer design tested at the Laguna Bell substation? Figure 7 of the paper would indicate that the contacts all parted simultaneously. It would be interesting to know the reason for grading the width of the ports, starting with small openings at the top. Since the heavy currents are interrupted with shorter contact separation, it would seem that the grading should be in the reverse direction so as to allow expansion for the heavier pressure surges and higher-velocity oil flow through the first few channels.

Lloyd F. Hunt and Fred H. Cole: It is gratifying to have such good discussion on our paper. Mr. Leeds and Mr. Hill bring out the point that carbon is formed by arcing; however, in our tests, after many short-circuit interruptions, there was no measurable amount of free carbon caused by the low arc energy. In their second paragraph it is possible for us to talk only about things we have had experience with, which include 110 230-kv oil circuit breakers of many makes. It has been necessary to give almost all of these switches a great deal of maintenance. In fact, as a result of many short-circuit clearances, the switches have not been in operating condition until they are repaired. We are happy to state that the development during the last year will allow us to fit up our switches with modern arc quenchers that will allow the large number of operations. With reference to their third paragraph, we have stressed the insulation of the dethermalizers by repeated line-dropping tests and have found no evidence of deterioration of the dielectric strength of the insulation. In their fourth paragraph they ask about the parting of contacts. The three contacts in each dethermalizer part at the same time. Also, it has been shown by Leeds' work, as well as by these tests, that a certain contact separation is necessary at arc extinction to prevent restrikes; therefore, it is important not to force all the oil out of the ports before this separation occurs. If the top ports were too large this would occur, and good arc extinction would not result. The pressures and amount of oil flow have been calculated for this correct operation, and field tests have substantiated the calculations.

In answering Mr. Strang's discussion, we wish to agree with him that the movement of cool oil across the arcing contacts is fundamental for good arc extinction. When we said "Breaker performance will improve at higher interrupting capacities," it was intended to mean within the rating of the switch. If the switch is rated at 500,000-kva interruption duty, it means that the performance of the breaker improves up to that point. This is necessary, as the design is such that pressures developed in the quencher are controlled by the ports and are so arranged to give most effective arc extinction at that rating with the proper factor of safety. Should the switch be rated at 2,500,000-kva interrupting capacity, the ports would be so designed that the maximum arc extinction would occur at this value and also be designed to withstand the internal pressures with the usual factor of safety. This is why the dethermalizing arc quenchers improve in operation up to the duty for which the switch is designed.

We appreciate the interest shown in our paper and thank those who took part in the discussion.

A New One-Cycle Directional Overcurrent Relay

Discussion and author's closure of paper 43-143 by W. C. Morris, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 725–7.

S. L. Goldsborough and **S. C. Leyland** (Westinghouse Electric and Manufacturing Company, Newark, N. J.): There is an implication made in this paper that high-speed directional overcurrent relays with satisfactory contact co-ordination have not been available in the past. This is entirely incorrect. Relays of this type have been used for years for phase protection, ground protection, and in several carrier-current schemes, and have had a service record requiring no apologies.

The prime requirement for correct contact co-ordination in any high-speed relay is the use of elements which inherently have co-ordinated time characteristics. For many years correct contact co-ordination has been obtained between a high-speed loop-type directional element and a balanced beam-type impedance element in the impedance relay. By the proper use of the beam construction for the overcurrent element in a directional overcurrent relay using a high-speed loop-type directional element, correct contact co-ordination is obtained. This combination has been used in a directional overcurrent carrier-ground relay with complete success for the last six years. Conventional applications of a similar relay have been made also. Directional overcurrent relays utilizing a plunger-type overcurrent unit and a loop-type directional relay also have a highly satisfactory operating record. This type of relay has been available for at least ten years.

The author states that "For the protection of most of the lower-voltage lines, distance relaying has generally been considered to be too expensive." Yet, in this relay he uses the same elements that are used in some distance-type relays. A single-zone distance-type relay need have no more elements than the author's single-zone overcurrent relay. Such a distance relay has the added advantage of operating for the same distance when wide variations in system generation (magnitude of fault currents) are encountered. In evaluating cost of protection, it is necessary to consider all factors involved. A relay which will provide better protection under all system conditions certainly has more value than one whose effectiveness varies with changes in generating capacity.

In the synopsis Mr. Morris mentions the common practice of adding instantaneous trip attachments to directional overcurrent relays but bemoans the many incorrect tripouts. Apparently he has encountered many questionable applications of instantaneous trip attachments. This certainly has not been our experience, for the instantaneous trip attachment, when properly applied, has an operating record as high as any other type of protective relay or relay scheme. It is hoped that the statement in the paper will not discourage the use of instantaneous trip attachments. One reads further with the anticipation that this application problem, if it really is a problem, is to be solved within the simplicity and price class of the common directional overcurrent relay but finds only one more version of a relay type which has been in service for years.

The only new feature which the author has introduced in his paper is directional control of a high-speed relay, and directional control solves only half of the problem. It is still necessary to have elements which will co-ordinate inherently for the other half of the problem, and such elements have been in common use for many years.

C. L. Smith (Rochester Gas and Electric Corporation, Rochester, N. Y.): The relay described by Mr. Morris is a welcome surprise to me. Not so many years ago large numbers of induction-disk directional overcurrent relays were installed throughout the utility industry. The directional elements were characterized as "instantaneous," and then, after the first severe fault either hung on too long or failed to clear at all, we woke up to the fact that the inverse time–current characteristic of the overcurrent element was being nullified neatly and completely by the inverse-voltage characteristic of the directional element. The addition of a directional-control feature did not improve the situation much; it just cascaded the over-all time of both elements. In due time, faster and better directional elements were developed, and, when they were used with high-speed overcurrent elements, they provided high-speed directional overcurrent protection at normal voltage and reasonably fast operation at low voltage. Contact co-ordination became a difficult problem. In many cases the only solution was the use of auxiliary interlocking or short time-delay relays. These remedies were more or less effective but did nothing to cure the fundamental trouble. We still had overcurrent elements that speeded up and directional elements that slowed down as the severity of the fault increased.

Apparently in this relay complete co-ordination has been accomplished by eliminating this basic difference in performance of the two elements. At the same time, performance at low voltage has been improved greatly. If my memory is correct this is the first time that any designer has submitted time–current curves and oscillograms of single-phase directional-element performance based on one per cent of normal voltage. It would be interesting to see similar curves based on normal voltage.

I should like to ask Mr. Morris what provision has been made for current calibration of the overcurrent unit; also, does he contemplate developing a similar relay having a three-phase directional unit co-ordinated with three single-phase overcurrent units?

G. B. Dodds (Duquesne Light Company, Pittsburgh, Pa.): The relay described in the paper undoubtedly will fulfill the requirements where an instantaneous overcurrent power directional relay is required. The number of places where it can be used can be determined only by experience.

The widespread use of instantaneous overcurrent elements for the first zone protection of transmission circuits has come about by the ease and little expense of installing instantaneous overcurrent devices in the cases along with the time-delay and directional elements. There are a great many locations on systems where coordination between the instantaneous overcurrent elements and the directional element is not required, because the fault current in the direction desired to be tripped by the instantaneous overcurrent element is greater than that in the opposite direction. Of course this condition can exist on only one side of a station. Undoubtedly, many applications have been made where coordination should be required and is not obtained, and the new relay will be applicable in such places.

The question arises, however, if, with a directional element that is separate from that used for time-delay protection on a circuit, and with an overcurrent element that, along with the directional element, requires a separate relay case from that used for time-delay protection, a further step might not be taken and an instantaneous directional impedance relay be used at little or no greater cost than the directional overcurrent relay described. Use of the impedance protection would avoid entirely any variation in operation due to variation in fault current caused by generation capacity or by system connections.

A further thought is that, if the relay described in this paper is used for instantaneous overcurrent protection of a circuit in place of distance relays, a variation obtained in fault current will occur, depending on whether the fault is single-phase or three-phase. As a great many faults are single-phase short circuits rather than three-phase short circuits, could the difference in measurement be eliminated by adding a second-phase current to the instantaneous overcurrent element as is done in the ohm unit of the reactance relay to obtain the same current under both fault conditions?

Information on the relay burden and on the available current pickup taps would be of interest.

The relay described undoubtedly will prove a valuable addition to the existing protective relays, although its field of wide application is not immediately apparent.

G. A. Powell (New York Power and Light Corporation, Albany, N. Y.) and **A. J. McConnell** (General Electric Company, Schenectady, N. Y.): The problem of coordination of the contacts of instantaneous overcurrent and high-speed directional relays is an old one which has not received proper attention heretofore. Mr. Morris describes a relay which solves the problem in a manner that eliminates the application effort normally occasioned by the possibility of an adverse contact race. Once the need for an instantaneous overcurrent relay is established, the application work is simply the determination of the overcurrent setting.

The New York Power and Light Corporation has successfully circumvented this problem by the use of a polyphase directional relay with polyphase-voltage restraint in conjunction with high drop-out plunger-type instantaneous overcurrent relays. The same directional relay is used with the time-delay overcurrent relays.

The voltage restraint eliminates the contact race caused by load current, both at the inception of the fault and during the opening of the adjacent circuit breaker. The contact race occasioned by reversal of short-circuit current is also eliminated if, after the nearby circuit breaker opens, the impedance around to the fault is greater than the impedance setting or reach of the directional relay. Any infeeds between the relay and the fault serve to increase the apparent impedance at the relay location.

It is granted that the polyphase directional relay with voltage restraint entails more application work than does the relay described by Mr. Morris. In addition to determining that the directional relay will not operate on the current reversal, it is necessary to check that the reach of the directional relay is sufficient during a phase-to-phase fault. It must be borne in mind that a polyphase directional relay with voltage restraint has approximately twice the reach during a three-phase fault that it has during a phase-to-phase fault. However, it has been determined that, if the impedance setting is less than twice the line impedance from the relay around to the fault (after the nearby circuit breaker has opened), the contact race is avoided. This generally provides adequate margin for desired operation during a phase-to-phase fault. If not calculation will usually indicate that the impedance setting can be increased considerably and still avoid the contact race.

F. S. Benson (Pacific Gas and Electric Company, San Francisco, Calif.): A high-speed one-cycle directional overcurrent relay that has positive operation when the fault is in the nontripping position will be of great benefit to relay engineers, as it has been necessary in the past to prevent incorrect operation of such a relay by the installation of other types of relays to provide a restraining action. The use of capacitors in the watt element to produce a voltage for one cycle after the circuit voltage has fallen to a very low value during a short circuit is indeed a clever idea and should provide the necessary torque to insure correct operation of the watt element.

The arrangement of a current phase-shift circuit, the poles of which are magnetically coupled in the overcurrent element to prevent its operation until the auxiliary contact in the watt element is closed, should provide the necessary time delay to prevent the overcurrent-element contacts from closing until the power flow is in the correct direction. This arrangement should prevent effectively the race between the contacts of the watt element and the overcurrent element from closing indiscriminately and should insure a correct sequence of contact closing.

It would be interesting to know the type of contacts used, whether or not a definite pressure is required, before sufficient current flows to operate the holding coil, whether or not they have ample wiping action, or whether they are of a nonbouncing design. The relay test man and inspector who must set and service these relays in the field would prefer to have adjustments that could be made to the relay in the field, such as changing the location of the core with relation to the magnetic field and some provision for changing the magnitude of the current through the phase-shifting arrangement of the overcurrent element. Since the overcurrent element (one-half cycle) may be closed instantaneously, it seems that taps should provide a range similar to that used with instantaneous overcurrent relays. No mention of such taps is made in the paper.

A description of the relay assembly would be quite timely, particularly the method of mounting the assembly within the case. It would be advantageous to be able to remove the complete assembly from the case when the relay is mounted on a switchboard, thereby making it much easier to make minor repairs or complete inspection. No mention is made with regard to relay test equipment, whether or not that will be included within the case, or whether the customer must provide his own relay-testing assembly.

W. C. Morris: Mr. Goldsborough and Mr. Leyland state that relays of this type have been available for years and have a service record requiring no apologies. The good service record is easily understood, if one stops to figure out the applications which are allowed after all the limitations, given in their current handbook section covering the relay they refer to, are imposed. The only applications which are allowed are those in which the load current cannot close the directional-unit contacts, and fault current can only close the directional-unit contacts when the fault is in the protected section. These limitations apply to single lines, loop circuits, or parallel lines.

Another limitation imposed by the handbook section is that the pickup of the overcurrent unit must be set "above the maximum instantaneous asymmetrical fault current for a fault at the next bus with maximum connected capacity." This limitation means that, on the usual system with the X/R ratio equal to 10, the instantaneous peak after one-half cycle is 1.74 times the peak of the symmetrical wave; therefore, an overcurrent element set for 90 per cent of the line length on an asymmetrical current basis would cover a much smaller portion of the line on a symmetrical current basis.

As the induction-cylinder element operates on the a-c component, it is not necessary to set its pickup on the asymmetrical fault-current basis, but it can be set to protect 90 per cent of the line, regardless of whether the fault current is symmetrical or asymmetrical.

We do not believe that operation in connection with carrier-current equipment is comparable, because there is a time delay of the order of a cycle during which the elements have time to position themselves correctly. We believe the diverse viewpoints shown by different discussers merely prove again how long unsatisfactory operation can be experienced before the reasons are found and the faults eliminated. It is admitted that distance relays have added advantages, but the cost of a distance relay is considerably more than that of an instantaneous directional overcurrent relay.

We too hope that the use of instantaneous trip attachments will not be discouraged in places where they can be applied properly. As a general rule, they can be applied everywhere with overcurrent relays, but we caution against using them in directional overcurrent relays.

In the closing paragraph Goldsborough and Leyland state that the only new feature of this relay is directional control of a high-speed relay. Directional control of a high-speed relay is not new, as instantaneous plunger relays have been controlled directionally by short-circuiting their operating coil. The new feature of the relay is the proper co-ordination of the contacts which allows the relay to be applied anywhere, even if load current is in the direction to close the directional-unit contacts.

Mr. McConnell has very kindly provided the information and method of determining when voltage-restrained directional relays can be used with high-speed instantaneous overcurrent relays, and, since we believe it adds materially to the subject, we are including it.

Co-ordination of Instantaneous Overcurrent Relays With a Polyphase Directional Relay With Voltage Restraint

Assume instantaneous overcurrent relay at R set at maximum current for fault at X_1. This is when line Z_D is out and with Z_B a minimum

$$I_{R\ \max}=\frac{1}{Z_B \min + Z_C}$$

With both lines in and a fault at X_2

$$I_R=\frac{Z_D}{Z_C+Z_D}\;\frac{1}{Z_A+\dfrac{Z_CZ_D}{Z_C+Z_D}}$$

$$=\frac{Z_D}{Z_A(Z_C+Z_D)+Z_CZ_D}$$

and R will operate if $I > I_R$ max or when

$$\frac{Z_D}{Z_A(Z_C+Z_D)+Z_CZ_D}=\frac{1}{Z_B \min + Z_C}$$

$$Z_D(Z_B \min + Z_C)=Z_A(Z_C+Z_D)+Z_CZ_D$$

$$Z_DZ_B \min = Z_A(Z_C+Z_D)$$

$$\frac{Z_DZ_B \min}{Z_C+Z_D}=Z_A$$

$$\frac{Z_DZ_B \min}{Z_C+Z_D}=KZ_A$$

where

$$K=1 \tag{1}$$

With breaker at X_2 open and a three-phase fault

$$E_R=I_R'Z_C+I_DZ_D$$

and the apparent impedance viewed from $R(Z_R)$ is

$$Z_R=\frac{E_R}{I_R'}=Z_C+Z_D\frac{I_D}{I_R'}$$

but $I_R'=\dfrac{Z_A}{Z_A+Z_C+Z_B}I_D$

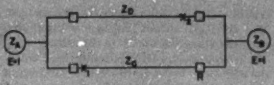

Figure 1

$$\text{or } Z_R=Z_C+\frac{Z_D}{Z_A}(Z_A+Z_C+Z_B)$$

$$Z_R=Z_C+Z_D+\frac{Z_D}{Z_A}(Z_C+Z_B) \tag{2}$$

Z_A is substituted from 1 in 2

$$Z_R=Z_C+Z_D+\frac{Z_D(Z_C+Z_B)(Z_C+Z_D)K}{Z_DZ_B \min}$$

$$Z_R=(Z_C+Z_D)\left(1+\frac{K(Z_B+Z_C)}{Z_B \min}\right)$$

$$Z_R=(Z_C+Z_D)\left(\frac{Z_B \min + KZ_B + KZ_C}{Z_B \min}\right)$$

Insofar as Z_B and K are concerned, Z_R appears to be a minimum when Z_B is a minimum $(=Z_B)$min and when $K=1$ (minimum relevant value for K, since the overcurrent relay would not operate in equation 1 for $K < 1$).
Minimum

$$Z_R=(Z_0+Z_D)\left(2+\frac{Z_C}{Z_B}\min\right) \tag{3}$$

which means that the directional relay would not operate if set for less than the Z_R of equation 3.

We believe that Mr. Smith's comments represent more nearly the true operating experience with adding instantaneous attachments, and we are grateful for his contribution, particularly in commenting on this being the first time information on one per cent of normal voltage has been presented. We hope to continue this practice and regret that we do not have time to supply him with similar curves on normal voltage at which, of course, operation is many times faster. Mr. Smith and other discussers asked for information on how the current pickup was adjustable. This is done by loosening a clamp on a collar which carries the spring and then by rotating the collar. A four-to-one adjustment range is provided, and it is possible to get any intermediate value desired. A three-phase directional unit co-ordinated with three single-phase overcurrent units, about which he inquired, has not been drawn up, but if there is a demand we should be glad to undertake this development.

We thank Mr. Benson for his comments. In answer to his question, the contacts used are our nonbounce billiard-ball type. No definite contact pressure is required to make contact, as in any induction relay, and as soon as trip current flows the holding coils assure adequate contact pressure so that no burning can occur. The core of the relays can be adjusted in the field, but it is not expected that this ever will be required. Incidentally, a smooth core is used in these relays, and it is the intention of the design to require no adjustment. Mr. Benson's request for a description of the relay case assembly can be answered simply by stating that the relay is mounted in our standard drawout case; we believe this satisfies all the requirements mentioned in his last paragraph, including testing facilities.

Mr. Dodds brings out a good point in that this relay is in the class immediately below one-step distance protection. We have not thought that it would be worth adding double windings to overcome the 15 per cent difference in measurement for three-phase and phase-to-phase faults, but this can be done easily. We appreciate the suggestion. He asked about the relay burden which is of the order of 0.8 ohm (20 volt-amperes) for a 4-to-16-ampere relay, and for a ground relay of one-half-to-two-amperes rating it is approximately 50 ohms.

Aircraft Transformers—Small and Light

Discussion of paper 43-146 by O. Kiltie, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, pages 899–902.

J. E. Yarmack (Federal Telephone and Radio Corporation, East Newark, N. J.); **E. C. Wentz** and **C. C. Horstman** (both of Westinghouse Electric and Manufacturing Company, Sharon, Pa.): Mr. Kiltie's paper on aircraft transformers is indeed a valuable contribution to the electrical art serving the aviation industry. With the advent of 400-cycle power system there is no doubt that there will be more and more requirements for aircraft transformers light in weight and small in size.

Our respective companies have collaborated in the design and manufacture of several aircraft transformers, and the results have been very gratifying. They concern the three-phase 400-cycle air-blast-cooled units used in the rectified a-c–d-c power plants generating from 200-ampere to 800-ampere 28.5-volt output.

The weights of these transformers are illustrated in Figure 1 of this discussion. Curve A is a typical weight characteristic of the three-phase transformers employing the Hipersil cores. By working these transformers up to 300 degrees Fahrenheit temperature, the weight could be reduced considerably. The conservatively established relation, however, is as shown by

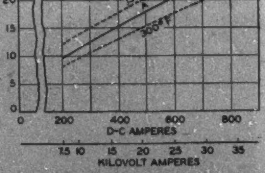

Figure 1. Weight relation of the aircraft transformer to the kilovolt-ampere rating and the rectified d-c output

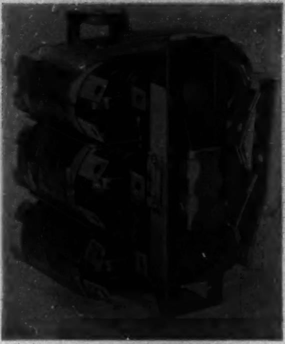

Figure 2. 7.5-kva three-phase 400-cycle air-blast-cooled aircraft transformer

curve *A*. One should not overlook the efficiency merits of these transformers as related to the output rating. With the 35-kva transformer a 97 per cent efficiency is readily obtainable; whereas, the 7.5-kva transformer will give an efficiency in the neighborhood of 92 per cent. Figure 2 of the discussion illustrates the view of a 7.5-kva 400-cycle three-phase transformer employing seven-mil Hipersil core. Its volume is 125 cubic inches and weight is 9½ pounds. With forced-air ventilation to the amount of 250 cubic feet per minute, its maximum temperature rise is in the neighborhood of 85 degrees centigrade.

Mr. Kiltie's statement in reference to the transformer-rectifier unit attracts our attention in particular. He states that the development work is well advanced on self-regulating transformer-rectifier units of 3-kw and 6-kw ratings. He also states that the 6-kw rating unit is within 40 pounds weight, and that very close regulation is accomplished by a control reactor, three-phase high-temperature transformer, variable resistor, and selenium rectifier.

We have explored various means towards the design and construction of the aircraft unit in question. The system that Federal Telephone and Radio Corporation has developed recently is the varying input at the primary of the transformer. This adjustment is accomplished by means of electronic regulator and the associated mechanism. The rectified output voltage is compared with the standard one. If the output voltage is higher than the predetermined value, the difference between this and the standard one affects the adjustment in the primary of the transformer and thus reduces the input into the rectifier. With the low output voltage the process is reversed. Figure 3 of this discussion illustrates the complete unit. It essentially consists of Hipersil-core transformer and autotransformer, selenium rectifier, cooling system, and electronic voltage regulator with its component parts. The equipment is housed in a sturdy lightweight case. The complete unit weighs approximately 41 pounds. The equipment operates from a 200-volt 400-cycle three-phase power supply. The output voltage can be selected anywhere between 26 and 30 volts and is held constant within plus or minus three tenths of a volt at any output current from zero to 200 amperes.

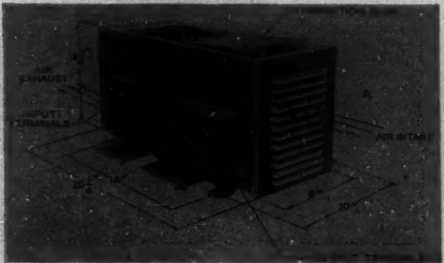

Figure 3. Aircraft transformer – rectifier unit of 6-kw rating. The unit weighs 41 pounds and is 20 inches long, $7^3/_4$ inches wide, and $9^1/_2$ inches high. It contains transformer with the primary winding arranged for variable input, cooling system, selenium rectifier, and electronic regulator

Parallel Operation of Airplane Alternators

Discussion and author's closure of paper 43-149 by D. W. Exner, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, December section, pages 755–60.

Cyril G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Mr. Exner's paper discusses what is probably the most important single consideration in the use of a-c systems for aircraft. As pointed out by him, there are three important factors to be considered:

1. Characteristics of the prime mover.
2. Characteristics of the control system.
3. Design characteristics of the alternator.

Since alternator design plays such an important role, it is in order to point out some of the lessons learned from the design of the 12.5-kva alternator of Figure 2 of the paper and the 40-kva alternator illustrated in Figure 6 of the paper.

The specifications for the 12.5-kva alternator imposed two requirements not conducive to minimum weight of active electrical parts. Rated speed was fixed at 3,430 rpm, and appreciable flywheel effect was demanded. When the 40-kva alternator was requested, the rated speed was fixed at 6,000 rpm. Moreover, the alternator designer was given practically a free rein in the choice of the most favorable proportions without regard to flywheel effect. A comparison of the results achieved is startling. The 40-kva alternator develops three times as much output per pound of active material, that is, copper and magnetic material, as its predecessor. (In this comparison, the output of the older machine was taken as 12.5 kva of load plus the excitation power.) The improvement in output per pound, of course, is due partly to the increased speed. But even after correction is made for the difference in speeds, the 40-kva alternator delivers nearly twice the output per pound per revolution. This important gain is due to a number of factors:

1. Absence of the flywheel-effect restriction.
2. Increase in rating.
3. Progress in design art.

These comparisons teach an important lesson to those who specify the alternators as well as to those who design them.

Perhaps even more remarkable is the output per pound of total weight of the alternator. If we exclude the weight of the exciter (which is an accessory) the weight of the alternator is less than one pound per horsepower of output at the five-minute rating. Even when the weight of the exciter is included, the weight is only of the order of one pound per horsepower. These figures compare very favorably with similar figures for the main engines.

W. K. Boice and **L. G. Levoy** (General Electric Company, Schenectady, N. Y.): This paper presents an excellent discussion of problems which are part of the a-c power-supply program initiated by the Army Air Forces and which are now receiving very active attention from the major electrical manufacturers.

A low short-circuit ratio indicates that the reactances of the machine are relatively high. If one of the engines driving paralleled alternators accelerates suddenly, the alternator driven by this engine will accelerate also, until the governing system can restore normal alternator speed. During this time the alternators will swing apart in angle, and an electrical torque, called the synchronizing torque, will resist the increase in angle. The value of this torque is dependent upon the reactances, particularly the transient reactance of the machine, and is comparatively low in the case of a machine with relatively high reactances. Because of time lags in the excitation system, the exciter will not be able to increase the alternator excitation quickly enough to offset the effect of higher reactances during the initial stages of the disturbance, and, therefore, higher displacement angles will result for a given disturbance. Hence, a machine with a low short-circuit ratio, being less able to resist sudden changes in angle, will have, in general, less transient stability than a machine with a higher short-circuit ratio. The ability of a machine to remain in synchronism during other momentary disturbances, such as electrical faults, is similarly affected by the short-circuit ratio.

As Mr. Exner points out, alternators with a short-circuit ratio of 0.60 have been used successfully when driven by auxiliary engines. Experience is yet to be obtained on the effect of the relatively poorer transient-stability characteristics of such alternators when driven by variable ratio transmissions from main engines. The paper emphasizes that the high acceleration rates of these engines accentuates the problem of maintaining synchronism between paralleled alternators.

While the maximum engine accelerations impose severe requirements on constant speed drives, governors, and alternators, data now available indicate that accelerations normally encountered will be materially less than the maximum possible values.

The choice of the method of synchronizing to be used on any airplanes must take into account the weight and complexity of equipment, as well as the extent of the operator's duties.

Parallel operation has certain disadvantages as well as the advantages outlined in the paper. These include the following:

1. Additional control equipment required.
2. More severe requirements for governor performance.
3. Routine attention given by operator to balancing of loads.
4. The fact that a disturbance to a paralleled a-c system disturbs the entire electric system on the airplane.
5. Larger fault currents, requiring larger and heavier circuit breakers.

It is questionable whether or not parallel operation is essential even for combat planes, although it is desirable from an operating standpoint. A-c systems should take into account possible nonparallel operation, even of systems normally to be paralleled.

Mr. Exner mentions the savings in wiring weight obtained at higher voltages. The effect of system voltage on weight of power-distribution-system copper is illustrated by the following figures, based on the same power, transmission distances, and current density.

27.5 volts direct current with ground return, 100 per cent.

120 volts direct current with ground return, 22.9 per cent.

120 volts direct current two-wire, 45.8 per cent.

120/208 volts, three-wire, three-phase alternating current, 0.75 power factor, 30.5 per cent.

These figures demonstrate why aircraft equipment for higher voltages, both a-c and d-c, have been developed.

D. W. Exner: Boice and Levoy state that a low short-circuit ratio indicates that the reactances of the machine are relatively high. Apparently, they have based this somewhat broad statement on experience with the more conventional 60-cycle designs. Actually, a lightweight 400-cycle design which has a lower short-circuit ratio and a considerably higher direct-axis synchronous reactance X_d has a transient reactance X_d' approximately one half as great as the values commonly existing in 60-cycle machines. Hence, the stability during short-time speed or load transients may be assumed to be good. Nevertheless, the transient time constant T_d' on these lightweight alternators is in the order of 0.01 second, so that the high transient synchronizing torque is effective in only the initial moment of the longer transients. It may help in releasing the overrunning clutch on a slow alternator, and it should give material stiffness in opposing engine-torque pulsations, as these involve principally upwards of 30 reversals per second.

More experience with these lightweight alternators, driven from the main engines through constant-speed drives, is necessary before the importance of the various design factors can be evaluated correctly. As I have pointed out in the paper, the condition of main engine drive introduces certain difficulties which are not encountered with auxiliary engine drive. This emphasizes the importance of co-operation in the design of the constant-speed drive and governor to minimize the natural rigidity of this coupling. I have mentioned the desirability of incorporating in the drive some degree of softness or slip proportional to load. This need not involve a direct power loss if it is obtained by a change in ratio. In some of the proposed drives this may be done by taking advantage of the inherent torque reaction upon the ratio-changing mechanism.

Boice and Levoy list certain disadvantages of parallel operation of a-c power units. The alternator control and governor weights are somewhat greater, but the increase must be considered in the light of the following factors:

(*a*). Improved alternator load factor, particularly in starting the larger motors.

(*b*). Improved fault-clearing ability, because more current is available to burn faults clear or to isolate the faulty section by blowing limiters.

(*c*). Weight of additional load-transferring equipment required for strictly nonparallel operation.

It is implied further that difficulty will be experienced in dividing load between paralleled a-c units. There is no reason for expecting a degree of load division which is inferior to that obtained with the present low-voltage d-c systems, as this function of the governor does not present a particularly difficult design problem. The paralleling control system, which is being developed, merely provides a convenient facility for the flight engineer to exercise a vernier adjustment of load division, if he so wishes, without having to leave his flight station.

The wiring weight figures given by Boice and Levoy show clearly why the larger airplanes vitally need a high-voltage distribution system. The higher weight given for the 120/208-volt a-c system is the result of the power factor. Because, at a voltage of 120 volts above ground, line drop is not a major factor, wire sizes may be selected on the basis of temperature rating. For this reason each of the three a-c line wires may have less than one third the copper section of the 120-volt d-c system because of the improved heat dissipation from the smaller-sized cable. Of course, this same principle may be applied to the d-c wiring by the use of multiple circuits in parallel.

In regard to Mr. Veinott's interesting comparison of alternator weights, he included an allowance for the self-excitation power amounting to approximately two kilowatts at the five-minute rating. This figure includes the losses of the electronic regulator.

High-Voltage-Ignition-Cable Design for Aircraft

Discussion of paper 43-145 by H. H. Wermine, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 716–19.

F. O. Stebbins (General Electric Company, Schenectady, N. Y.): Mr. Wermine's paper describes the present problems met in the design and manufacture of satisfactory high-voltage aircraft ignition cable and points out that a method for simulating flight conditions by cycling cable tests at operating temperatures under vacuum is proving of great value in determining what aircraft ignition cable will and will not do. The problem of mechanical breakage of the insulation on the ignition wire and the effects of corona in causing the ignition wire to deteriorate rapidly are described. The difficulties of removing defective ignition wire from the ignition-shielding assembly are also referred to.

The failure of high-voltage aircraft ignition cable to meet the stringent requirements of aircraft applications has been recognized for some time. Since it was recognized several years ago that a considerable period of time would be required before these problems are solved, a method for building ignition-shielding assemblies for airplane engines using wire subject to mechanical and altitude difficulties, which reduces the effects of these difficulties to a minimum, was developed and is described herewith.

Figure 1 of this discussion shows an ignition-shielding assembly for an 18-cylinder aircraft engine. This assembly consists of two parts, the manifold and the 36 flexible leads. The manifold contains high-voltage aircraft ignition cable and is filled with gel, so that it is one solid mass of cable and gel without any inclusions of air. This prevents movement of the ignition cable due to vibration and protects the insulation from mechanical stresses, and, since there is no air present inside the manifold, the formation of corona inside the manifold with resultant destructive effects on insulation cannot occur. The flexible leads which run from the manifold to the

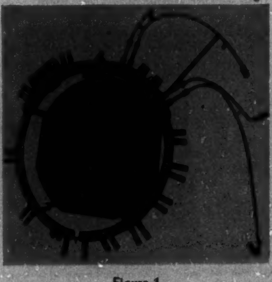

Figure 1

spark plugs are not protected from corona at high altitude and may, therefore, fail from this cause. Such a failure, however, is not difficult to repair, because these flexible leads are detachable from the manifold and can be replaced easily.

We believe that this ignition-shielding-assembly design presents a good solution to the high-voltage-ignition-cable problem on aircraft at the present time. The manifold is permanently mounted on the aircraft engine with the ignition cable protected against mechanical failure and corona, and the parts of the ignition-shielding assembly which cannot be protected suitably against these difficulties are replaceable easily in case of failure.

A. M. Ross (General Electric Company, Bridgeport, Conn.): Perhaps a further comment on the simulated engine tester mentioned in Mr. Wermine's paper may be of interest. This instrument was developed in its essential form by H. H. Race, General Electric Company research laboratory, Schenectady, N. Y., and owed its subsequent evolution to the co-operative efforts of the ignition-cable manufacturers' technical advisory committee under Mr. Wermine's able chairmanship. Introduced at a time when the causes of ignition-cable failure were obscure and the road to progress not apparent, it promptly pointed a path to the very satisfactory improvements already described to you.

In its early stages the low-pressure high-temperature high-voltage tester exposed the serious weaknesses of lacquered-braid-covered cables, demonstrating that charred-cable terminations of conventional structure had suffered even more from tracking and arc-over than from the high temperatures encountered in spark-plug wells. Lacquered-braid cables rarely withstood as much as ten hours of testing under the procedure first used, whereas some of the earliest Neoprene-sheathed cables tested exceeded 100 hours of life!

With the development of cyclic heat and pressure the simulated engine tester, never a respecter of persons, proceeded to expose faults in almost all parts of the ignition wiring system: plugs, connectors, grommets, and boots as well as cables, and it has stimulated notable improvements in the design of many of these components.

Doctor Race's equipment has yielded information so far on approximately 400 samples of various cables involving more than 13,000 sample hours of "flight" between sea level and 40,000 feet! With the latest developments of technique and equipment it is indicated that 80 per cent of the Neoprene-sheathed cables have lives longer than 100 hours of test, whereas only 20 per cent of the conventional lacquer-braid types survive this period. This estimate assumes, however, the most careful gasketing and latest improved connectors. A survey of the years' work indicates a somewhat less favorable comparison. If all types of setup and testing used are taken into account, but short-time failures of plugs or connectors are excluded, we have:

1. For 48 tests on lacquered-braid cables an average life of less than 22 hours.
2. For 64 tests on Neoprene-sheath glass-braid cables an average life of more than 131 hours.

It may be said now that the simulated engine tester is well on the way to becoming a reasonably precise means of evaluating aircraft ignition cables and should be indispensable to the development of improved cable constructions.

The success thus achieved may be credited to workers of the General Electric Company, Detroit Edison Company, Electric Autolite Company, Packard Electric Division of General Motors Corporation, and Belden Manufacturing Company who contributed to various phases of the equipment design and operating technique. The notable improvement in cables already accomplished is owed in considerable measure to many frank discussions among the cable manufacturers, the engine builders, and the various government agencies meeting in committee.

Radio-Noise Elimination in All-Metal Aircraft

Discussion and author's closure of paper 43-140 by Fred Foulon, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, pages 877–91.

C. W. Frick (General Electric Company, Schenectady, N. Y.): The excellent analysis of the problem of radio-noise elimination in aircraft presented in the paper, which pictures the chain from the influence source to the point of radio reception, brings out quite clearly the need for co-operation among the three groups: the aircraft builders, the radio manufacturers, and the electric-apparatus manufacturers. As stated in the introduction to the paper, the solution of the problem "requires that these components be considered conjointly and that each party accept his part of the total responsibility."

The electric-apparatus manufacturer's part in this solution is concerned with the radio-influence characteristics of apparatus as furnished to the aircraft builder. He does not install the wiring in the aircraft and thus has no control over such measures as might be employed therein to minimize the apparatus influence. Yet he must have some way of knowing whether his apparatus is or is not acceptable from the radio-noise standpoint. There should be a standard of acceptability based on some quantity which can be measured directly on the apparatus circuits in the factory by recognized methods. In many respects the radio-influence voltage (*RIV*), measured with the accepted radio-noise meter at the apparatus terminals, seemed to fulfill this requirement. The use of this quantity has been extensive and quite successful in connection with broadcast reception. Considerable *RIV* data have been taken on aircraft apparatus with the expectation that such information would meet the requirements. It may be noted that the author of the paper concludes that apparatus *RIV* cannot be applied, because it does not give a measure of receiver-noise output unless a direct correlation has been made. He points out that the over-all coupling between influence source and radio receiver varies widely from one installation to another. This situation has been overcome in another case by obtaining enough data on coupling to yield a representative average. Similar data on the aircraft case would be useful. Also, his objection that situations are encountered, particularly at higher frequencies, wherein noise coupling is functional with current rather than voltage does present a real problem. I have encountered such conditions in tests at ten megacycles and above.

With regard to the author's conclusion that measurements in the output of a radio receiver are the most accurate with respect to actual evaluation of the effects of radio-influence sources, I would like to point out that this reasoning is applicable only to the combination of apparatus and radio receiver as installed in aircraft. It has been my experience that measurements by the receiver-noise-output method are very useful for checking the over-all installation in an aircraft but are not applicable to the testing of individual pieces of apparatus in the factory. To get results which could be duplicated, many details pertaining to the radio set, the antenna, the circuit dimensions and spacings would have to be standardized, and the tests made in a screened room. Tests with radio sets and antennas to pick up noise produced by apparatus were tried in the early days of broadcasting but were never very successful and were abandoned when radio-noise meters became available. All this experience indicates that the receiver-output method does not meet the requirements for providing a basis of acceptability for apparatus.

It seems that the best way to proceed would be to add to our present technique whatever additional factors are required to apply the present radio-noise-meter methods to the measurement of apparatus in such manner as to make results significant to aircraft operation. A voltage basis has been used successfully in other situations. For example, in the co-ordination of power and telephone systems the telephone influence factor (*TIF*) of the voltage is the basis for the acceptability of rotating machines, even though the inductive effects are known to be associated with both voltage and current.

The first step would be to obtain as many data as possible with existing methods and equipment, including measurements with radio-noise meters on apparatus and on aircraft circuits as well as radio-receiver noise-output measurements, and to study these data jointly in order to determine the next step. It is highly desirable to bring the three parties together through some joint committee or other agency, so that each party may be given an opportunity to bring out his viewpoint. This has been done in other situations. For example, the co-ordination of power systems with wire communication systems has been carried forward successfully by joint committee organizations which were set up about 20 years ago and are still functioning. Possibly some existing committee in the aircraft field could undertake this activity either directly or through a subcommittee. It is hoped that the discussion of the subject at this time will stimulate interest and lead to some definite action.

F. O. Stebbins (General Electric Company, Schenectady, N. Y.): The radio-noise problem has always been difficult to solve, wherever it has made an appearance, and the methods of travel which radio noise uses in arriving at its ultimate destinations have in some instances appeared to be uncanny and mysterious. Radio noise travels along copper wires and metallic objects in the same manner as ordinary d-c power and a-c power. However, it seems to violate Ohm's law, when it passes from one insulated wire to another wire, when it travels right through an open switch, and when, after every effort has been made to suppress it, the radio receiver is still unusable from excessive noise caused by a resonance condition and overlooked coupling circuits, and so forth.

There are two reasons why radio-noise phenomena are confusing to the ordinary layman.

1. The frequencies involved are very much higher.
2. The amount of power is very much less than is encountered in most other electric fields.

It may be said, approximately, that ordinary power frequencies are of the order of one cycle, audio frequencies are of the order of 1,000 cycles, and radio frequencies are of the order of 1,000,000 cycles. This difference in frequency for radio frequencies as compared with ordinary power frequencies of 1,000,000 to 1 causes effects which appear to be mysterious. For example, the open switch previously referred to can be considered as a condenser with a very small number of microfarads between the blade and the pole. The impedance of this condenser to ordinary power frequencies is very high but is only $^1/_{1,000,000}$ of this high value to radio frequencies. This is a very appreciable reduction in impedance, and in some instances the amount of noise in the radio receiver has been very little different, whether the switch was open or closed.

The inductive reactance of a choke coil is proportional to the frequency of the power flowing through it. At first thought it would seem that the choke coil would be the ideal suppressor of radio noise, since its impedance should be approximately 1,000,000 times as great at radio frequencies as at power frequencies. There is just one little gremlin in such a solution. Every choke coil consists of turns of wire, and between any two turns in the coil there are a minute number of microfarads. These microfarads are essentially in parallel with the inductance of the coil. Even though the inductive reactance is increased 1,000,000 to 1 with radio-frequency power, the condensive reactance in parallel with the inductive reactance decreases 1,000,000 to 1. This condensive reactance, unless the coil is of special design, provides a low-impedance path across the coil. Choke coils must, therefore, be designed to have a minimum of distributed capacitance, and one simple design that is being widely used is a single-layer coil.

Since radio-noise frequencies are about 1,000,000 times ordinary frequencies, and induced voltage is proportional to frequency, the ability of wires exposed to those carrying radio noise to pick up that noise is increased about 1,000,000 times and is independent of the insulation between the two wires. This explains the ability of radio noise apparently to pass through insulation.

The second outstanding difference between radio-noise power and ordinary power is the actually very small amount of power involved with radio noise. For example, radio-noise voltage is measured in microvolts which are millionths of a volt. If radio noise as great as a few microvolts appears on the antennas of the radio receiver, radio reception may be impossible. The volume of this radio noise gives the person hearing it the impression that the amount of power involved is large, that is, of the order of watts or perhaps kilowatts.

In conclusion, it may be stated that persons working on radio-noise problems must always keep in mind that the frequencies involved are many times higher than those encountered on ordinary circuits, and that the amount of power involved is minutely small if a solution to the problem by rational methods is to be obtained.

Fred Foulon: A careful reading of C. W. Frick's discussion of my paper will disclose the fact that he confirms rather than disputes the major contentions of the paper. The remaining issue seems to be that, while the paper attacks the problem from the standpoint of the evaluation of the installed radio equipment (which is the only one of real value to the aircraft manufacturer who must guarantee radio performance), Mr. Frick wishes to attack the problem from the electric-apparatus standpoint. In seeking a valid yardstick for the determination of the radio-noise influence of electric apparatus, he says, "To get results which could be duplicated, many details pertaining to the radio set, the antenna, the circuit dimensions and spacings (that is, coupling factors) would have to be standardized...." "...this experience indicates that the receiver-output method does not meet the requirements for providing a basis of acceptability for apparatus." He points out further that the author's reasoning, that measurements in the output of the receiver are the most accurate with respect to actual evaluation of the effects of radio-influence source, "is applicable only to the combination of apparatus and radio receiver as installed in aircraft" but is "not applicable to the testing of individual pieces of apparatus in the factory." This is only another way of stating the author's contention that a measurement made on electric apparatus is no measure of actual radio-noise output, unless all coupling factors are known. This contention extends even further. *RIV* measurements are of little value when made on aircraft wiring or terminal points. They may begin to be of value when made on the antenna circuit of the receiver in question, but then only if the receiver is calibrated in terms of input noise voltage. The present evaluation in terms of sine-wave voice-frequency input still leaves an undetermined correlation between noise-wave and sine-wave output with which to reckon.

The measurements of coupling factors and determination of values of *RIV*, he suggests, have already been made to a certain point. This work has shown definitely that the nature and complexity of these factors and their variation is such that only basic rules can be laid down for their control, and that the degree to which these rules must be followed again varies greatly with the installation. The realistic fact is that there simply has not been time enough to organize this work through co-ordinating committees, as he suggests, and to obtain the standardization necessary to achieve the required control over apparatus, wiring practices, and design of radio equipment.

He is quite correct in stating that very extensive work is yet to be done, with respect to the determination of the various coupling factors involved between radio-noise-influence sources and actual production of radio noise in a certain receiver, could well be done through the medium of joint committee organizations. He cites the co-ordination of the radio-noise work between power systems and wiring communications systems as having been carried by joint committee organizations that were set up 20 years ago. The work of the joint co-ordination committee on radio reception of Edison Electric Institute, National Electrical Manufacturer's Association, and Radio Manufacturer's Association and an analysis of contemporary writers on this subject[1] discloses the fact that an enormous amount of time has been spent on the measurements of noise-influence voltages with respect to field strength of electric apparatus and its associated wiring; but not until 1942 has a real correlation been made between these sources of radio-noise influence and their actual effects in terms of radio-noise output of receivers. The determination of these coupling factors, as given in the AIEE paper by Mr. Frick, "Measurements Pertaining to the Co-ordination of Radio Reception With Power Apparatus and Systems," is the first evidence in some time that the final link (all coupling factors combined) in this very difficult chain is about to be evaluated. It is obvious that, until the final link is evaluated, satisfactory radio performance cannot be predicted by radio-noise-influence measurements made at any point outside the receiver.

The aircraft manufacturer finds himself working from the other end of this long chain, in that he must guarantee the performance of the radio equipment. Former specification requirements which stipulated the use of much bonding and shielding were supposed to provide an automatic guarantee of noise-free radio performance but were found unusable and burdensome to the aircraft manufacturer and services alike. The test "by fire" of having to make the radio equipment work has simply proved that the approach must be from the standpoint of cutting the noise chain at the point it can be done most economically. Many hundreds of measurements of *RIV* throughout the airplane has given us the average picture for which Mr. Frick is looking. This picture has told us simply that there are certain things we can do to avoid *RIV* coupling in the first place by proper design and layout of our wiring and radio equipment, and that some unanticipated trouble items must be hunted down in the first complete installation. We have then available to us certain simple means of suppressing the leftover noise troubles. Such correction is simple enough not to hold up the production of the airplane. Separately encased filters are used as a stop-gap measure where insufficient filtering has been incorporated in the equipment affected. These filtering elements can later be incorporated integrally in the equipment affected. In airplanes not having the benefit of the all-metal shield the coupling between wiring and antenna is undoubtedly more difficult to reduce to a satisfactory point. In this case a greater consideration must be given the actual effect of the *RIV* source.

In a specific case where extreme coupling exists between the antenna and a *RIV* source (and the frequency range under consideration is very wide), it is possible that a filter of several pounds weight representing a large percentage of the weight of the *RIV* source may be required. The same source may operate with no effect of radio noise in another installation. Hence, it is obvious that if the conditions of installation can alter the situation to such a degree that any *RIV* limits for apparatus determined as acceptable must take these factors into consideration.

The obvious solution is to obtain the lowest possible *RIV* output of any given design by avoiding practices known to produce high *RIV* but to avoid expensive suppression means until proved necessary in a specific installation. Co-ordination between the equipment manufacturer with the user of the equipment hence, is indicated at this point. To attempt to utilize suppression means that will be certain to meet all requirements is simply uneconomical and probably unattainable.

In answer to Mr. Stebbins discussion with respect to my paper, it may be noted that there is only one contention which has a bearing on the material of the paper. Mr. Stebbins holds that the problem is one of keeping the radio-noise-influence power on the airplane circuits at low values. It is exactly this approach which is untenable when all of the considerations of electric-equipment design, aircraft electric-circuit layout, and receiving equipment are considered in the fabrication of the airplane. That this approach may work well from the standpoint of the industrial radio-interference interaction among industrial equipment, communication circuits, and ground-based receiving circuits is not at all to say that it will work in the airplane where the relationships of noise influence producing electric equipment, aircraft wiring, and receiving equipment are entirely different. It is held that it is simpler to cut the chain of *RIV* coupling some place than to attempt to control the manifold *RIV* sources. This point is more fully developed in the preceding rebuttal to C. W. Frick's material.

Mr. Stebbins also fails to differentiate between radio noise which may or may not result from radio-noise influence of electric apparatus. It is not correct to assume that the determination of *RIV* at one point or another establishes the fact that radio noise will result in connected receiving equipment. The statement that "radio noise should be kept at a sufficiently low level so that the signal can be received" is quite correct; but the statement that "the problem is really one of keeping the power at radio frequency on the airplane in the order of microvolts" is definitely not in accord with the author's findings based on many tests.

REFERENCE

1. References 13, 14, 15, and 16 of the paper.

The Automatic Welding-Machine Starter and Its Relation to Maximum Utilization of Power and Facilities

Discussion and author's closure of paper 43-139 by Noel E. Porter, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 728–30.

Abe Tilles (University of California, Berkeley, Calif.): Mr. Porter effectively presents an important and timely subject. Anyone creating additional power-supply facilities in effect out of thin air is indeed the man of the hour. It would be of interest if he would compare initial expenditure for added facilities and for starters, not only in dollars but also in pounds of copper, steel, and rubber.

As I am engaged in development work at Kaiser Company Richmond yard three (sister yard to the one with which Mr. Porter is associated) on control units of the general nature discussed in his paper, I have some comments and questions falling under the headings of cost, timing, and psychological factors.

COST

The cost figures are good as representative figures. The facilities' first cost per kilovolt-ampere appears conservative, that is, low. Under present conditions high-continuity distribution systems might well cost more, perhaps more than twice the figure cited. On the other hand, our power costs figure even lower than the figure cited. At the low rates that have been on file for many years by the efficient utility serving our yard, and at the high load factor which we are able to maintain (above 80 per cent), we are reduced to the paltry expedient of saving machine-idling power at a total incremental cost of 5.63 mils ($0.00563) per kilowatt-hour.

At current rates for labor, three minutes of an operator's time cost more than power charges for three hours of a machine's idle operation. If the operator could not make two round trips in three minutes, it would be uneconomical for him to shut off his machine, quite aside from other possible objections. If such a machine is to be stopped for less than a half shift of idleness, it must, therefore, be stopped automatically. Fortunately, very inexpensive control units can be assembled.

It is to be expected that maintenance of motor main-line contactors will be increased by increased number of stops. It is also the consensus of opinion that decreasing idle generator operation will noticeably decrease the commutator maintenance required, and that this saving will outweigh other maintenance items. What is Mr. Porter's experience in this regard?

Although his general conclusions on power saving and number of machines simultaneously in operation are clearly sound, it would be of interest if Mr. Porter could cite a direct comparison between his probability calculations and observed data, preferably on a large number of machines.

TIMING

In discussing the timing, one might use the term "idle time" to refer to substantial periods of idleness, for example, periods of more than three minutes, and work time to periods when the machine is substantially in use. That is, work time is used here to include both arc time and pauses, the brief nonarc intervals normally alternating with arc time. Normally, arc times from one-fourth to two minutes in duration alternate with pauses from one-half to one minute in duration. During these pauses the welder moves to a new spot, raises his hood to inspect the weld, brush off slag, insert a new rod, and so forth. Distinct from these pauses included in the working time and taking up about half of it are the large intervals of machine idle time, when the operator takes off his hood and engages in, or waits for, other essential steps of the fabrication process, such as grinding down a surface; cutting work with a torch; lifting, measuring, and clamping additional work in place.

Study of records shows that on some work the pause, as defined previously, reaches one minute. That is, practically all the numerous nonarcing intervals are less than one minute. Practically all nonarcing intervals greater than one minute are the initial portions of much longer idle times. On other types of work the pause may be less than one-half minute. Our observation is that the length of the pause varies between operators and for various kinds of work but usually ranges from one-half to one minute. Mr. Porter's experience in this regard would be of interest to all.

It seems clear that the average delay setting desired of an automatic control unit is an interval somewhat larger than the pauses encountered. This delay should be preferably not too much larger than the pause interval, but in this regard the adjustment is not critical.

It has been our observation that an average delay setting of two to three minutes is entirely satisfactory. Some possibility of adjustment within this interval, and also beyond, is obviously desirable in a secondary way, as for the psychological satisfaction of operating departments involved. Precision in setting thisdelay interval, while it may be a source of some satisfaction, is in no way essential to materializing the benefits to be obtained from the control unit. Mr. Porter's conclusions in this regard would be of interest.

We found that satisfactory delay settings gave on the order of 15 stops per shift, and the machine disconnected 60 per cent of the total time. If Mr. Porter could give similar data on number of stops and per cent of time saved as a function of delay setting, that would be of great interest.

What does Mr. Porter estimate as an over-all delivery time for control units in quantity, if one starts from "scratch," ordering the component relays, and so forth?

PSYCHOLOGICAL FACTORS

A sensibly instantaneous start at all times is, as Mr. Porter points out, a psychological prerequisite. Has Mr. Porter encountered any classes of work on which reasonable objections have arisen to the presence of a good control unit?

In introducing units in quantity, what type of instructional program was used to avoid such difficulties as having operators "borrow" machines "because they weren't running anyway?"

Knowledge that a foreman on the upper deck can tell whether he is working or not by looking at his machine should tend to exert a slight steady pressure on the operator to keep busy. This alone might prove a most valuable item.

It is, of course, possible to go a step further in this direction and include an arc-time totalizing equipment within the control unit. Conceivably, this might apply such pressures as to arouse strong uncontrolled resentments, competition among operators for easy continuous work, and so forth. Its suitability in a yard where the total basis of operation is hour work and not piecework seems open to question.

As Mr. Porter has had experience with both of the previously mentioned degrees of incentive, his comments on this point will be of interest.

R. R. Dysart (United States Maritime Commission, Oakland, Calif.): As electrical engineer for the United States Maritime Commission on the West coast, part of my duty has been to study and analyze the benefits derived from the use of automatic welding controls. You and I have often heard the expression, "Oh, what do we care about expenses. The Government pays for it anyway." Of course that is neither patriotism nor horse-sense economy, but hundreds of people are saying it every day. But don't forget, each sum of money wasted, whether large or small, comes out of our own pockets.

Thanks to engineers and the electrical industry for the low-cost power available for public use. In some types of ships that are being built on a production basis, the cost of electric power is less than one per cent of the total cost of labor, but, nevertheless, if the one per cent represents several thousand dollars, it is to our mutual benefit to give due consideration to economies where possible. In no case should speed of shipbuilding be sacrificed for economies which would be overshadowed greatly by prolongation of the war even by a single day or hour.

Actual performance-test data obtained from a group of controls operating in connection with an area in the shipyard where detailed observations have been made presents the following benefits:

1. Conservation of critical materials, represented by a reduction in

Kilowatt demand.
Kilowatt-hours used.
Generating capacity and transmission facilities.
Maintenance.
Power-factor improvement.

It may be seen readily that each of the preceding represents real savings in dollars. The savings, as pointed out by Mr. Porter in his paper, have actually been realized; therefore, the investment in the control device contributes a saving not only in present-day critical materials but in dollars as well.

2. Conservation of critical man power.

The arc-time totalizer, which contributes to increasing production efficiency, cannot be evaluated in as simple terms as the automatic start and stop feature of the control device. However, tests are beginning to show and the welder's personal response is beginning to indicate that production efficiencies are increasing materially. The entire welding department is beginning to realize how easy it is to increase its output and to retain that improvement. A relatively small increase in footage welded per man per day represents savings in dollars and critical man-hours far greater in magnitude than the direct savings outlined in the foregoing text. Therefore, it is considered that both the automatic-control feature and the arc-time totalizer are very important contributions to the industries using welding in our all-out war program.

Noel E. Porter: The author thanks Doctor Tilles and Mr. Dysart for their constructive and interesting discussions on the paper.

It is believed that Doctor Tilles's analysis of arc time and normal working pauses is correct, particularly for horizontal welding. For vertical and overhead work, however, the normal working pauses may often be more than one minute in duration. A further substantiation of Doctor Tilles's analysis was shown in test conducted for average welding conditions with various time-delay settings. At three-minute delay, the number of starts was found to be 17 per eight-hour shift. With a delay of one-half minute the number of starts increased to an average of 80. The curve of starts versus time-delay setting bends noticeably at the $1\frac{1}{4}$-minute-delay setting point, so that the number of starts increases rapidly with settings below this value. Only a slight variation was noted in the number of starts as the average arc time was varied from 20 to 30 per cent. See Figure 1 of this discussion.

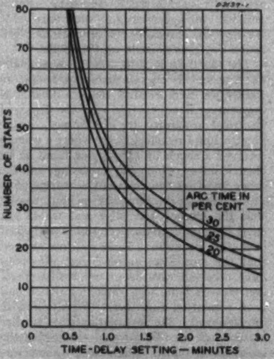

Figure 1. Number of starts versus time-delay setting during eight-hour period

Matters of impulse records, timing, wear and tear on contacts, and other specific properties of the components of various types of starters have been studied and are undergoing considerable investigation at this time. However, the very rapid increase in number of starts with reduced time-delay setting would lead one to believe that accuracy in delay setting is a prerequisite of a satisfactory unit. Preliminary results from an accelerated life test on two types of starter units reveal that wear and tear on the welding-machine line starter will exceed by far wear on the contacts of the automatic-starter relay contacts. Whether or not the reduced wear on the commutator due to lower total operating time will offset entirely the increased maintenance cost on contactors is yet to be determined fully. It is somewhat probable that the two factors will balance on a maintenance-cost basis.

H. S. Doolittle, in a recent report to the management of the Permanente Metals Corporation and to the United States Maritime Commission, gives the test results on 135 welders equipped with automatic starters. With an average arc time of 35 per cent, welding power was reduced 22 per cent. These results check favorably with the method of calculation outlined in the paper. In general, for arc times up to 30

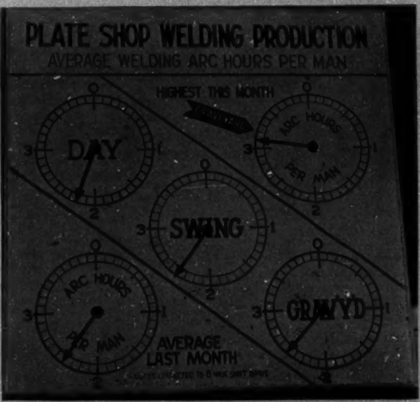

Figure 2

per cent, machines with starters will run for a total period equal to twice the arc time. Thus, for 25 per cent arc time the machines are disconnected for 50 per cent of total time. Above 30 per cent arc time, starters save half or slightly more than half the normal no-load running time.

Mr. Dysart's comments on the importance of arc time to increased production are being substantiated rapidly in our experience. Incentive systems based on arc time have been used successfully for many years. In our plate shop an arc-time board in plain view gives the average arc-hours per man for the previous day for each shift, as shown in Figure 2 of this discussion. Competition is between shifts and is based on the total number of welders for each shift and corrected to an equivalent eight-hour basis for all shifts. After this control system was instituted, in one month's time average production per man increased nearly 20 per cent. Similar control boards are being installed now in other welding production areas.

The possible detrimental psychological effects resulting from the use of arc time as a basis for welding checking and production control are contingent entirely upon the manner in which it is presented and upon labor relations and local conditions. It is believed that arc-time control, when properly applied, will increase inevitably welding production per man, because, in general, it gives the fairest index to actual welding work performed.

Effect of Frequency on Let-Go Currents

(43-134)

Effect of Wave Form on Let-Go Currents

(43-135)

Discussion and author's closure of paper 43-134 by Charles F. Dalziel, Eric Ogden, and Curtis E. Abbott, and paper 43-135 by Charles F. Dalziel, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, December section, pages 745–50 and 739–44.

J. J. Smith (General Electric Company, Schenectady, N. Y.): Dalziel, Ogden, and Abbott have given us interesting data on the measurements of the magnitude of let-go currents on a large number of subjects. Their results indicate that the value let-go current is about nine milliamperes for men and six milliamperes for women. These values are based on 60 cycles voltage and cover 99.5 per cent of the cases tested.

In their conclusions, the authors make the following statement based upon the foregoing results:

> "The reasonably safe 60-cycle current for most normal healthy adult men is about nine milliamperes; the reasonably safe 60-cycle current for most normal healthy adult women is about six milliamperes."

In a paper, "The Effect of Electric Shock on the Heart" by Ferris, King, Spence, and Williams,[1] the following statement is given in conclusion 3:

> "A current just below the threshold for ventricular fibrillation is the maximum to which man safely may be subjected. Based upon numerous tests on animals of several species comparable in size with man, this maximum current is about 0.1 ampere, for a duration of one second or more and a pathway between an arm and a leg."

Obviously, the safe current referred to in the Ferris paper is not the same type of safe current referred to in the present paper. It seems, therefore, more appropriate to refer to them as limiting values of current. It may be noted also that in the present paper some of the human subjects were shocked up to 25 milliamperes, and when the current was removed they suffered no ill effects. This would indicate that there is another limiting value of current which is of interest and which is considerably higher than the values in the conclusion of the paper, namely, that which may be applied before the effect on the human system is such that artificial resuscitation is necessary. Ferris and his colleagues suggest that this value is below the threshold for ventricular fibrillation but give no data on this point.

The fourth type of limiting current might be regarded as the threshold of feeling in connection with electric shock. This is of importance in connection with the design of electrical apparatus for the home. As an illustration, I recollect a case where a woman received a shock by having one hand on an electric mixer, which she was using, and the other hand on the door of the refrigerator. Being alone at the time, she decided, wisely, that the kitchen was alive and would not enter it again until an electrician had looked over the situation. Investigation showed that the maximum current that she could have received was about one milliampere, and this was due to a capacitor which had been placed on the device to reduce radio interference. The installation of a smaller capacitance for this purpose eliminated the sensation of shock. This example, however, indicates clearly that a limiting value of current for such household appliances should be based upon the threshold of feeling rather than let-go currents or any other type of current. A joint committee of the Edison Electric Institute, National Electrical Manufacturers' Association and Radio Manufacturers' Association[2] has studied this question and recommends, so as to avoid the possibility of a shock which may be felt, a limiting value of 0.3 milliampere for the maximum current which may flow through such a capacitor to the frame in devices which normally are used ungrounded.

Thus, we have at least four types of currents which we are interested in having definite data on:

1. The threshold of feeling:

This is of interest in connection with the design of electrical apparatus for general use. The value of this has been given by EEI, NEMA, or RMA as not to exceed 0.3 milliampere.

2. The let-go currents discussed in this paper:

These are of prime importance when we are interested in situations, such as electric fences, where it is desired to shock the subject but not to such an extent that there is danger to him if he should be alone and become unable to disconnect himself from contact with the circuit.

3. The maximum current which the subject may be able to stand without becoming unconscious and thus necessitating recourse to artificial respiration:

This value is of importance in connection with the work of electricians and engineers who, in their work, are subjected necessarily to greater possibility of contact with electric circuits and, with this knowledge, may be expected to work under conditions which will safeguard them as far as possible. I have been unable to locate any definite information on this value in the literature, but Ferris and his colleagues suggest it lies below the threshold value for ventricular fibrillation. It would be interesting to have some definite data on this subject, and I hope some of the workers in this field can fill this need.

4. Finally, the value which produces ventricular fibrillation:

This, of course, is of great interest, since the experiments indicate that artificial respiration will not help in the case of the subject who has reached this stage, and that, if recovery is to be achieved in such cases some additional measures, such as countershock, must be applied. Ferris and others give 100 milliamperes as the value for this current.

It should be noted that the foregoing values I have given are those which apply to a normal adult. If the correlation which has been indicated in certain papers between the size of the subject and the amount of current is valid for all types of shock, these values would have to be reduced for the case of children. In fact, there is on record a case[3] studied by the Oregon Agricultural College, where a current of not more than nine milliamperes electrocuted a four-year-old boy.

The subject of the effects of electric shock is quite important, and I believe it will profit us materially in our studies if we subdivide it into the four divisions that I have outlined previously, each of which is important in its own connection. Those who have a problem in this respect then will have authoritative information available, and it could be used to good advantage in dealing with any other phases of this subject.

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2. NEMA publication 105.

3. International Association of Electrical Inspectors *News Bulletin*, November 1940.

H. B. Williams (Columbia University, New York, N. Y.): The conclusions on the effect of frequency are in line with the findings of physiologists on the stimulating effect of electric current at various frequencies on isolated living tissue, such as nerve.

In view of the fact that sudden death from contact with low-voltage circuits is most often caused by ventricular fibrillation, and that the currents which have been found by animal experimentation to be necessary to produce this condition when passed from a foreleg to a hind leg on animals with chest dimensions similar to those of man are considerably larger than the let-go currents for the same frequencies, the conclusion reached by the authors that the let-go current should be regarded as the maximum to which human beings may be exposed safely seems to be a sensible one. There seems to be no question but that certain individuals may be killed by currents insufficient to stop the movements of respiration. Currents at the let-go level are painful and hard to endure even for a short time. If they are continued long they may lead to a rise in blood pressure which throws strain on the heart. In persons with a damaged heart this may result in inability of that organ to maintain the circulation, and death may then occur.

The effects at frequencies above the usual commercial power frequencies are of particular interest in connection with the increasing use of electronic devices in which current of higher frequencies is often produced, often at comparatively high voltage and with the possibility of delivering relatively large currents. Although the finished equipment is usually well protected, there may be danger to personnel engaged in development work who have to work with energized equipment without protective structures.

At the higher frequencies large currents may pass without causing stimulation of muscle or nerve, and these may cause deep heating effects. Since the heat-sensitive mechanism is located in the skin, there is the possibility of damage to internal organs by high-frequency currents, even though no very unpleasant sensation may be apparent. The currents necessary to produce this effect probably would be of the order of an ampere or more.

Not only the general public, but also electrical workmen should be cautioned against careless contact with any power line. The impedance between points of the human body beneath the skin is of the order of 300 to 400 ohms for 60-cycle alternating current. Hence, it is evident that the only thing which protects against severe and dangerous electric shock on contact with a 120-volt power line is the high resistance of the dry uninjured skin.

Knowledge that thorough wetting of the skin lowers its impedance and produces a hazard is becoming general, but it should be emphasized that cuts, scratches, or abrasions of the skin, or broken blisters, may have an even greater effect in lowering resistance than mere wetting. Electrical workers should protect carefully such minor injuries.

Much can be done for the protection of the general public by correct design and construction of equipment, but those whose duty it is to install and maintain such equipment must be aware not only of the well-known danger of contact with circuits of high voltage, but also of the exceptional but still not uncommon, conditions which make contact with any power circuit hazardous.

With reference to "Effect of Wave Form on Let-Go Currents," where alternating current of frequency 100 cycles or less is superimposed on direct current, it undoubtedly is true that the stimulating effect depends on the crest value. In attempting to predict the effect of current pulses of very short duration we must take account of the fact known to physiologists that as the duration of a current diminishes its strength must be increased in order to produce stimulation, and as the duration becomes very small this increase must be very great, finally becoming so great that destruction of living substance occurs before it can respond. Sinusoidal currents of frequency half a megacycle and upward are used for heating living tissue without stimulation. This might lead one to suspect that single-current pulses lasting but a few microseconds might be harmless, and even their repetition might not stimulate. This is not true when the peak voltage and peak energy of such pulses are high and when they are repeated about 200 times per second. In view of the extensive use of electronic equipment generating such pulses, it is important that personnel be protected from contact. It is especially important to guard engineering personnel engaged in development of such equipment, since they have to work on exposed circuits. The dangerous character of such pulses has been determined by animal experiment. When the war is over it would seem highly desirable to investigate the effect of alternating and pulsating currents which depart very greatly from the sinusoidal form and, particularly, of pulses of approximately square-topped form of short duration, but of high peak voltage and energy.

It would be desirable also to investigate the effect of condenser discharges with various time constants. Underground power cables operated at 10,000 volts are capable of delivering a powerful shock, and even after momentary grounding a considerable afterdischarge may be delivered.

H. W. Bousman (General Electric Company, Schenectady, N. Y.): We all are indebted greatly to the authors for their persistence in measuring let-go current on enough subjects to get information which can be treated by statistical procedures. These data will be used undoubtedly as a basis for design where electric-shock hazard must be controlled. There are two points arising in the interpretation of these test results which I should like to discuss:

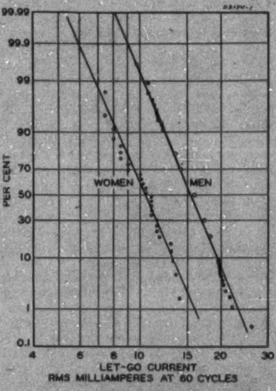

Figure 1. Per cent of adults with 60-cycle rms let-go current less than value shown

Redrawn from Figure 1 of "Effect of Frequency on Let-Go Currents"

1. The authors have based their conclusions on a value to protect 99.5 per cent. This leaves one half of one per cent unprotected. It may not be feasible to protect every person, but it seemed desirable to see how the current might vary if a different value for the percentage protected were taken. By extending Figure 1 of the paper to 99.99 per cent, we find that for this value of protection the current for men at 60 cycles should be approximately six milliamperes which is appreciably below the value of nine milliamperes given by the authors.

2. In Figure 1 of the paper the results on the subjects who withstood least current and most current, respectively, depart most from the best straight line. This suggests that some function other than linear correspondence should be used to describe the physiological response to stimulus by electric current. When the precedent is used that responses to stimuli of other sorts, such as light and sound, show approximate proportionality to the logarithm of the stimulus, it seemed worth while to replot in terms of the logarithm of the current. Figure 1 of this discussion is so plotted and shows the points, including the extremes, to lie very nearly on a straight line, thereby empiricially justifying the assumption of logarithmic response. By so replotting in Figure 1, we find the let-go current which will protect 99.5 per cent is ten milliamperes and that which will protect 99.99 per cent is eight rms milliamperes. The corresponding limits for women are seven and five milliamperes. This discussion refers to adults only. It is probable that children would experience a considerably lower limit of let-go current.

J. E. Wise (nonmember; the State of Wisconsin Industrial Commission, Madison, Wis.): The information presented by Dalziel, Ogden, and Abbott is very interesting, especially in view of the regulations with regard to electric fences which have been adopted by the Industrial Commission of Wisconsin. Our earlier study of the problem was limited almost entirely to fence controllers delivering an alternating or a pulsating current. A study of the literature with regard to hazards of electric current and consideration of the available units resulted in the rules which, with an interrupted a-c controller, limit the current to 25 milliamperes with a shock interval of not to exceed 0.1 second and an "off" period of at least 0.9 second. For a battery-operated unit of the inductive-discharge type, the maximum peak current of 500 milliamperes and a total current-time product of three milliampere seconds have been specified. For the capacitor-discharge unit we have the same general requirements and have indicated that the current between shocks shall not exceed five milliamperes. Complaints have been made that this current is so small as to make it impossible to maintain a charge on the capacitor in cases where there may be a leakage due to either vegetation or moisture on insulators.

It seems, from the material presented in this paper, that it may be possible to increase the allowable current between shocks without the danger of a person or animal being "frozen" to the fence, and by proper regulation of the charging current the discomfort may be reduced.

A further study of the paper undoubtedly will be of great interest and will aid in preparing workable regulations.

J. A. Dickinson (nonmember) and **F. B. Silsbee** (National Bureau of Standards, Washington, D. C.): The pair of papers by Professor Dalziel and his colleagues on the effect of the frequency and of the wave form of an electric current on the shock felt by a human victim forms a valuable rounding out of the work which he has been carrying on in this field for several years. It is to be hoped that, when peace permits the resumption of such researches, these studies on continuous currents will be supplemented by similar work on electric transients of short duration. There is at present a serious dearth of information on the tolerance limits and physiological effects of such transients.

With modern techniques using the cathode-ray oscillograph, it should be possible to measure such effects and to correlate them with various times in the heart cycle.

A rigorous mathematician might question the validity of concluding as a general theorem that "let-go currents were controlled by the peak of the current wave" on the basis of only four types of wave.

1. Sinusoidal.
2. Triangular.
3. Peak produced by third harmonic only.
4. Dimple produced by third harmonic only.

For instance, would a sharp peak of a few microseconds' duration superposed at the crest or at the zero of the main wave be felt? However, such freak waves seldom would be encountered.

The deductions made regarding safe or "reasonably safe" currents for children would appear to be based on very limited data. Admittedly, it is hard to get children, particularly small children, to serve as human guinea pigs. If we are to assume that a healthy child can stand the same current density through an arm or leg as can an adult and assume geometrical similarity, the comparison of a child weighing 30 pounds with an adult weighing 150 pounds would predict a factor of almost 1 to 3 between their let-go currents. Further, a child's skin is thin and generally more moist than that of an adult and would, therefore, have a lower-contact resistance. It would seem to us that a factor of 3 or 4 would be more in order than the factor of 2, which the authors have suggested between the safe currents for children and for adults.

Small children are inherently curious and often mischievous. The most dangerous age as far as traffic accidents are concerned has been found to be the four-to-five-year-old group. Such children are active, prone to run about and climb, but have yet to learn caution. Electric circuits (rural supply wires as well as electric fences) must be kept safe from children as young as this. Further, in the case of electric fences, the worst possible exposure must be anticipated. The death of a small girl in the West who was killed through contact with an electric fence while her feet were in an irrigation ditch, and the similar fatality of a small boy in Pennsylvania who lifted a charged fence wire while crossing a brook, indicate the type of exposure which must be considered in designing electric-fence controllers. It would not seem desirable from the data available to set the "safe" value for children as high as 50 per cent of that for adults.

The reader of these papers should be careful to avoid the pitfall of considering that the stimulus in the case of superposed alternating current and direct current is dependent solely on the crest values shown at *A* in Figure 1 of paper 43-135. Points on the upper curve of Figure 2 (43-135) do not correspond to a constant value of this crest, but to a value which is less, as the a-c component forms a greater part of the total.

An interesting point brought out by the deviation curves of paper 43-134 is that the "founding fathers of our nation" were somewhat wrong in their postulate that "all men are created equal," since some men can let go twice as much current as others. However, in the d-c trials of Figure 12 of this paper, where the criterion is mental rather than physical, all men do show up more nearly alike.

Charles F. Dalziel: The many helpful suggestions and interest given this research are appreciated greatly. Suggestions of new topics worthy of consideration will be given attention when conditions brought about by the war permit resumption of the investigation. Because of time limitations, the author is unable to comment in detail on the controversial and interesting points raised by discussers. For discussion of thresholds of feeling by the writer, refer to reference 1.

REFERENCE

1. PRACTICAL ASPECTS OF ELECTRIC SHOCK, *Electrical West*, March 1941, pages 40–5.

A New Simple Calculator of Load Flow in A-C Networks

Discussion and author's closure of paper 43-152 by Waldo E. Enns, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, December section, pages 786–90.

W. Ridgway (General Electric Company, Schenectady, N. Y.): The calculator described in this paper is designed to give direct readings of real and reactive power flow in the resistance network representation of an a-c network. The method of a-c network analysis by resistance networks has been described previously in papers by W. C. Hahn[1] and Waldo E. Enns.[2] The methods outlined in these papers differ in the values set up in the resistance networks, but both depend on the basic concept of resolving the a-c network into two resistance networks to which inphase and out-of-phase currents are applied to obtain voltage drops and real and reactive power flow. Direct readings of real and reactive power avoid the lengthy calculation necessary to obtain these quantities when this method of analysis is applied on a d-c board.

In order to obtain real and reactive power it is necessary to solve the following equations:

$$\text{Real power} = 3E_p(V_p/M + V_q/N) + 3E_q(V_q/M - V_p/N)$$

$$\text{Reactive power} = 3E_p(V_q/M - V_p/N) - 3E_q(V_p/M + V_q/N)$$

In the calculator these equations are solved by using a special wattmeter to perform the indicated operations. This requires a complicated connection and switching arrangement which must be set up for every problem.

The analysis of an a-c network by this calculator depends on the assumption of the correct values for E_p and E_q at all generator and load busses. There is only one correct value of E_p and E_q everywhere to give the correct solution for assumed values of loads and generation. The initial adjustment of the network for a previously unexplored problem would be very difficult, since all values of E_p and E_q are interdependent and may not converge rapidly.

No provision is made for direct measurement of current flow in the calculator. Under this condition short-circuit studies cannot be made.

There is no doubt that the resistance network method of a-c network analysis has merit for problems of limited scope. However, the calculator described, with its complicated connection and switching arrangement and the difficulty in obtaining correct values of E_p and E_q, would appear to offer little advantage over the previously discussed resistance network analysis of a-c networks using the d-c board, particularly since short-circuit studies are possible on the d-c board and cannot be made on this calculator.

REFERENCES

1. LOAD STUDIES ON THE D-C CALCULATING TABLE, W. C. Hahn. *General Electric Review*, part I, July 1931, page 444; part II, August 1931, page 482.

2. METHOD FOR A-C NETWORK ANALYSIS USING RESISTANCE NETWORKS, Waldo E. Enns. AIEE TRANSACTIONS, volume 61, 1942, December section, pages 875–80.

F. C. Poage (Ebasco Services, Inc., New York, N. Y.): In discussing this paper by Mr. Enns, because of its approach to the subject of power-system network analysis via the consideration of voltage drops, I wish to call attention to a much-neglected feature of all network analysis, namely, checking the results. Further, I wish to present a simple method of check and adjustment which can be applied to the results of any power-system flow analysis. This method can be applied regardless of how the analysis was made, whether by a complete a-c calculating-board study, wherein all parts of the system are set up and loaded simultaneously on one of the few large analyzers available in this country, by piece-by-piece board studies such as these described by Mr. Enns in this and his previous paper, by detailed computation, or by outright guess.

The check consists of determining how nearly the algebraic sums of the inphase and quadrature voltage drops measured in one direction around each loop of the network add up to zero. The adjustment consists of determining the separate inphase and quadrature amperes or volt-amperes which must be circulated in the respective loops to balance the loop voltage-drop components to zero. The inphase and quadrature circulating amperes or volt-amperes thus found are added algebraically to the loads in each part of the loop to determine the correct loads in the various sections of the loop.

As an example, assume the simplest of cases, two parallel lines 1 and 2 connecting busses *A* and *B* and operating at 40 kv to neutral, or approximately 66 kv. Copper sizes, conductor spacings, and route mileages are such that line 1 has 8 ohms resistance, 6 ohms reactance, and 10 ohms impedance. Line 2 on a shorter route and having larger conductor has 3 ohms resistance, 4 ohms reactance, and 5 ohms impedance. The load is 5,400 kw and 3,600 kilovars from *A* to *B*.

Table I

Inphase Drop		Quadrature Drop	
$I_{P_1}R_1 = 15\times8$	120	$jI_{P_1}X_1 = 15\times6 =$	90
$I_{P_2}R_2 = -30\times3 =$	-90	$jI_{P_2}X_2 = -30\times4 =$	-120
$I_{Q_1}X_1 = 10\times6 =$	-60	$-jI_{Q_1}R_1 = -10\times8 =$	-80
$I_{Q_2}X_2 = -20\times4 =$	-80	$-jI_{Q_2}R_2 = 20\times3 =$	60
Errors (volts)	10		-50

Table II

Inphase Drop		Quadrature Drop	
$I_{P_1}R_1 = 15\times8 =$	120	$jI_{P_1}X_1 = 15\times6 =$	90
$I_{P_2}R_2 = -30\times3 =$	-90	$jI_{P_2}X_2 = -30\times4 =$	-120
$I_{Q_1}X_1 = 10\times6 =$	60	$-jI_{Q_1}R_1 = -10\times8 =$	-80
$I_{Q_2}X_2 = -20\times4 =$	-80	$-jI_{Q_2}R_2 = 20\times3 =$	60
Errors (volts)	10		-50
$\Delta_1 I_P R_L = 3\times11 =$	33	$\Delta_1 I_P X_L = 3\times10 =$	30
	43		-20
$\Delta_1 I_Q X_L = -3\times10 =$	-30	$-\Delta_1 I_Q R_L = 3\times11 =$	33
	13		13
$\Delta_2 I_P R_L = -1.2\times11 =$	13.2	$\Delta_2 I_P X_L = -1.2\times10 =$	-12
Final errors (volts)	-0.2 negligible		1 negligible
$\Delta I_P = 1.8$ amperes			
$\Delta I_Q = -3.0$ amperes			

Table III

Inphase Drop		Quadrature Drop	
$I_{P_1}R_1 = 16.8\times8 =$	134.4	$jI_{P_1}X_1 = 16.8\times6 =$	100.8
$I_{P_2}R_2 = -28.2\times3 =$	-84.6	$jI_{P_2}X_2 = -28.2\times4 =$	-112.8
$I_{Q_1}X_1 = 7.0\times6 =$	42.0	$-jI_{Q_1}R_1 = -7.0\times8 =$	-56.0
$I_{Q_2}X_2 = -23.0\times4 =$	-92.0	$-jI_{Q_2}R_2 = 23.0\times3 =$	69.0
Final errors (volts)	-0.2		1.0

Table IV

Line	Power (Kilowatts)	Reactive (Kilovars)
1	2,016	840
2	3,384	2,760
Totals *A* to *B*	5,400	3,600

The first and simplest assumption is that both components of the load divide between the two lines in the ratio of 2 to 1, that is, as the reciprocals of the impedances. Under this assumption the currents are as follows:

$$\text{Inphase current}\begin{cases}\text{Line 1}-I_{P_1}=15\text{ amperes}\\ \text{Line 2}-I_{P_2}=30\text{ amperes}\end{cases}$$

$$\text{Quadrature current}\begin{cases}\text{Line 1}-I_{Q_1}=10\text{ amperes}\\ \text{Line 2}-I_{Q_2}=20\text{ amperes}\end{cases}$$

The voltage drops around the loop can be written as shown in Table I.

It is obvious that if a current of proper magnitude and phase is circulated around the loop and is superimposed upon the assumed currents from *A* to *B*, the errors found in the voltage-drop check may be cancelled, and correct current values determined for each line.

Since, in this example, the greater error is in the quadrature drop, and the greater part of this drop is in the I_PX group, the first step is to divide the volts, +50 necessary to reduce the error in this column to zero, by the loop reactance 6 + 4 = 10 ohms, which gives a correction current of $\Delta I_P = 5$ amperes to be applied. This can be multiplied by the loop resistance $R_L = R_1 + R_2 = 11$ ohms, which gives a correction of +55 volts to be added to the error of the inphase drop and +50 volts to be added to the quadrature-drop error.

By this correction we have wiped out the error in the quadrature drop, but we have increased the error of the inphase drop to 65 volts. Inspection of this result indicates that we obviously have overshot our inphase current correction, and, also, that we must make a similar correction of opposite sign in the quadrature current.

If we designate the loop resistance as R_L, the loop reactance as X_L, and the successive corrections by Δ_1, Δ_2, and so forth, and add them to those in Table I, the work sheet for determining these corrections looks about as shown in Table II.

The corrected values for the inphase and quadrature currents in each line are thus

In line 1 $I_P = 16.8$ amperes, $I_Q = 7.0$ amperes
In line 2 $I_P = 28.2$ amperes, $I_Q = 23.0$ amperes

When these corrected values are used, the original list of voltage drops can be repeated as indicated in Table III.

If the original assumption of division of power and reactive between the two lines is corrected, the corrected division is as indicated in Table IV.

In more intricate cases, such as that shown by Mr. Enns in his Figure 1 in which one circuit is part of two or more loops, the checkup becomes more complicated, because the various loops must be considered one after another, and the readjustments may have to be repeated. The process may be likened to that of determining the value of a mathematical series in which successive terms give smaller and smaller increments, and the practical value of the series is determined by the first two or three terms.

In cases where neither component of the voltage drop across the loop exceeds a small value, say, five per cent of line voltage, it is practical to use values of kilowatts and kilovars instead of inphase and quadrature amperes.

Glenn W. Bills (Bonneville Power Administration, Portland, Oreg.): Mr. Enns is to be congratulated for his ingenuity in designing a calculator which is not only easy to use but has the merit of being fairly accurate. Although only the actual solution of network problems with the aid of this calculator can verify it, I believe Mr. Enns' analyzer will save much time in determining voltages and load flow in a system, as compared with the usual analytical method involving a cut-and-try process. From experience on an a-c network analyzer I know that board operators can become very proficient in estimating terminal conditions on a-c networks, and the same thing should apply to this simple calculator. A very nice feature of the calculator is that, once the complete connection diagram is set up, only switches must be manipulated to obtain readings, even though only a few resistors are necessary.

Waldo E. Enns: The author expresses his appreciation to W. Ridgway, F. C. Poage, and Glenn W. Bills for their fine discussions. Mr. Ridgway refers to the paper by W. C. Hahn which also outlines a method by which load-flow problems may be solved with resistance networks using the usual form of d-c board. However, it should be noted that the method employed by Mr. Hahn was very different in concept from that employed by this new calculator.

Reference is made by Mr. Ridgway to the complicated connections and switching arrangement required for this new calculator. This may appear to be the case when one looks at the complete diagram; however, it should be noted that the plugging or connection diagram for this type of calculator is simple, so that the actual setup of a network would be an easy matter for the operator. As may be noted in the paper, the speed with which an operator can converge to the correct values of E_p and E_q to obtain the desired loading condition on a network under study cannot be known until the calculator is built and the best technique developed.

The discussion by Mr. Poage is of interest and demonstrates a technique of computing load flow in loop circuits which may be used to good advantage when a network is not too complex.

The discussion by Mr. Bills indicates that he believes that this simple calculator may prove capable of saving much time as compared with the usual analytical methods, and the writer believes that he is correct in this conclusion.

Lightning Surges Transferred From One Circuit to Another Through Transformers

Discussion and author's closure of paper 43-133 by P. L. Bellaschi, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, December section, pages 731–8.

J. H. Hagenguth (General Electric Company, Pittsfield, Mass.): The principal value of the paper is that once more the electrical profession is made aware of the fact that transformers are not a barrier to lightning surges passing from the high-voltage lines to the low-voltage circuits.

The principal simplification of equations given in the paper appears to be in the omission of the surge impedance of the primary lines. As shown in Figure 5 of reference 1 of the paper, this is permissible provided the transformer ratio is of the order of 4:1 or higher. For lower ratios the calculation will give values which are too high. In a previous paper[1] I have given three simple equations with two charts which allow for computing transferred maximum voltages as well as rate of voltage rise on the secondary side for different fronts and tails of the applied wave. I wish to make a correction at this time. Equation 3a of that paper should read

$$\frac{\alpha^2}{\omega_o^2}=\frac{\frac{3}{4}\left(\frac{T_2}{3}+C\,\frac{Z_1/r^2Z_2}{Z_1/r^2+Z_2}\right)}{T_2CZ_2}$$

In my paper it is clearly pointed out that, so far as the electromagnetic component is concerned, only the normally known values of per cent reactance, frequency, kilovolts, and kilovolt-amperes are required, besides an approximate knowledge of the primary and secondary surge impedances.

I believe it is also worth while to call attention to the work of L. V. Bewley[2] with respect to this topic. While his paper is of a much more theoretical nature, it is well to include it in a bibliography for those who wish to study the subject more intimately.

It would seem that Mr. Bellaschi considers a rotating machine winding as an inductance, so far as the transfer phenomena are concerned. It seems that most investigators of transient behavior of generator windings are agreed that these windings act primarily as a surge impedance.[3] Since surge travel is relatively slow in machine windings (about 100 feet per microsecond), the surge-impedance character should persist in most machines during the length of time represented by a lightning wave.

Table I

L_L	Crest Voltage: Calculated in Paper	Crest Voltage: Calculated Using C, H-L	Crest Voltage: Experiment (First Crest)
0.000354	1.2	1.08	1.1
894	2.4	2.07	2.2
1,727	3.4	2.92	3.08
4,400	4.6	3.98	4.1
∞	5.8	5.2	5.2 from Table II

The calculated and experimental test values of Table IV of the paper would check much more closely if the capacitance between high-voltage and low-voltage winding were taken into account, and the first crests of the oscillation were used in all cases (see Table I of this discussion).

Although the electrostatic component has been mentioned in Mr. Bellaschi's and most other papers on the subject, one phase of it has not been given sufficient attention. Under certain conditions of transformer operation, the low-voltage winding and the connected apparatus are not connected to ground through resistance. If the capacitance of the low-voltage network is low, then the electrostatic transfer of voltage from high-voltage winding to low-voltage winding becomes the predominant factor. The capacitance relation cannot be determined customarily from the usual known transformer specifications. In most cases the electrostatic voltage thus transmitted may exceed by a large amount the electromagnetic component, and the voltage induced in the low-voltage winding may exceed considerably that calculated by using the ratio of the insulation test levels, particularly when the turn ratio of the transformer is high. In all such cases particular attention should be given to the insulation level chosen for the low-voltage winding and its connected apparatus. Special protection in the form of grounding resistors, arresters, or capacitors may be required in such cases.

REFERENCES

1. LIGHTNING PROTECTION OF GENERATOR-TRANSFORMER UNITS, J. H. Hagenguth. *Electrical World*, February 1, 1936.

2. TRANSIENT OSCILLATIONS OF MUTUALLY COUPLED WINDINGS, L. V. Bewley, AIEE TRANSACTIONS, volume 51, 1932, pages 299–308.

3. References 7, 9, and 10 of the paper.

P. L. Bellaschi: The simplifications to which Mr. Hagenguth refers are recognized in the paper but certainly can stand further stressing. For instance, a surge transferred from the low-voltage side to the high-voltage side of a transformer is a possibility, and, in such case, it is apparent from the paper and the appendix that the more general solution is required. The question of high induced electrostatic effects does, of course, arise if the terminals are floating, as shown in Figure 12 of the paper, or, for instance, if simultaneous surges are applied to the two terminals, as illustrated in Figure 13 of the paper. Whether rotating machines can be considered as an inductance after a certain length of front is attained, theoretically may still be a question of discussion which hardly can be disposed here.

I heartily agree with Mr. Hagenguth in emphasizing the potential hazard seemingly nonexposed circuit can be subject to as the result of transferred surges. This condition has been recognized by the AIEE lightning arrester subcommittee in its recent report.[1]

REFERENCE

1. REPORT ON APPLICATION OF LIGHTNING PROTECTIVE DEVICES IN WARTIME, AIEE lightning arrester subcommittee. AIEE TRANSACTIONS, volume 62, 1943, September section, pages 586–8.

The Effects of Mutual Induction Between Parallel Transmission Lines on Current Flow to Ground Faults

Discussion and authors' closure of paper 43-151 by John I. Holbeck and Martin J. Lantz, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, November section, pages 712–15.

Waldo E. Enns (Portland General Electric Company, Portland, Oreg.): This paper is a valuable contribution, because it proves by means of actual examples that the mutual effect between parallel transmission circuits cannot be neglected in many instances without getting into difficulty with incorrect relay settings.

As is indicated in the paper, it is possible to use a d-c board for short-circuit studies involving mutual induction, when the parallel circuits are operating at the same voltage. However, when parallel lines are operated at different voltages, it is necessary to use the a-c board for the setup or to make an analytical study of considerable complexity.

Those of us who do not have ready access to an a-c board for this type of study should bear in mind that, when the mutual effects are neglected on the d-c board, there may be sufficient error in the distribution of the current to result in relay settings which will give false tripping. When this is known, if faulty relay action does occur at points where the mutual effects have been neglected, it would appear advisable that a study should be made on an a-c board to determine the correct current distribution, so that more nearly correct relay settings may be determined.

John I. Holbeck and Martin J. Lantz: With reference to the closing remarks in Mr. Enns' discussion of our paper, we wish to point out that the present d-c calculating board of the Portland General Electric Company could be changed to a-c operation provided suitable meters are obtained. In this connection, a modified d-c board converted to a-c operation has been developed by the Bonneville Power Administration and is described in a paper entitled, "Bonneville Power Administration's Short-Circuit Calculating Board," by Martin J. Lantz and Orin A. Demuth. This paper was presented before the Portland chapter of the AIEE, in the technical papers competition in May 1943.

Whenever one of two parallel lines (same voltage) are tapped, it is necessary to use an a-c network analyzer to determine the current values for a single phase-to-ground fault on the tapped line after one end of the line has opened by relay. As mentioned in our paper, an a-c network analyzer must also be used to determine the magnitudes of the ground currents for those cases in which two or more lines of different voltages occupy the same right of way.

Application of 720-Cycle Carrier to Power-Distribution Circuits

Discussion and author's closure of paper 43-141 by J. L. Woodworth, presented at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943, and published in AIEE TRANSACTIONS, 1943, pages 903–15.

R. J. Schonborn (Bureau of Power and Light, City of Los Angeles, Calif.): Mr. Woodworth's paper describes very ably and completely the theory of the application of 720-cycle carrier to 60-cycle power-distribution circuits under both the shunt- and series-type of coupling.

The Bureau of Power and Light of the City of Los Angeles has in operation three installations of 720-cycle carrier-current-control equipment, two of the shunt-coupled type and one of the series-coupled type.

The three distributing stations in which the transmitters are installed are supplied with energy at 34,500 volts, 60 cycles, which is transformed to 4,800 volts three-phase, three-wire, for distribution over underground and overhead radially connected feeders.

The two shunt-coupled carrier transmitters were placed in operation in the summer of 1934 and are installed one in each of two distributing stations located in the Hollywood district.

One of these distributing stations has an installed transformer capacity of 35,000 kva, 23 feeders, and a connected load of 1,315 kw of constant-current street-lighting transformers; the other station has an installed transformer capacity of 22,500 kva, ten feeders, and a connected load of 926 kw of street-lighting transformers.

Each shunt-coupled carrier transmitter derives its energy from a motor generator set consisting of a 50-kva 720/480-cycle three-phase alternator driven by a 75-horsepower two-speed induction motor. The 720-cycle frequency is used remotely to control ornamental street-lighting circuits, the 480-cycle frequency being reserved for possible future control of water-heater load.

The bureau's experience with the shunt-coupled installations during their nine years of operation has been very satisfactory. Initially, there were a few false operations of field controllers due to interference between stations, while both were transmitting simultaneously. This trouble has been corrected by operating on a schedule with only one station transmitting at any one time.

The motor generator sets as purchased were connected by *V* belts in order to facilitate the future adjustment of machine speeds resulting from changing the then existing 50-cycle system frequency to the present 60-cycle value. After the frequency change some faulty operations were obtained which were traced to low carrier frequency due to belt slippage occasioned by imposing the carrier on the 60-cycle system at time of peak load, thereby heavily loading the alternator. This trouble was eliminated by direct coupling of the motor to the generator, which became feasible only after the system was converted from 50 to 60 cycles.

The series-coupled installation was purchased during the present emergency and recently placed in operation primarily to provide a means of blacking out quickly a number of ornamental street-lighting circuits in the event of an air-raid alarm. Previously, each of these circuits was controlled by a time clock, and, in case of an air raid alarm, the individual switch at every street-lighting transformer had to be opened manually in the field by a volunteer-citizen worker living nearby.

The series-coupled transmitter installation is located in a station having 30,000 kva of transformer capacity, 13 feeders, and a connected load of 1,167 kw of constant-current series street-lighting transformers.

Carrier energy at 720 cycles is generated by a self-driven self-excited single-unit induction-frequency converter. This single frequency is used not only to control remotely ornamental street-lighting circuits for regular and black-out operation but also for off-peak water-heater control, so that the station and system peak loads will not be increased by the water-heater load.

The operation to be performed is selected through coded impulses in the case of street-lighting control and through a single impulse of two different lengths for water-heater on-and-off control. Control may be exercised over feeders singly or in groups of three simultaneously.

The minimum 720-cycle operating voltage required at the field controllers varies from 1.25 to 1.5 volts. Tests have been made at eight field locations selected to represent a full range of distances from the transmitter to determine the value of the 720-cycle voltage actually impressed at the controller terminals under single-feeder and three-feeder group operations. The voltage actually obtained varied from a low value of 3.7 volts to a high value of 5.1 volts, depending upon the distance from the station and the 60-cycle load on the station. These values are well within the operating values of the field controllers. The corresponding voltages under single-feeder and three-feeder operation at each of the eight locations differed by a maximum of 0.4 volt.

The 720-cycle voltage impressed at the transmitting station during a single-feeder operation varies from approximately 4.75 volts at one-sixth feeder load to approximately 5.4 volts at full load with somewhat lower values for three-feeder-group operation.

Extensive tests were taken under various operating conditions to determine the value of interference or stray voltages on feeders adjacent to those on which carrier current was being impressed. The interference voltages at the station bus varied from approximately 0.1 volt at one-sixth feeder load to approximately 0.65 volt at full load. These values are too low to cause faulty operation of field controllers.

After the values of various capacitances were adjusted to obtain correctly tuned circuits, the performance of the station equipment proved to be satisfactory. The field controllers have been installed just recently and are now in the course of adjustment. As the coded impulses received in the field are of more than sufficient strength to operate the tuned circuits of the controllers, it is believed that after mechanical adjustments are completed the over-all operation will be satisfactory.

Compared with the shunt system, the series system has greater flexibility and selectivity in that an operation may be transmitted over a single feeder or a group of three feeders, whereas in the shunt system all operations are transmitted over all feeders connected to the bus to which the transmitter is coupled.

J. P. McKearin and **F. R. Longley** (Western Massachusetts Electric Company, Springfield, Mass.): The Springfield division of the Western Massachusetts Electric Company has for a number of years used 720-cycle carrier control on its street lamps. A single three-phase transmitter connected by shunt coupling to the 13.8-kv bus at a station near the center of load delivers the control signal over a network of 13.8-kv underground transmission cables to eight stations and substations and to a low voltage network. At these points it is stepped down to the distribution lines in the city of Springfield, Mass., and four adjoining towns. The territory thus covered has a population of 188,000 and a combined area of 113 square miles. A spare and duplicate transmitter is maintained at another location in a substation also near the center of load.

The present control system was installed in 1935, and, therefore, we have had eight years' experience in its operation. Prior to that time, beginning in January 1931, we had used 720-cycle control in one substation area.

The transmitter operates the switches of about 5,300 lamps out of a total of 6,600 in the territory now under carrier control. Of the remaining 1,300 lamps 750 are controlled by pilot wire, which was in use before the time of the carrier system, and 550 are on series circuits connected to constant-current transformers in substations.

The voltage level of the carrier signal is sustained well by the 720-cycle capacitance of the 13.8-kv cables, and it passes over them with almost no voltage drop, even at the most distant point. However, the voltage drop in passing through the step-down transformers to the distribution busses at the stations and substations may in some cases be as high as 50 per cent. This means that the voltage delivered by the transmitter to its 13.8-kv bus (on the 115-volt base) should be about twice that required of transmitters connected directly to primary distribution busses. We found that the minimum signal level at the distribution busses should be about four volts. This agrees with Mr. Woodworth's recommendation. At the 13.8-kv busses we need about eight to ten volts.

The large voltage drop through the station and substation transformers can be caused by double transformation and by heavy 720-cycle loads, such as 60-cycle generators, at the intermediate bus. At some stations the 13.8-kv potential is first stepped down to 5.5-kv busses to which generators and heavy industrial loads are connected and then down through another set of transformers to the primary distribution bus. At 720 cycles, the leakage reactance of transformers normally would be 12 times the 60-cycle reactance, but we have found that in some cases the 720-cycle reactance between busses is from 15 to 20 times the transformer 60-cycle value. At substations where the transformation is in one step only there is much less voltage drop between the busses, and if all were of that type we could use a lower-voltage level at the transmitter. On the other hand, if the 13.8-kv interstation transmission system should be open-wire overhead lines instead of lead-covered cables, we would need a higher transmitter voltage to allow for carrier-frequency voltage drop in these lines.

Cases of 720-cycle resonance are likely to be found at locations where capacitors are used for 60-cycle power-factor correction. This causes no damage to the capacitors, but it blows capacitor fuses, trips capacitor switches, and does strange things to the 720-cycle voltage levels in the vicinity. It is not surprising that such cases of resonance exist when it is remembered that at 720 cycles this equipment is brought 144 times closer to resonance than at 60 cycles. The reactance of the capacitor is reduced to $^1/_{12}$ the 60-cycle value, while the associated inductive reactance is increased 12 times. The normal 60-cycle reactance of a capacitor used for power-factor improvement, and the normal 60-cycle inductive reactance of its associated equipment, such as the step-down transformer to which the capacitor is connected, are such that, when the frequency is increased, the capacitive and inductive reactances approach equality and consequent resonance.

A study was made of the probability of resonance of capacitor installations at various industrial plants, and it was found that in many cases the constants of the circuit are such that at 720 cycles resonance or near resonance will exist.

The capacitor is located generally on the secondary side of the transformer bank supplying the customer's load, and the 720-cycle reactance of the capacitor often will be of the same order of magnitude as that of its transformer bank. When the plant is taking load, the load equipment in parallel with the capacitor may prevent the occurrence of resonance but will not prevent it when the plant is loaded lightly. At still other locations a resonant condition will exist when the plant load is at some point between its minimum and maximum demands. Several interesting examples of these resonant conditions are as follows:

A plant that manufactures machine tools installed a capacitor to reduce its kilovolt-ampere demand. They were troubled by persistent blowing of the capacitor fuses. A curve-drawing ammeter was cut into the capacitor circuit, and its chart showed that the times of fuse operations coincided with the times when the street lights were turned on or off. A test with the fuses replaced by solid connections showed that the 720-cycle current taken by this capacitor was 305 per cent of its 60-cycle current rating. The total capacitor current was 321 per cent of its 60-cycle rating. Calculations showed a distinct resonant condition at the carrier frequency.

Another industrial plant with a demand of about 2,000 kva had a relatively small capacitor, rated 180 kva, connected to the plant bus. The load was mainly induction motors. The plant took its power from a 5.5-kv feeder. The station to which this feeder was connected is at the farthest point on the system from the 720-cycle transmitter. At this station there were two transformations to the distribution bus:

1. 13.8-kv three-phase to 5.5-kv two-phase.
2. 5.5-kv two-phase to 2.4-kv two-phase.

Because of the distance of the station from the carrier transmitter and the double transformation, it was expected that the carrier voltage on the station's distribution bus would be low, especially at times of power-system peak load when voltage drops, both 60-cycle and 720-cycle, are at their maximum. Contrary to expectations, tests showed that at such times the 720-cycle voltage at the station's distribution bus was quite high. It averaged about eight volts. When the 60-cycle power-system load is at its minimum, voltage drops are of course at their minimum, and it was, therefore, expected that during these periods the 720-cycle voltage at the distribution bus would still be high, but when measured on a Sunday afternoon it was found to be much lower than the minimum acceptable limit. It averaged 3.4 volts when first checked. Like all the low-frequency carrier voltages, it varied some from time to time, depending upon the power-system load conditions. It also is usual to find some phase unbalance of the signal voltages. On the low phase the lowest signal strength measured on a subsequent occasion was found to be 2.1 volts.

In a search for the cause of these reversals in the normal 720-cycle voltage drops, calculations were made of the 720-cycle impedances of the apparatus in the station and of the lines and customers' apparatus in the territory which the station served. It was found that the 720-cycle reactance of the 180-kva capacitor in the previously mentioned plant was equal almost exactly to the sums of the 720-cycle reactances of the customer's step-down transformer bank, and the 5.5 kv feeder to which the transformer bank was connected. Further study showed that the capacitor was also in 720-cycle resonance with the induction motor load with which it was paralleled.

These data indicated clearly what was happening. During weekdays when the customer's plant was in operation the capacitor and induction motors in parallel with it formed a 720-cycle wave trap which blocked the flow of 720-cycle current through the power station to the customer's load. This was the largest part of the 60-cycle load on the 5.5-kv station bus, and, consequently, the flow of 720-cycle current through the station transformers stepping down from the 13.8-kv bus to the 5.5-kv bus was small. The 720-cycle voltage drop through these transformers was, therefore, small, leaving a high signal voltage on the 5.5-kv bus. The 720-cycle voltage drop through the 5.5-kv to 2.4-kv transformers was small at all times. Thus, the signal voltage at the 2.4-kv distribution bus was high at times of high 60-cycle power load.

During week ends the customer's plant was shut down, but the capacitor was left in service. With the reactance of the induction motors removed the capacitor was in 720-cycle series resonance with the customer's step-down transformer bank and the 5.5-kv feeder. This combination formed a heavy 720-cycle load on the 5.5-kv station bus and drew a heavy 720-cycle current through the 13.8-kv–5.5-kv transformers. The resulting large 720-cycle voltage drop through this transformer bank left a very low signal voltage on the 5.5-kv and 2.4-kv busses, thus, the low signal voltage on the distribution bus at times of low 60-cycle power loads was accounted for.

Verification of this cause of low signal voltage was simple. On another Sunday afternoon the distribution-bus carrier voltage was measured and found to be about 3.1 volts. The customer was requested then to open his capacitor switch, and, upon doing so, the signal strength on the distribution bus immediately jumped to 6.4 volts. That is not so high as the weekday voltage previously mentioned. The difference was due mostly to the transmitter characteristic. The transmitter delivers a signal voltage about 10 to 20 per cent low on week ends.

A third and last example concerns a 720-kva capacitor on a substation bus. The capacitor switch was being tripped during the application of carrier voltages to the system. Its carrier-frequency current was about equal to its 60-cycle current, causing a 40 per cent increase in its total rms current. This trouble was corrected easily by increasing the trip-current relay setting.

When the possibility of 720-cycle resonance at capacitor installations was recognized we determined the characteristics of a small reactor which, when connected in series with a capacitor, will prevent the occurrence of the carrier-frequency resonance. We also recommended to the manufacturers that capacitors used in this territory be so equipped. It was recommended that the reactor have a 60-cycle reactance of 5.5 per cent (on the base of the capacitor 60-cycle reactance as 100 per cent) with taps at 3.5 per cent and 1.5 per cent and be designed liberally in winding and core to allow for some flow of 60-cycle harmonic currents.

The reactor taps are intended to permit adjustment, so that when the customer's installation is made nonresonant to 720-cycle frequencies it will not at the same time be made resonant to any of the normal 60-cycle harmonics. When the proper reactor tap is used, the total inductive reactance in series with the capacitor will lie between the limits of 5.5 per cent and 7.5 per cent. This is an optimum position between third and fifth harmonic resonance and well out of range of resonance at other harmonics. It is not practical to attempt to suppress completely the last vestige of these normal power-circuit harmonic currents. They will show up in varying amounts in almost any capacitor, whether or not it is equipped with a series reactor, but the reactor as recommended will limit them to harmless amounts

Smaller series inductances could be used to suppress the 720-cycle currents, but they are likely to set up resonance at other harmonics, especially over the week ends when harmonics of the power-circuit voltage are usually at their maximum. For example, a 4 per cent inductive reactance would be perfect for the fifth harmonic resonance and could cause destructive overheating of a series reactor.

The use of the series reactors, as has been recommended, has corrected all 720-cycle resonance conditions at customers' capacitor installations. The reactor is small in size, it adds only a slight amount to the cost of the capacitor installation, and this is partly offset by an increase of about five per cent in the capacitor kilovolt-amperes.

At two-station distribution busses the 720-cycle voltage was low and was raised to satisfactory operating levels by the installation of capacitors on those busses. One was the example described here, where low voltage was caused by resonance of a customer's capacitor. The capacitor installed in this station was adjusted to increase the signal level about 100 per cent on the low phase and 75 per cent on the higher phase.

When the power system to which the low-frequency carrier control is applied is interconnected with other systems, a considerable amount of the carrier current may escape by way of the interconnections. The transmitter should be large enough so that its output can include these extra losses, or wave traps should be installed at the interconnections. The decision is a matter of cost. On the other hand, it may be desirable in some cases to let the energy escape and thus increase the territory covered by carrier control, either for present or future use. In that event one should of course be sure that the signal voltages that appear in the outside territory are high enough to insure dependable operation of street-lamp controllers.

Extensive changes in the electric characteristics of the power system on either side of the interconnections, such as a large increase in the 60-cycle operating voltage or changes in transformer capacity at the interconnections, also can effect material changes in the flow of carrier current through the points of interconnection.

The power system of the Springfield division is interconnected at two points through transformers to a high-voltage transmission system. Within eight circuit-miles of the interconnections there are five stations and substations connected to the high-voltage lines. One of these serves a distribution system to which carrier-controlled street lamps are connected. Its carrier signal is supplied by the Springfield transmitter by way of the interconnections and high-voltage lines.

In 1942 the operating voltage of most of the Western Massachusetts Electric Company's transmission lines was raised from 66 kv to 115 kv, including the lines at the two points of interconnection. After this change had been made it was found that the load on the 720-cycle transmitter had increased from about 300 kva to 600 kva.

Some of our experiences with low-frequency carrier have had their humorous aspects. In 1935, when the present carrier control was installed, extensive tests were made throughout the power system to determine the signal strength at points of utilization. Many signals of about eight seconds' duration were delivered by the transmitter at intervals of about 5 to 15 minutes for several hours at a time. In substation transformers and voltage regulators the 720-cycle frequency superimposes on the 60-cycle hum, an audible high-pitched whine, not loud, but enough to attract attention. It is strongest in the feeder regulators. The signals penetrated to stations and substations connected to the high-voltage lines outside the territory of the Springfield division and produced the typical 720-cycle whine in their equipment. The operators in these stations were not aware of the carrier installation in Springfield and, hearing the strange noise, feared that some of their apparatus was developing incipient trouble and might be at the point of blowing up. Since the noise came from many directions and lasted for only a few seconds at a time, they could not tell which piece of equipment was involved, whether oil switch, transformer, or regulator. In one substation a bus capacitor also tripped off each time the signal noise was heard.

In short order the maintenance crew in the outside territory was flooded by calls for help and spent some hours in a frantic hunt to locate what was suspected of being a defective piece of equipment causing high-frequency voltage surges by intermittent internal arcing. Eventually, they checked with the Springfield division and were relieved greatly, but were equally disgusted, to learn the cause of the noise.

Some of the engineering problems of this carrier installation appeared difficult when first encountered, but this happened mainly because they were new and without precedent, and seemed elusive. They now are better known and are handled more readily. The control system has provided valuable advantages. It has released large amounts of sorely needed underground duct space in densely loaded areas by allowing us to change the street lamps from the series to the multiple type; it has reduced street-lamp outages some 80 per cent; and now that black-outs are on us, we have perhaps the most convenient of all controls for black-out switching of street illumination. If in the future we should need carrier control of other devices, such as water heaters, sign lights, midnight street lamps, and remote operation of power switches, it can be obtained from the same transmitter at the same frequency by use of coded signals.

John Bankus (Portland General Electric Company, Portland, Oreg.): Mr. Woodworth's paper is a very flaluable and timely contribution to electric distribution and should do much to promote the installation of off-peak utilization equipment.

The type of control which Mr. Woodworth has outlined is very flexible in its application. As was pointed out in the paper, the 720-cycle control voltage can be impressed on only a portion of one primary feeder, on one entire primary feeder, on all primary feeders from one substation, on a number of substations, or on an entire system. The capacity of the transmitter will depend on the extent of the system which is to be controlled. Local conditions will determine whether it is economical to install one large transmitter or several smaller units.

In this connection it may be of interest to see what transmitter capacity may be required for extending the 720-cycle control over an entire system which covers an area of 2,500 square miles and has a peak of 200,000 kw.

Mr. Woodworth made such a determination by setting up the Portland General Electric Company's system on the a-c network analyzer at Schenectady. The results of this study are summarized in the succeeding material. The Portland General Electric Company prepared a complete 720-cycle impedance diagram which covered its 57-kv and 11-kv network and primary-feeder system, its interconnections both with the Bonneville Power Administration at St. Johns and Salem, the Northwestern Electric Company at station *L*, and Knott substation. The load on the 11-kv interconnected network amounts to approximately 125,000 kw. The primary feeders in the urban areas are 2,400 volt and 4,150 volt. In rural areas many feeders are 7,200 volt. Four cases considered on the a-c network analyzer are described in the following:

Case 1. 720-Cycle Transmitter Coupled to 57 Kv at Knott Substation

The transmitter input (based on 11 kv) was 900 volts, 310 amperes, 30 per cent power factor lagging, 148 kw, 465 kilovars. The coverage was between 40 and 50 per cent of the total system load. This study showed such poor voltage on the interconnected 11-kv network that it definitely indicated the impracticability of providing control over the entire system by coupling to the 57-kv system only. It also indicated that traps would be required at Salem in order to reach locations at that end of the 57-kv system.

Case 2. 720-Cycle Transmitter Coupled to 11 Kv at Knott Substation

The transmitter input to the 11-kv network was 900 volts, 365 amperes, 30 per cent power factor lagging, 175 kw, and 550 kilovars. Sufficient voltage was obtained on the closer in four-kilovolt busses which are supplied by 11-kv network in Portland. At some points beyond these stations partial coverage was indicated. At more remote points the voltage was too low to be of any value. Indications were that the low reactance of 77,550-kva generators at station *L* were absorbing a great deal of reactive energy and thus reducing the voltage on the 11-kv network.

Case 3. 720-Cycle Transmitter Coupled to 11 Kv at Knott Substation with Station-*L* Generators Corrected by Capacitors

The transmitter input to the 11-kv system was 900 volts, 350 amperes, 36 per cent power factor lagging, 200 kw, and 500 kilovars. With this arrangement the coverage was better than for case 2 and was effective over 60 to 75 per cent of the total load.

Case 4. 720-Cycle Transmitter Coupled to 11 Kv and 57 Kv at Knott Substation with Station-*L* Generators Corrected by Capacitors and Traps in 57 115-Kv Transformer Banks at St. Johns and Salem

The total transmitter input (on the 11-kv base) into both the 11- and 57-kv systems was 800 volts, 460 amperes, 75 per cent power factor lagging, 400 kw, and 603 kilovars. Of this, approximately 250 kw was delivered to the 57-kv system and the remainder to the 11-kv. Adequate voltage for carrier control was obtained at all points on the 11-kv network. Also, good voltage was obtained at all points on the 57-kv network. This arrangement may be considered, therefore, to be a 100 per cent solution.

J. L. Woodworth: The discussion by Mr. Schonborn is particularly valuable, since it compares operating results obtained with both shunt- and series-transmitter coupling with 4,800-volt distribution substations on an interconnected system. The description of the initial interference obtained between the two transmitters using shunt coupling confirms the conclusions reached in the paper and emphasizes that 720-cycle traps are required to isolate each substation area if shunt coupling is applied at very many substations on an interconnected system.

The experience with the later series-coupled installation confirms the conclusions reached in the paper, that, by proper application, the interference voltage between adjacent feeders can be reduced to acceptable values at each substation, and, when this is done, the interference between transmitters at two or more substations is practically negligible.

The series-coupled installation had to meet two somewhat conflicting requirements. Control of water heaters was required by individual feeders to obtain maximum flexibility of off-peak load control, and high-speed operation was required for street-light control. The required flexibility was obtained when provision was made for controlling the feeders, either singly or in groups of three simultaneously.

The discussion by Mr. Bankus describes very appropriately the approach necessary when the chief value of carrier control is to permit an operator to obtain high-speed control and signalling simultaneously at many points on a large complex distribution system having extensive networks and interconnections. On applications of this type 720-cycle traps are required usually in the interconnections to other power systems to prevent potential interference with other 720-cycle control installations on the interconnected systems. This approach has been favored often when potential air raids chiefly determine the control requirements, or when the chief emphasis is placed on street-light control. For off-peak load control over-all economic studies usually favor control by individual substations or areas, even though 720-cycle traps may then be required to isolate areas if the system includes extensive primary or secondary networks.

The discussion by Mr. McKearin and Mr. Longley presents valuable data covering the operating experience with a central transmitter using shunt coupling to impress the 720-cycle voltage over an extensive network comprised chiefly of underground cable. The power system is considerably more complex than any of the systems studied in the paper, and the discussion should be extremely helpful to anyone planning the application of carrier control to systems of similar complexity.

The method described in which capacitors were installed at the substation busses to improve the 720-cycle voltage at certain low-voltage points should also prove valuable on other installations. Capacitors can be applied frequently to improve the 720-cycle voltage and thus reduce greatly the transmitter power required.

The discussion regarding the 720-cycle operating experience with power-factor-correction capacitors in Springfield appears rather formidable, until it is realized that it covers a five-year operating period and a large number of capacitor installations. The use of a series reactor proved to be a simple and effective way to eliminate trouble due to series resonance between the capacitor and the transformer reactance. The practice being followed of recommending a 5.5 per cent series reactor for every capacitor installed in this territory probably was decided on, because the capacitors are the property of industrial customers, and, therefore, the capacitor application is not under direct control of the utility. This practice has the disadvantage that the expense of the reactor is incurred not only when it is not required but also when its omission would improve the 720-cycle operation.

As was pointed out in the paper, when capacitors give harmful 720-cycle effects, the use of reactors in series with some (not all) capacitors gives over-all beneficial effects. The other users who have encountered this trouble with one or more of their capacitors have been able to follow this more economical solution.

Because of wide variations in distribution-system characteristics and in practices followed in applying capacitors, it is impossible to make universally applicable predictions as to the probable number of capacitors giving harmful effects which cannot be overcome by proper adjustment of the transmitter. Assuming 720-cycle carrier is used on all distribution circuits within its field of application, studies and operating experience indicate that, in general, reactors would be required in not more than ten per cent of the secondary capacitors and not more than 15 per cent of the primary feeder capacitors present on these circuits.

Operating experience so far indicates that the application data quite properly include conservative margins to allow for unknown factors in the applications. As these unknown factors become better understood through additional operating experiences, it is probable that these margins can be reduced to obtain economic savings.

Report of the Board of Directors

THE BOARD OF DIRECTORS of the American Institute of Electrical Engineers presents herewith to the membership its 59th annual report, for the fiscal year ending April 30, 1943. A general balance sheet showing the condition of the Institute's finances on April 30, 1943, together with other detailed financial statements, is included herein. This report contains a brief summary of the principal activities of the Institute during the year, more detailed information having been published from month to month in *Electrical Engineering*.

BOARD OF DIRECTORS' MEETINGS

The board of directors held five meetings during the year, four in New York, N. Y., and one in Chicago, Ill.

Information regarding many of the more important activities of the Institute which have been under consideration by the board of directors and the committees is published each month in the section of *Electrical Engineering* devoted to Institute activities.

WAR ACTIVITIES

The Institute has continued its general co-operation in many undertakings connected with the war efforts. In accordance with recommendations of the committee on planning and co-ordination, the board of directors approved, on August 7, 1942, a comprehensive statement of Institute policies and activities under war conditions. It provides for the holding of national technical meetings and District technical meetings, with the omission of all programmed social activities, including dinner dances, ladies program, and those trips which do not form a part of the technical program.

Technical papers, publications, and Section meetings are to be devoted to subjects closely related to the war efforts in so far as practicable. Under the sponsorship of the standards committee, the technical committees are reviewing many of the AIEE Standards in the light of wartime requirements. A considerable number of guides for the economical application and operation of electrical apparatus are in process of development by the technical committees under the sponsorship of the technical program and standards committees. These will supplement many existing standards, and assist in the conservation of critical materials. In some cases, wartime standards may be prepared. An emergency modification of AIEE Standard 45, "Electrical Installations on Shipboard," has been completed.

All committees are expected to conduct their work in accord with the spirit of these policies, and the committee on planning and co-ordination will submit new recommendations if changes become necessary.

The committee on national defense and the committee on civil protection were combined to form the committee on co-operation with war agencies, and its services were offered by President Osborne to the several government agencies most likely to need such assistance as it might be able to render.

This committee has rendered especially valuable assistance to the armed forces in their search for technical specialists qualified for commissions. More than 600 names were submitted. It appointed three subcommittees: one to collect and distribute information on conservation of critical materials, one to serve in an advisory capacity on the allocation of technical man power, and the third to assist in building up a technical library in the office of the chief signal officer in Washington.

A special committee was appointed to confer with representatives of the conservation division of the War Production Board on the substitution of silver for copper and aluminum in electrical installations. It submitted a comprehensive report.

In his visits with many Sections, President Osborne has participated in programs arranged for intensive discussions of means of conserving critical materials in co-operation with representatives of the War Production Board.

A great many Sections have held meetings connected with various phases of the war efforts, including a considerable number of production clinics intended to expedite production of war materials, held in co-operation with other engineering societies. Sections have also co-operated in local war activities.

Three members of the headquarters staff are serving in the armed forces: G. Ross Henninger, editor, as lieutenant-colonel in the Air Service Command, Patterson Field, Ohio; W. R. MacDonald, Jr., assistant editor, as major in the Signal Corps, Washington, D. C.; and John J. Anderson, Jr., of the standards department, in the U. S. Naval Reserve, receiving training on the USS *Prairie State*, New York, for a commission as ensign.

The headquarters staff has, of course, continued rendering all possible assistance to the armed forces and divisions of government in response to various types of requests.

PRESIDENT'S VISITS

President Osborne attended the summer convention, the national technical meeting in January, the North Eastern District technical meeting in Pittsfield, Mass., and the South West District technical meeting in Kansas City. In June he will attend the national technical meeting in Cleveland, Ohio.

Places Visited by President Osborne

California
Los Angeles Section
San Diego Section
San Francisco Section

Colorado
Denver Section

District of Columbia
Washington Section

Georgia
Georgia Section, Atlanta

Illinois
Chicago Section

Indiana
South Bend Section

Kansas
Wichita Section

Massachusetts
Boston Section
Worcester Section
North Eastern District technical meeting, Pittsfield

Missouri
South West District technical meeting, Kansas City

Nebraska
Nebraska Section, Omaha

New York
National technical meeting, New York
Niagara Frontier Section, Buffalo
Rochester Section
Syracuse Section

North Carolina
North Carolina Section, Raleigh

Ohio
Akron Section
Cincinnati Section
Cleveland Section

Oklahoma
Oklahoma City Section

Oregon
Portland Section

Pennsylvania
Philadelphia Section
Pittsburgh Section

Virginia
Virginia Section, Richmond

Washington
Seattle Section

Wisconsin
Milwaukee Section

Canada
Saskatchewan Section, Regina
Toronto Section
Vancouver Section

ANNUAL MEETING

The annual business meeting of the Institute was held Monday morning, June 22, 1942. The annual report of the board of directors for the fiscal year which ended April 30, 1942, was presented in abstract by the national secretary. A report on the finances of the Institute was presented by National Treasurer W. I. Slichter. The report of the committee of tellers upon the election of officers for the year beginning August 1, 1942, was presented, and President-Elect Osborne responded to his introduction with a brief address. During this session there was an address by D. D. Ewing, head, school of electrical engineering, Purdue University, Lafayette, Indiana. Also, national prizes for papers presented in 1941 were presented, and the Lamme Medal for 1941 was presented to Forrest E. Ricketts, vice-president, Consolidated Gas Electric Light and Power Company of Baltimore.

NATIONAL CONVENTIONS

Three national conventions were held during the year, and a brief report on each follows:

Summer Convention. The 58th summer convention was held in Chicago, Ill., June 22–26, 1942. In addition to the annual business meeting, and conference of officers, delegates, and members, there were 20 sessions and conferences with approximately 60 technical papers and informal conference presentations. A general session on the subject of "The Organization and Management of Large-Scale Engineering Works" was addressed by M. R. Sullivan, vice-president, American Telephone and Telegraph Company, New York, N. Y.; R. C. Muir, vice-president, General Electric Company, Schenectady, N. Y.; and H. B. Gear, vice-president, Commonwealth Edison Company, Chicago, Ill. Entertainment features consisted of a preconvention reception for early arrivals, president's reception and dancing, buffet dinner at the Lake Shore Club, banquet, and golf tournaments. The registration was 1,179.

Pacific Coast Convention. The 30th Pacific Coast convention was held in Vancouver, B. C., Canada, September 9–11, 1942, with a registration of 245. "Wartime National Efficiency" was the topic of an address given by G. A. Gaherty, president, Montreal Engineering Company, Ltd., and Walter J. Gilson, general manager, Eastern Power Devices, Ltd., Toronto, gave an address entitled "International Prejudices" before a joint luncheon meeting sponsored by the Vancouver Electric Club. There were five technical sessions and one student session. Social features included a luncheon party, a musical program, a bridge tea, and a formal dance. The 22nd annual competition for the John B. Fisken Golf Trophy was won by Bernard DeM. Mertens, British Columbia Electric Railway Company, Ltd.

National Technical Meeting. The 31st national technical meeting (formerly called winter convention) was held in New York, January 25–29, 1943, with a program including 13 technical sessions and 12 conferences, at which 62 papers were presented. At a general session, the two feature addresses concerned the specialized technical training programs established by the Army and Navy in collaboration with leading educational authorities. Colonel Herman Beukema, director of specialized training division, United States Army, and J. W. Barker, special assistant to the secretary of the Navy, were the speakers. Discussions were by R. E. Doherty, president, Carnegie Institute of Technology, Pittsburgh, Pa.; R. C. Muir, vice-president, General Electric Company, Schenectady, N. Y.; and George T. Seabury, secretary, American Society of Civil Engineers.

At a dinner meeting, Vannevar Bush, president, Carnegie Institution of Washington, and director of the Office of Scientific Research and Development, spoke on "Research in the War Effort." At a session on Wednesday evening three medals were presented: the Edison Medal to Edwin H. Armstrong, the John Fritz Medal to Willis R. Whitney, and the Hoover Medal to Gerard Swope. Social events and inspection trips were omitted in accordance with the wartime policies adopted by the board of directors. The registration was 1,419.

Concurrently with the AIEE national technical meeting, the Institute of Radio Engineers held its winter conference, which culminated in a joint meeting at which George C. Southworth, Bell Telephone Laboratories, Inc., spoke on "Beyond the Ultra Shorts." Following this, there were talks by Sir Noel Ashbridge, chief engineer, British Broadcasting Company, by shortwave radio from London; L. P. Wheeler, president IRE, Arthur F. Van Dyck, IRE past president, and James Lawrence Fly, chairman of the Board of War Communications, who spoke from Washington, D. C.

DISTRICT MEETINGS

North Eastern District Meeting. This meeting was held in Pittsfield, Mass., April 8–9, 1943, and included five conference sessions, a general session, and three technical sessions. At a general session there were addresses by H. S. Osborne, president AIEE, and J. F. Burt of the Western Massachusetts Companies. The get-together dinner was followed by an address by C. E. Smith, vice-president, New York, New Haven, and Hartford Railroad, on "Railroad Transportation in the Present Emergency." The registration was 319.

South West District Meeting. The ninth meeting of the South West District was held in Kansas City, Mo., April 28–30, 1943. It was the third held in that city. The principal features were a general session and a symposium on critical materials and equipment, both held jointly with the Missouri Valley Electric Association, four technical sessions, one student session, a luncheon with three talks, a district executive committee meeting, a clinic on Section management and operation, a dinner for men, and arrangements for student consultations with industry representatives. The attendance was 570, including 338 AIEE members, students, and guests.

Table I. Section and Branch Statistics

	For Fiscal Year Ending April 30					
	1938	1939	1940	1941	1942	1943
Sections						
Number of Sections	65	67	70	72	72	73
Number of meetings held	624	635	701	703	647	598
Total attendance	110,148	85,692	91,949	92,554	78,254	66,111
Branches						
Number of Branches	120	120	121	123	124	125
Number of meetings held	1,334	1,190	1,346	1,163	946	942
Total attendance	60,446	53,380	64,972	52,285	37,785	38,227

SECTIONS

1. Programs Connected With War Efforts. The Sections committee sent three letters, August 28, September 29, and January 15, to each Section to encourage participation in war efforts. All Sections have a keen appreciation of the importance of this contribution to the war effort. A great many programs have been devoted to conservation of critical materials, production clinics, and other subjects connected with the war.

2. Development of Central and South American Sections. In response to the committee's inquiries among members in Central and South America regarding the desirability of forming Sections in their countries, many favorable letters have been received. Certain difficulties appear to make the formation of regular Sections impracticable at present, but the committee is planning to recommend to the board of directors a plan for a special type of organization for those countries.

3. Section Territory in the United States. The only remaining areas in the United States not assigned to Sections are in Arkansas and Nevada. The committee and the vice-presidents of Districts 8 and 9 are recommending the assignment of the remaining counties in Nevada to three Sections.

4. Subsections. The committee wrote each Section on December 10 regarding the desirability of forming geographical Subsections and on February 26 regarding the formation of technical groups. A new folder on Subsections was prepared, and copies were sent to the Sections in March. Some Subsections have been formed recently, and others are under consideration.

5. Contact With Members in Armed Forces. The Sections committee was actively interested in the development of recommendations upon which the board of directors acted in January, providing an inactive membership status for members and enrolled students in the armed forces and the merchant marine, and authorizing the publication committee to prepare for mailing to them a quarterly news letter.

6. Section Operation and Management. Special plans have been developed for discussions on the management of Section activities and the types of information that should be supplied to the Sections.

STUDENT BRANCHES

One new Student Branch was established during the year, at Vanderbilt University, bringing the total number to 125.

The interest of the students in Institute activities has continued very satisfactorily. There is apparently some increase in the use of outside speakers and in the use of industrial moving pictures for meetings. It seems that industrial moving pictures may assist in replacing inspection trips. On the whole, it seems that the Canadian Branches have maintained a greater degree of normalcy than the branches in this country in regard to meetings and inspection trips.

Some of the District group meetings of the Branches have been given up because of the accelerated programs in colleges and transportation difficulties.

With the increasing use of staff and facilities by the Army and Navy, together with the strict scheduling of the student time in the Army and Navy, the work of the Student Branches may be severely handicapped. It is hoped that the enthusiasm and diligence of the counselors may inspire the students whether in civilian, Army, or Navy life, so that the Branch activities may be continued. There exists a real challenge to bring to the future engineers a definite view of peacetime engineering and of the ideals of the profession which will stand them in good stead as the havoc of war decreases.

General Committees

FINANCE COMMITTEE

A continuance in the growth of Institute membership, together with a material increase in revenue from advertising in *Electrical Engineering*, permits the finance committee to report for the fiscal year an excess of receipts over expenditures of approximately $38,000, in addition to transfers made during the year of $10,000 to the pension fund, and $20,000 to the reserve capital fund.

In its preparation of the annual budget, the finance committee has endeavored to give full consideration to probable losses in dues revenue as a result of concessions now being made to members serving in the armed forces and merchant marine.

Expenditures have been in accordance with a program of Institute activities in effect during the past few years except as modified by any special conditions adopted to conform with the war effort. Particulars regarding the provisions of the current budget appear in the December 1942 issue of *Electrical Engineering* news section, pages 617–18.

Institute investments now reflect the current market trend. Upon referring to the financial statement accompanying this report, it will be noted that losses on securities which developed in less favorable years have been almost entirely overcome by the appreciation in other security values. Approximately 55 per cent of the Institute's investments is now in United States war and other Government bonds.

It is expected that losses in dues revenue resulting from concessions to members in service will become more evident during the fiscal year which began May 1, 1943, and that the cost of Institute activities will increase by reason of war conditions; for example, the executive committee of the Institute recently voted to increase the financial support of United Engineering Trustees, Inc., because of increased operating expenses and a decrease in normal building-operating revenue. For this reason, the committee expects that some of the recent excess of receipts over expenditures will be needed to meet deficiencies and increased costs.

Haskins & Sells, certified public accountants, have audited the Institute books and their report appears on pages 13 to 16.

TECHNICAL PROGRAM COMMITTEE

Technical Meetings. Three national and two District technical meetings, comprising 44 technical sessions and 24 conferences, were held during the year. The total registration at the five meetings was 3,497, as compared with 4,274 at six meetings held in 1941–42. The registration at the three national technical meetings was 2,843, as compared with 2,856 the year before, despite a large decrease in guest registration. At the national technical meeting in January, the average attendance at each of the 29 sessions and conferences was 138, compared with an average attendance of 109 at each of the 27 sessions held last year, and 121 at each of the 27 sessions in 1941.

Programs. In line with the AIEE wartime policy (*EE, Sept. 1942, p. 477*), the committee gave preference to subjects of immediate importance in the war effort, and fully one third of the sessions were devoted wholly to wartime problems. A principal theme of the year's work was the promotion of "Conservation of Critical Materials Through Good Engineering" through the presentation and discussion of a series of wartime guides or reports and papers on the selection and operation of electrical equipment (*EE, Oct. 1942, p. 527*). Twelve such guides or reports were prepared or proposed (*EE, Mar. 1943, p. 114*). It was agreed that these guides should be entirely educational in character, and that they should all be reconsidered, and approved, revised for normal use, or rescinded, after the war emergency. A foreword to this effect, prepared by the standards committee, was included in all such reports prepared subsequent to April 1943.

Civic Affairs Conferences. With a view to furthering the participation of engineers in civic affairs, especially city and regional planning, conferences were held at the Chicago and Pittsfield meetings. The Pittsfield conference, held during the North Eastern District technical meeting, on April 8, brought together six civic authorities and experts who are actively engaged in planning work. Their addresses followed an address by President Osborne on "The Engineer's Interest in City Planning." A similar conference has been proposed for the national technical meeting to be held in Cleveland.

Committee Policies. To minimize time and travel, the committee held only three meetings during the year. These three meetings were attended by 56, 50, and 66 per cent of the committee members or duly appointed representatives.

In view of the increasing importance of electronics, the committee recommended and the board of directors approved giving the joint subcommittee on electronics full status as a technical committee. Because of the expanding use of carrier current for multiple purposes, which are in the fields of several technical committees, a joint subcommittee on power system applications of carrier current has been appointed consisting of representatives of the committees on protective devices, automatic stations, power transmission and distribution, and communication.

To aid the war effort and to encourage reports on the latest developments, restrictions on the closing dates for the receipt of papers have been relaxed. This, together with the preoccupation of most authors and reviewing committee members with war work, has led to an increase in the length of the average paper to a figure of 6.28 pages, the highest figure in the past five years.

General Sessions. General sessions have dealt with the broader aspects of the profession as related to the war effort and postwar problems. The general session for the meeting in Chicago was on the subject of "The Organization and Management of Large-Scale Engineering Work." The general session of the January meeting was devoted to "Technical Man Power in the War Effort." Plans are being made to devote the general session of the meeting in Cleveland to the engineering aspects of Latin-American relationships.

PUBLICATION COMMITTEE

The Institute's principal technical publications were produced during the year on the same general basis as during the preceding year, no change in publication

Table II. Statistics of Technical Programs for Last Five Years

	Year Ending April 30, 1943	Year Ending April 30, 1942	Year Ending April 30, 1941	Year Ending April 30, 1940	Year Ending April 30, 1939
Number of national technical meetings	3	3	3	2	3
Number of District technical meetings	2	3	2	3	3
Registration at national meetings and District meetings	3,497	4,274	4,339	3,548	4,100
Number of papers presented	168	182	195	177	184
Number of papers recommended for *Transactions*	141	152	167	151	169
Estimated number of pages required for printing papers in *Transactions*	886*	807*	938*	886*	986*
Average length of papers recommended for *Transactions*	6.28	5.3	5.72	5.86	5.83
Number of technical sessions	44	50	46	43	50
Number of technical conferences	24	23	18	10	12

* Partly estimated.

Table III. Membership Statistics for Fiscal Year Ending April 30, 1943

	Honorary Members	Fellows	Members	Six-Year Associates	Associates	Subtotals	Totals
Membership April 30, 1942	5	792	4,903	6,145	7,099		18,944
Additions							
New members qualified		1	116		1,628	1,745	
Fomer members re-instated or re-elected		2	79	51	105	237	
Subtotals		3	195	51	1,733	1,982	
Transfers	1	48	250	771		1,070	
Totals	1	51	445	822	1,733	3,052	
Deductions							
Died		15	51	50	13	129	
Resigned		3	27	112	80	222	
Dropped		2	34	152	226	414	
Subtotals		20	112	314	319	765	
Transfers			48	216	806	1,070	
Totals		20	160	530	1,125	1,835	
Net Changes	1	31	285	292	608	1,217	
Membership April 30, 1943	6	823	5,188	6,437	7,707		20,161

policy having been enacted in the interim.

The 1942 *Transactions* volume marked the completion of the transition to the improved publication procedure adopted in August 1940, and included only 1942 technical papers and related discussions. Publication of technical papers continues on a month-by-month basis, in a segregated *Transactions* section of *Electrical Engineering*, with discussions and a few long technical papers published in the two semiannual "Supplements to *Electrical Engineering—Transactions* Section."

Subject matter of both technical papers and general-interest articles published in *Electrical Engineering* is now closely related to the war effort, in accordance with the wartime policies adopted by the Institute's board of directors in August 1942. First preference is given those papers and articles directly aiding the war effort, and those not meeting this requirement constitute a minor part of the total program.

Two factors are now limiting the amount of technical material published:

1. A reduced amount of available material, probably a result of the employment of the majority of electrical engineers in war work that cannot be publicized.
2. Governmental restrictions on the use of paper.

However, efforts are being made to maintain the publication of technical material at the highest possible level.

In addition to the usual technical-paper pamphlets produced for national and District meeting programs, special pamphlets which were published in the year included:

1. "Bibliography on Circuit Interrupting Devices, 1928–40," prepared by the AIEE committee on protective devices, May 1942.
2. "Bibliography on Electrical Safety, 1930–41," prepared by the AIEE committee on safety, July 1942.
3. "Bibliography on Automatic Stations, 1930–41," prepared by the AIEE committee on automatic stations, December 1942.
4. "Advanced Methods of Mathematical Analysis as Applied to Electrical Engineering." This is a consolidated reprint of five lectures published in *Electrical Engineering*, originally presented as a symposium under auspices of the basic science group of the New York Section.

In addition to the special publications already mentioned, the board of directors at its January 1943 meeting authorized the publication of a news bulletin for AIEE members in the armed services and the merchant marine. The first issue is now in preparation.

Governmental censorship regulations have continued in effect as reported last year. Measures have been taken to avoid publication of censorable material, and manuscripts dealing with restricted subject matter have been cleared with designated authorities before publication. Shipments of publications to members and subscribers in foreign countries have continued on the same basis as reported last year. No shipments are made to enemy territory or to enemy-occupied or controlled territory. Distribution to other foreign destinations is being made under export-license control and rigorous limitations imposed by the United States Office of Censorship and the Office of Export Control of the Board of Economic Warfare.

MEMBERSHIP COMMITTEE

The Institute membership stood at 20,161, April 30, 1943. This compares with a membership of 18,944 on April 30 last year. The goal set by the membership committee was 20,000. It was never quite clear, because of the abnormal conditions which prevailed, whether this objective would be attained. There have been many mixed influences throughout the past year resulting from the readjustments of all engineers to war conditions. It is a tribute to the loyalty and determination of every Section membership committeeman that we have been able to forge ahead at all when almost every reasonable sign pointed in the reverse direction. As in past years our best source of leads for new members was the very fine response to the chairman's letter of September 16, 1942, to the entire membership. Some 896 names of candidates were submitted. The membership committee herewith expresses its gratitude to those members who sent in suggestions for new members.

As usual several tables have been included to show what has been happening to the Institute membership, comparing this year's results with those of previous years.

In Table III it is seen that the gross additions to membership were 1,982 while the deductions were 765. The net increase in membership was 1,217. Last year the net increase amounted to 1,058 members. This year's net increase of 1,217 members

Table IV. Number of Applications Received From Enrolled Students and From All Others

Year Ending April 30	Students	All Others	Total
1943	783	1,431	2,214
1942	971	1,031	2,002
1941	887	1,011	1,898
1940	911	918	1,829
1939	849	872	1,721

Table V. Number of Enrolled Students as of April 30

Year	New Applications	Renewals	Total
1943	2,512	3,200	5,712
1942	2,585	3,377	5,962
1941	2,351	3,188	5,539
1940	2,525	2,992	5,517
1939	2,271	2,971	5,242

Table VI. Number of Delinquent Members in Section Territory Reinstated

August 1, 1942 to April 30, 1943	323
Year beginning August 1, 1941	318
Year beginning August 1, 1940	383
Year beginning August 1, 1939	302
Year beginning August 1, 1938	354

Table VII. Status of Membership Dues as of April 30

Year	Total Membership	Members Fully Paid	
		Number	Per Cent
1943	20,161*	18,247**	90.5
1942	18,944	16,922	89.3
1941	17,886	15,777	88.2
1940	17,213	14,997	87.1
1939	16,605	14,371	86.5

* Of the 20,161 members reported for April 30, 1943, 771 names are being carried upon the membership rolls in an inactive status, in accordance with the resolutions adopted by the board of directors in the interest of members in the military service of the United States or allied countries, or members located in such countries unable to remit membership dues at this time:

1. Members owing dues to April 30, 1942........496
2. Members owing dues to April 30, 1943......1,418

(During the period May 1 to May 17, 1943, 297 members have paid dues to April 30, 1943, reducing the number not fully paid to 1,121.)

** Including 936 Members for Life; and the 771 mentioned above who are not currently paid up, but from whom no payments are expected during the war.

is the largest ever recorded except for the year ending April 30, 1921.

Table IV indicates the number of applications received from enrolled students and others for the past five years. The number of applications from enrolled students is lower than that reported last year, principally because those in the armed forces and merchant marine were given the opportunity to continue their student affiliation while in service. The increase of 400 applications this year as compared with last year from other than students reflects wider employment of engineers and a general increased need on the part of engineers to keep abreast of latest developments in the field of electrical engineering.

It should be pointed out that no direct check can be made between Tables III and IV, because applications received are not all acted on before the close of the fiscal year.

Table V shows data on the number of enrolled students and new applications for student membership from students for five years ending 1943. The effect of the war is very clearly indicated in the figure for 1943.

The results portrayed in Table VI show in no small way the success achieved by Section membership committeemen in their contacts with wavering members.

Table VII contains the "vital" statistics of the Institute for this year as compared with the past four years. Since it is the "fully paid members" who make the AIEE the successful organization that it is, it is always of interest to have a look at their statistical behavior. The total membership of 20,161 on April 30, 1943, includes 771 names being carried in an inactive status because of military service as compared with 327 names on the same date last year.

Table VIII is included to keep the record straight and is self-explanatory.

It would be easier to prepare a report of the year's membership activities if Table IX could be left out. Every year we note

Table VIII. Record of AIEE Membership

Year	Total May 1	Year	Total May 1	Year	Total May 1
1884	71	1905	3,460	1925	17,319
1885	209	1906	3,870	1926	18,158
1886	250	1907	4,521	1927	18,344
1887	314	1908	5,674	1928	18,265
1889	333	1909	6,400	1929	18,133
1890	427	1910	6,681	1930	18,003
1891	541	1911	7,117	1931	18,334
1892	615	1912	7,459	1932	17,550
1893	673	1913	7,654	1933	17,019
1894	800	1914	7,876	1934	15,230
1895	944	1915	8,054	1935	14,269
1896	1,035	1916	8,202	1936	14,600
1897	1,073	1917	8,710	1937	15,308
1898	1,098	1918	9,282	1938	16,078
1899	1,133	1919	10,352	1939	16,605
1900	1,183	1920	11,345	1940	17,213
1901	1,260	1921	13,215	1941	17,886
1902	1,549	1922	14,263	1942	18,944
1903	2,229	1923	15,298	1943	20,161
1904	3,027	1924	16,455		

Table IX. Deaths of AIEE Members Reported in "Electrical Engineering"

Name	Date of Election	Date of Death	Grade at Death	Obituary Notice in Electrical Engineering
Abeel, L. B.	Associate '31	Oct. 20, 1942	Associate	Feb. 1943, p. 77
Ackland, E. W.	Associate '05	Mar. 28, 1942	Member	July 1942, p. 366
Allner, F. A.	Associate '12	July 18, 1942	Member	Sept. 1942, p. 481
Ames, Azel	Associate '09	Nov. 23, 1942	Associate	Jan. 1943, p. 35
Andree, J. W.	Member '26	Apr. 7, 1942	Member	July 1942, p. 366
Atwood, J. A.	Associate '16	Jan. 7, 1942	Associate	July 1942, p. 366
Barry, J. G.	Associate '03	Mar. 4, 1943	Associate	Apr. 1943, p. 177
Barstow, W. S.	Associate '94	Dec. 26, 1942	Fellow	Feb. 1943, p. 76
Baylis, R. N.	Associate '89	Sept. 5, 1942	Member	Mar. 1943, p. 132
Booth, G. A.	Associate '13	Feb. 18, 1942	Associate	May 1942, p. 264
Brenner, W. H.	Associate '93	June 20, 1942	Member	Sept. 1942, p. 481
Brown, J. E.	Associate '03	Aug. 23, 1942	Associate	Nov. 1942, p. 578
Calmus, F. A.	Associate '29	Aug. 2, 1942	Associate	Oct. 1942, p. 533
Carpenter, C. E.	Associate '96	Nov. 9, 1942	Associate	Jan. 1943, p. 35
Christiansen, K. C.	Associate '17	Dec. 5, 1942	Member	Mar. 1943, p. 131
Clark, H. D.	Associate '32	Jan. 21, 1942	Associate	June 1942, p. 331
Coho, H. B.	Associate '94	May 19, 1942	Associate	Dec. 1942, p. 624
Cole, G. M.	Associate '02	Jan. 6, 1942	Associate	Aug. 1942, p. 438
Constad, H.	Associate '35	Sept. 1940	Associate	Oct. 1942, p. 533
Cornell, C. L.	Associate '90	July 18, 1942	Member	Sept. 1942, p. 481
Cranston, J. A.	Associate '08	Apr. 15, 1942	Associate	June 1942, p. 330
Danford, F. E.	Associate '37	Oct. 11, 1942	Associate	Dec. 1942, p. 624
Darling, N. J.	Member '34	Oct. 26, 1942	Member	Dec. 1942, p. 624
Davenport, J. C.	Associate '07	Dec. 19, 1942	Member	Mar. 1943, p. 132
Dodson, H. A.	Associate '37	Jan. 23, 1942	Associate	June 1942, p. 330
Dow, Alex	Associate '93	Mar. 22, 1942	Honorary Member	May 1942, p. 263
Durran, R. T.	Associate '05	June 5, 1942	Associate	Sept. 1942, p. 482
Eaton, R. W.	Associate '07	June 1, 1942	Member	Aug. 1942, p. 438
Edwards, J. P.	Associate '92	July 10, 1942	Member	Nov. 1942, p. 578
Egli, J.	Associate '26	May 10, 1942	Member	Aug. 1942, p. 438
Fenley, W. H.	Associate '14	Nov. 22, 1942	Associate	Mar. 1943, p. 132
Flory, A. C.	Member '26	Mar. 1, 1943	Member	Apr. 1943, p. 178
Fortenbaugh, S. B.	Associate '95	Feb. 6, 1943	Fellow	Mar. 1943, p. 131
Freeman, W. E.	Associate '11	May 21, 1942	Member	July 1942, p. 365
Frost, F. G.	Member '22	July 1, 1942	Member	Oct. 1942, p. 533
Gallagher, J. A.	Associate '42	Nov. 11, 1942	Associate	Apr. 1943, p. 178
Glander, E. G.	Associate '11	Sept. 10, 1942	Associate	Nov. 1942, p. 578
Goldenstein, M. M.	Associate '07	Oct. 1942	Member	Mar. 1943, p. 32
Goldmark, C. J.	Associate '88	Nov. 3, 1942	Associate	Feb. 1943, p. 77
Hadley, F. W.	Associate '96	June 7, 1942	Member	Dec. 1942, p. 624
Harding, C. F.	Associate '06	Apr. 13, 1942	Fellow	May 1942, p. 264
Harisberger, John	Associate '02	Sept. 9, 1942	Member	Dec. 1942, p. 623
Hays, J. C.	Associate '04	July 21, 1942	Member	Sept. 1942, p. 481
Hayward, H. O. F.	Associate '39	Jan. 15, 1943	Associate	Apr. 1943, p. 178
Hellmund, R. E.	Associate '05	May 16, 1942	Fellow	June 1942, p. 329
Herpich, R. L.	Associate '42	July 30, 1942	Associate	Nov. 1942, p. 578
Hertz, S. S.	Associate '17	Feb. 20, 1943	Fellow	Apr. 1943, p. 176
Hewitt, W. R.	Associate '94	Feb. 21, 1940	Associate	Aug. 1942, p. 439
Hilbert, E. E.	Associate '42	Feb. 26, 1943	Associate	Apr. 1943, p. 178
Holmes, J. T.	Member '24	Feb. 12, 1943	Member	Apr. 1943, p. 178
Hosford, H. B.	Associate '30	Dec. 1942	Associate	Mar. 1943, p. 133
Jarvis, C. D.	Associate '11	Mar. 6, 1942	Associate	May 1942, p. 264
Kallir, Ludwig	Member '29	Jan. 7, 1943	Member	Apr. 1943, p. 178
Kearns, E. W.	Associate '24	Feb. 18, 1942	Associate	July 1942, p. 366
Kelly, P.	Associate '19	Apr. 5, 1942	Associate	July 1942, p. 366
Koppitz, C. G.	Member '21	Jan. 6, 1943	Member	Feb. 1943, p. 76
Lang, R. H.	Associate '21	Apr. 4, 1942	Member	June 1942, p. 330
Larner, C. W.	Associate '12	June 11, 1942	Associate	Aug. 1942, p. 438
Manson, D. E.	Associate '07	Apr. 30, 1942	Member	July 1942, p. 365
McBerty, F. P.	Associate '12	Oct. 19, 1942	Associate	Mar. 1943, p. 132
Meyer, D. C.	Member '29	Feb. 2, 1943	Member	Apr. 1943, p. 177
Morgan, M. I.	Associate '39	Jan. 6, 1943	Associate	Mar. 1943, p. 132
Morrow, L. W. W.	Associate '13	Nov. 15, 1942	Fellow	Dec. 1942, p. 623
Noble, C. S.	Associate '18	Sept. 17, 1942	Member	Feb. 1943, p. 77
Nordstrum, L. D.	Associate '04	Feb. 1, 1943	Member	Apr. 1943, p. 178
O'Reilly, A. J.	Associate '03	Jan. 27, 1943	Associate	Apr. 1943, p. 176
O'Toole, J. R.	Associate '39	Apr. 6, 1942	Associate	Mar. 1943, p. 133
Outzen, A. N.	Associate '14	Aug. 26, 1941	Associate	Apr. 1943, p. 176
Palmer, J.	Associate '13	Apr. 20, 1942	Associate	July 1942, p. 366
Penn, Marion	Member '21	Mar. 29, 1942	Fellow	May 1942, p. 264
Phillips, F. R.	Member '27	Oct. 23, 1942	Member	Dec. 1942, p. 623
Porter, J. F.	Associate '87	Nov. 7, 1942	Fellow	Dec. 1942, p. 623
Porter, J. S.	Associate '35	May 2, 1942	Associate	Oct. 1942, p. 533
Prindle, E. J.	Associate '06	Dec. 17, 1942	Member	Feb. 1943, p. 77
Pyper, D.	Associate '29	Mar. 6, 1942	Associate	Sept. 1942, p. 482
Quigley, F. P.	Member '33	Mar. 22, 1942	Member	May 1942, p. 264
Reiber, A. H.	Associate '21	Feb. 1, 1943	Fellow	Apr. 1943, p. 177
Reist, H. G.	Associate '90	July 5, 1942	Fellow	Sept. 1942, p. 481
Rice, H. F.	Associate '21	Mar. 24, 1942	Member	June 1942, p. 330
Roff, O. C.	Associate '07	Mar. 25, 1942	Associate	June 1942, p. 330
Rosebrugh, T. R.	Associate '91	Jan. 24, 1943	Associate	Apr. 1943, p. 177
Ruess, C. N.	Associate '25	Sept. 7, 1942	Associtae	Jan. 1943, p. 35
Runyon, W. M.	Associate '41	June 1942	Associate	Mar. 1943, p. 132
Rutherford, E. J.	Member '35	Dec. 10, 1942	Member	Mar. 1943, p. 132
Sanderson, E. W.	Associate '10	Mar. 1942	Associate	July 1942, p. 366
Sandstrom, P. N.	Associate '25	Jan. 14, 1943	Associate	Mar. 1943, p. 132
Sasscer, W. H.	Associate '28	Apr. 28, 1942	Member	July 1942, p. 366
Schift, Martin	Member '20	Feb. 15, 1943	Fellow	Apr. 1943, p. 177

(Continued on following page)

Table IX (continued). Deaths of AIEE Members Reported in "Electrical Engineering"

Name	Date of Election	Date of Death	Grade at Death	Obituary Notice in Electrical Engineering
Schwarz, E. H.	Associate '03	Feb. 21, 1943	Member	Apr. 1943, p. 177
Scudder, Hewlitt	Associate '99	July 31, 1942	Associate	Sept. 1942, p. 481
Seelig, A. E.	Associate '07	Sept. 13, 1942	Associate	Feb. 1943, p. 77
Sharp, C. H.	Associate '02	May 14, 1942	Fellow	June 1942, p. 329
Shreeve, H. E.	Associate '06	Apr. 24, 1942	Fellow	June 1942, p. 330
Siegel, E. M.	Member '42	Apr. 10, 1942	Member	June 1942, p. 330
Smith, A. C.	Member '15	Oct. 25, 1942	Member	Mar. 1943, p. 131
Snook, H. C.	Associate '05	Sept. 23, 1942	Fellow	Nov. 1942, p. 578
Spurck, R. M.	Associate '18	Dec. 12, 1942	Fellow	Feb. 1943, p. 77
Stahl, N.	Associate '08	Jan. 1, 1943	Fellow	Mar. 1943, p. 131
Stewart, R. S.	Associate '96	Nov. 11, 1942	Member	Apr. 1943, p. 177
Stockwell, J. F.	Associate '03	Jan. 28, 1943	Associate	Apr. 1943, p. 177
Summers, C. L.	Associate '41	1942	Associate	Mar. 1943, p. 133
Tanzer, E. D.	Associate '15	June 11, 1942	Associate	Oct. 1942, p. 533
Tesla, Nikola	Associate '88	Jan. 7, 1943	Fellow	Feb. 1943, p. 76
Townsend, C. W.	Associate '41	May 28, 1942	Associate	July 1942, p. 366
Towson, M. S.	Associate '08	Mar. 17, 1942	Associate	June 1942, p. 330
VanDyke, C. S.	Associate '11	May 8, 1942	Associate	Aug. 1942, p. 439
Ward, Ralph B.	Associate '24	July 26, 1942	Member	Jan. 1943, p. 36
Weber, R. L.	Associate '16	Apr. 27, 1942	Member	Aug. 1942, p. 438
White, J. G.	Associate '89	June 2, 1942	Member	July 1942, p. 365
Whittle, Horace	Associate '20	Feb. 18, 1942	Associate	May 1942, p. 264
Williams, J. H.	Associate '13	July 1942	Member	Dec. 1942, p. 624
Woltz, F. I.	Associate '10	Nov. 27, 1942	Member	Feb. 1943, p. 77
Wood, L. A. S.	Member '24	May 5, 1942	Member	July 1942, p. 365
Wood, W. D.	Associate '31	Mar. 22, 1942	Associate	June 1942, p 330
Woodworth, L. B.	Associate '00	May 29, 1942	Member	Oct. 1942, p. 533
Zimmer, C. S.	Associate '37	July 21, 1942	Associate	Nov. 1942, p. 578

with sadness the name of a friend, associate, or acquaintance who has left our ranks forever. Again this year we pay our last humble respects to those of our membership who have taken their departure from this life.

The general organizational form of the national membership committee remained the same as in the previous year. It has been found that an advisory subcommittee which assists the chairman in making quick decisions and the research subcommittee for studying perplexing problems in a detached and deliberate manner have greatly assisted in keeping the work of the committee moving in a well-balanced manner.

Table X. Applications for Admission and Transfer

Applications for Admission		
Recommended for grade of Associate	1,844	
Re-elected to the grade of Associate	136	
Not recommended	2	
		1,982
Recommended for grade of Member	197	
Re-elected to the grade of Member	26	
Not recommended	39	
		262
Recommended for grade of Fellow	1	
Re-elected to the grade of Fellow	1	
		2
Applications for Transfer		
Recommended for grade of Member	272	
Not recommended for grade of Member	13	
		285
Recommended for grade of Fellow	47	
Not recommended for grade of Fellow	—	
		47
Students		
Recommended for enrollment as Students		2,317
Total		4,895

The research subcommittee is putting together and boiling down a considerable amount of material which will ultimately form the basis for a new AIEE booklet which can be used to good advantage in explaining the Institute to potential new members. It will also be a very valuable reference booklet for each member. It is believed that a new and different way has been found to show what the Institute is and what it has done and can do for electrical engineers.

The progress on this booklet has not been so fast as we had hoped it would be because of the pressure of war work on the members of the research subcommittee. However, when the booklet becomes available, it will be well worth the wait.

It has been the consistent policy of the chairman this year to assist the Section membership committee chairmen in the principal task of getting new members and keeping present members. Relatively few communications have been sent out and those that did go out were brief and in line with the main objectives of the Committee. The Section membership committees have functioned splendidly this year and deserve credit for doing a good job in the face of many odds.

BOARD OF EXAMINERS

The board of examiners held 11 meetings during the past year, averaging about $2^1/_2$ hours each, and acted upon 4,895 cases, divided as shown in Table X. This represents an increase of 33 per cent over the total handled by the examiners during the previous year.

While there were no changes during the year in the basic procedure followed in the consideration of cases for either admission or transfer, a question was raised on the interpretation of the constitutional requirements for Fellow grade. The board of examiners has appointed a subcommittee to study this matter with a view to recommending constitutional revisions which will clarify particularly the wording of Article II, Sections *4c*, *d*, and *e*. A second subcommittee is making a study of the admission form to determine whether a more effective form may be developed.

COMMITTEE ON PLANNING AND CO-ORDINATION

The committee considered thoroughly, in the summer of 1942, the desirability of modifying Institute activities to provide for greater assistance in the war efforts. At the meeting of the board of directors on August 7, a comprehensive statement of policies and activities of the Institute under war conditions was adopted. Some of the principal parts have been mentioned already under "War Activities." The statement was published in full (*EE, Sept. 1942, p. 477*).

The committee submitted a comprehensive report at the meeting of the board of directors January 28, 1943, which resulted in the actions outlined below:

> The Sections and Districts were encouraged by the board to schedule the usual number of meetings, to be devoted to the war effort, or, in the event of a change in the international situation, to peace aims.
>
> Paragraphs *4c*, *4d*, and *4e* of article II of the constitution were interpreted as not requiring five years in Member grade.
>
> Members and enrolled students in the armed services and the merchant marine were given the option of continuing to pay regular dues or student fees with the usual privileges, or of being inactive members or students for the duration of the war without payments.
>
> The publication committee was authorized to prepare a quarterly news letter to be sent to members in war service.

COMMITTEE ON CODE OF PRINCIPLES OF PROFESSIONAL CONDUCT

"Canons of Ethics" were submitted by the engineers' joint committee on ethics to the Engineers Council for Professional Development board at its annual meeting in October 1942, and, after consideration by that board, were forwarded to the boards of the several constituent members of ECPD, of which AIEE is one. The submission was made with the request for

1. General comments.
2. A statement of what, if any, items of the submitted draft could be omitted appropriately from a code of ethics.
3. What, if any, items should be contained in an adequate code of ethics that are not in the submitted draft of canons.

The board of directors of AIEE referred the matter to the committee on code of principles of professional conduct, and that committee has it under consideration with the hope that a report may be submitted to the board before the summer meeting.

COMMITTEE ON CONSTITUTION AND BY-LAWS

The work of this committee has been limited to making two routine changes in the by-laws and to consideration of the desirability of making a number of changes in the constitution.

The two routine changes in the by-laws were made to make the by-laws conform with the action of the board of directors taken at its meeting on August 7, 1942, to abolish the institute policy committee.

Specifically, the changes were as follows:

Section 65—Delete the name of the institute policy committee from the listing under the heading "general committees."

Section 73—Delete the whole section.

The consideration of the desirability of making certain changes in the constitution is still in progress in the committee on constitution and by-laws, and it is expected that the committee's recommendations to the board of directors will be submitted well in advance of the close of the present administrative year.

The specific points involving the constitution which are now under consideration by the committee on constitution and by-laws are indicated by the brief titles which follow:

1. Elimination of the term "national."
2. Elimination of the term "professional engineer."
3. Tax-exempt status.
4. Election procedure.
5. Constitutional-amendment procedure.
6. Mailing amendment and election ballots together.
7. Qualifications for Fellow grade.

COMMITTEE ON RESEARCH

The third year of operation of this committee as a general committee continues to indicate that the change from technical-committee status has in no way interfered with the development of research papers by Institute members, as evidenced by a review of the large number of papers describing various research works presented during the year.

The number one activity of the committee has been that of sponsoring two research problems: one, an extensive welding research program sponsored jointly with the American Welding Society; and the other, research on insulating oils and cable saturants, project 74. J. C. Balsbaugh, who has been supervising this latter project, together with A. G. Assaf, presented, at the national technical meeting in January, some results of this work in a paper entitled, "Electrical Stability of Electrical Insulating Oils Under Limited Oxidation." The welding research program is very large, involving many problems, the total of the Welding Research Council budget being about $200,000. To this the Engineering Foundation and the Institute make rather small money contributions, but they cooperate as requested and thus keep in close touch with the work being done. The activity of the American Welding Society makes careful supervision of the project on the part of the Institute unnecessary.

Two AIEE members, Comfort A. Adams and Herman Halperin, are entitled to the thanks of the committee and the Institute for the work they have done in promoting the work of the welding research and the cable saturants research, respectively.

The committee has kept in close touch with all of the technical committees of the Institute by inviting each of those committees to designate one of its members as a special research-committee contact member. Through this means, any committee is readily able to call upon the committee on research as needed.

COMMITTEE ON SAFETY

The committee has continued its activities on the problems of safety from electrical fire and accident hazards. An important contribution was the completion of a bibliography of the publications in this field during the years of 1930 to 1942 by the Institute at the request of the committee, and the sale has been very gratifying, showing a widespread interest in information of this kind.

The effort to persuade the Student Branches to hold meetings on safety has been continued during the current school year. In March 1943, a post-card questionnaire returned by the Student Branches indicated that more than half of them held one or more meetings during the year on various phases of electrical safety. For the coming year, each Student Branch will be urged to hold a meeting on electrical-fire safety some time during the National Fire-Prevention Week in October, and also to hold at least one meeting on electrical-accident safety problems during the spring of 1944. The committee believes that safety is an excellent subject for Student Branch discussions.

Encouragement has been given to the presentation of papers and publication of articles in *Electrical Engineering* and elsewhere. One technical paper on "The Effect of Frequency of Let-Go Currents," a conference paper on the teaching of safety in one of the engineering colleges, and an article in *Electrical Engineering* on the role of lighting in accident prevention, as well as several short news items in *Electrical Engineering*, have been presented or published during the year.

The Institute has supported the activities of the National Fire Waste Council and the National Fire Protection Association, and the AIEE representatives have participated actively in the work of various committees in the American Standards Association dealing with codes covering electrical-safety problems.

War conditions make it increasingly important that electrical installations be maintained in good order, and the committee is lending its support in this direction.

Standards

STANDARDS COMMITTEE

The year 1942–43 from the standards committee's viewpoint was marked by the completion of an unusual number of standards projects, reports on proposed standards, the initiation of new lines of various wartime endeavors on the part of the many committees and subcommittees engaged in the normal processes of standards work, and also by the standards committee taking an active leading role in the promulgation of the Institute's wartime policies as defined by the committee on planning and co-ordination and adopted by the board of directors on August 7, 1942.

The war emergency has focused attention on the importance of the nationwide general use of industry standards and also on the very definite advantage to be gained through the formulation by established industry standardizing bodies of all war emergency standards or application guides for "duration" use. After careful consideration of the many technical problems involved in the preparation and use of emergency standards, the standards committee decided that in a great majority of the cases the wartime situation could be taken care of effectively by the preparation of wartime guides and interim reports covering the application of various types of standard electrical apparatus. Consequently, the standards committee, in collaboration with the technical program committee, and the special committee on co-operation with war agencies appointed by President Osborne, recommended that the Institute's technical committees prepare guides and reports covering application and operation of apparatus and, where necessary, emergency substitutions or modifications of present standards.

The preparation of some 13 or 14 such guides is now well along toward completion, and some are already completed. These guides and interim reports are educational in character and in no way mandatory. They have been prepared by technical committees for the purpose of making essential information immediately available to war industries, thus furthering the conservation of critical material and labor for the war emergency. It is expected that the standards committee will be able to publish these in suitable form for general use following the national technical meeting in Cleveland.

One instance of the immediate need for a modification of an existing standard has developed. It will be a wartime "Modification of and Supplement to" AIEE Standard 45, "Recommended Practice for Electrical Installations on Shipboard." This modification of Standard 45, sponsored by the committee on marine transportation, is to apply during the national emergency only. It is now in course of publication, and a very heavy demand from the marine field is already indicated.

It has been evident for a number of years that the growing complexity of standardizing procedure has made it ever more difficult to understand for those not continually in touch with the situation. With this problem in mind, the standards committee has developed and published a "Standards Manual." The groundwork for the manual was laid by the late R. E. Hellmund, and it is proving to be of great assistance to the many committeemen engaged in standards work. A recent development, a proposed addition to the manual, is a chart showing graphically the various steps through which standards projects must pass before final approval as AIEE and American Standards.

Among the many new and revised standards and reports considered and approved during the current year are the following: dielectric test voltage measurements; apparatus bushings; air switches and bus supports; switchgear assemblies; neutral grounding devices; wet tests; power circuit breakers; principles for selection of reference values for electrical standards; insulator tests; lightning arresters.

Several special committees and individual representatives were appointed to handle these new projects: terminal potheads; roof bushings and floor and wall bushings; general service conditions and altitude correction factors; co-ordination of AIEE and ASME test codes.

A considerable number of standards which are being handled by ASA sectional committees, on which the Institute has representatives, or for which the AIEE is acting as sponsor or joint sponsor have also been or shortly will be approved formally as American Standards. These cover the following subjects: rotating electrical machinery; transformers, induction regulators and reactors; graphical symbols for telephone, telegraph and radio use; graphical symbols for electric power control and measurement; graphical electrical symbols for architectural plans; a-c power circuit breakers, and rotating electrical machinery on railway locomotives and rail cars, and others.

It has also been necessary, because of the many personnel shifts due to the war emergency and to reorganization of inactive projects, to select a considerable number of new AIEE representatives to serve on sectional committees of ASA. Two such projects of particular interest at the present time are mercury-arc rectifiers and electric fences.

With a view toward conserving members' time and travel, only three committee meetings were held this year, and as much work as possible was handled effectively by correspondence.

UNITED STATES NATIONAL COMMITTEE OF THE INTERNATIONAL ELECTROTECHNICAL COMMISSION

The work of the International Electrotechnical Commission has been practically suspended because of the international situation. The central office is being maintained, however, and continues to receive mail at its old address in London, although its valuable records have been moved to a place of safety "for the duration." The United States National Committee of the IEC also is keeping its organization intact and will continue to meet annually so that, as soon as circumstances permit, it will be possible to resume activities.

The USNC at its annual meeting in December 1942 re-elected the following officers: E. C. Crittenden, president; L. F. Adams, vice-president; and H. S. Osborne, vice-president and treasurer.

During the year, the committee suffered the loss through death of R. E. Hellmund, and Clayton H. Sharp, its honorary president. Doctor Sharp was a founding member of the USNC and served as secretary from 1920 to 1924, as president from 1924 to 1940, and as honorary president from 1940 until his death. His absence will be particularly felt when peace returns and negotiations looking to the re-establishment of IEC work are undertaken.

Technical Committees

COMMITTEE ON AIR TRANSPORTATION

Because all members of the committee are concerned primarily with the war effort, the committee work was restricted to that which was absolutely necessary. Only one meeting was called, and that was held at Institute headquarters, January 28, 1943. All other work of the committee has been handled by correspondence.

The activities of the committee can be divided into three groups: standards, technical papers, and formation of sectional groups.

A standard is being prepared by J. W. Allen of the Bureau of Aeronautics to show the relation of temperature to altitude, knowledge of which is necessary for aviation practice. This information should be of great value to electrical engineers throughout the industry, as the extreme range in both temperature and altitude is not appreciated by most design engineers. A subcommittee is preparing revisions for all Institute standards regarding rotating machinery that may be used in aircraft. The work of this committee will deal primarily with d-c apparatus and with increasing altitude and temperature variations to care for aviation equipment.

The committee has invited and received confirmation of approximately 60 technical articles which are being prepared by electrical engineers who are active in the aviation industry. These papers have been correlated carefully into a program which will show all aspects of aviation activities. As these papers are published, it is anticipated that there will be a large influx of aviation electrical engineers into the Institute.

For the third activity of the committee, the formation of aviation divisions within local Sections, the logical starting point was the Los Angeles Section, where many aircraft plants are concentrated. More than 300 prospective members appeared for the first meeting of the proposed division. This division is now well organized and plans to meet at least six times a year to hear technical papers from its own members.

As a result of the success of the division at Los Angeles, invitations were prepared for the formation of similar divisions in other localities. At this writing, the Philadelphia Section is planning to form such a division, as eight large plants in its area employing many electrical engineers are concerned solely with aviation activities. There are many other plants in this area which have some aviation electrical activities. It is anticipated that a very active division will result. It is expected that similar divisions will be formed in the Washington–Baltimore area, the New York Section, the San Diego Section, and the Seattle Section.

COMMITTEE ON AUTOMATIC STATIONS

The committee held three meetings during the year, one at the summer convention in Chicago in June 1942, one at the national technical meeting in New York in January 1943, and one in April 1943, at which the work of the committee and subcommittees was discussed.

The bibliography on automatic stations, known as Special Publication 5, covering the period 1932–41 inclusive, was published in December 1942, and is now available at headquarters.

The subcommittee which distributed copies of a questionnaire throughout the United States and Canada, on a-c automatic-reclosing equipment applying to stub feeders, has now completed its report, which has been submitted for presentation at the national technical meeting in Cleveland in June.

A report on conductors used for supervisory control and telemetering has been prepared in connection with a questionnaire issued jointly with the committee on protective devices, and has been submitted by the committee for presentation at the national technical meeting in Cleveland in June 1943. This report covers only the data coming within the scope of the committee on automatic stations. The data prepared by the committee on protective devices was presented by that committee at the national technical meeting in January 1943.

The committee sponsored a paper on multichannel carrier-current facilities for a power line, together with a paper on automatic tie-line load control, an application to the Northwestern Electric Company's system.

A paper on a new control system for automatic parallel operation of load-ratio-control transformers was recommended by the committee on electrical machinery and the committee on automatic stations for presentation at the North Eastern District technical meeting.

A paper on experience with oil circuit reclosers on Rural Electrification Administration systems was recommended by the committee on protective devices and the committee on automatic stations for presentation at the national technical meeting in Cleveland.

COMMITTEE ON COMMUNICATION

The committee's activities have been quite adversely affected by the war effort, because

1. Executives and workers in the communication field have been overburdened with responsibilities.
2. Clearances for prepared papers dealing with this subject matter have been for the most part impossible to secure, particularly on up-to-date subjects.

Material, therefore, which the committee could develop and sponsor for technical meetings and for publication, has been very limited in quantity. In view of these circumstances, encouragement has been successfully given to more of the "informal-conference" method of disseminating information, and it is believed that further conferences should be encouraged for the future.

The committee has deferred for the duration the preparation of a report on the progress of the art in communications.

COMMITTEE ON DOMESTIC AND COMMERCIAL APPLICATIONS

The membership of the committee was expanded for the administrative year which began August 1. This move added new talent and diversified personal interests, and gave wide geographical representation.

At the national technical meeting in January, one conference organized by the committee was devoted to the subject

"Analysis of Military Loads and Postwar Applications." This conference placed emphasis on the need for accurate measurements of military loads, with applications and supply systems "cut to fit."

Inasmuch as wartime restrictions and conversion of industry during the year practically eliminated the production of domestic and commercial utilization devices, little or no justification was found for the committee to undertake work on applications for civilian needs. Agricultural electrification received a greater share of attention.

COMMITTEE ON ELECTRICAL MACHINERY

Committee Meetings. The committee held two meetings during the year, one at the summer convention in Chicago, and one at the national technical meeting in New York in January 1943, at which technical sessions at national and District technical meetings were discussed, and the work of the committee and its subcommittees was reviewed.

Subcommittee Activities. The synchronous machinery subcommittee has completed its work on the revision of the test code on synchronous machinery, and this code is now being reviewed by the test code coordinating subcommittee.

The transformer and induction-regulator subcommittee completed the "Interim Report on Guides for Overloading Transformers and Voltage Regulators," which was presented at the summer convention in Chicago in June 1942, and at the Pacific Coast convention in Vancouver in September 1942. The subcommittee has prepared an "Interim Report on Overloading of Distribution Transformers" which is to be presented at the national technical meeting in Cleveland. As a result of the work of the subcommittee, several papers on the loading of transformers during the war emergency were presented at the national technical meeting in New York in January 1943, and additional papers will be presented at the national technical meeting in Cleveland.

The induction machinery subcommittee, in co-operation with the committee on industrial power applications, prepared a report on "Proposed Guide for the Selection of Electric Motors and Motor Controllers," as occasioned by the war emergency, and this report was presented at the national technical meeting in New York, in January.

The insulation resistance subcommittee is working on a test code for measuring insulation resistance, and it is estimated that at least another year will be required for completion of this work.

AIEE Technical Meetings. The committee on electrical machinery sponsored one session at the summer convention, at which five papers on overload operation of transformers and rotating machinery were presented. The committee participated in joint sponsorship of a session on mercury-arc-rectifier applications at this convention.

The committee sponsored one technical session and one conference session at the national technical meeting in New York.

The committee plans to sponsor two sessions at the national technical meeting, in June, one of these to be on synchronous machinery, and the other on transformer loading.

Co-operation With Other Committees. J. F. Calvert has been designated as contact man with the committee on research. The committee on electrical machinery is represented on the subcommittee on temperature measurements of the committee on instruments and measurements and on the joint subcommittee of AIEE standards committee on outdoor and indoor cable terminal potheads, roof bushings, and floor and wall bushings.

COMMITTEE ON ELECTRONICS

By action of the board of directors, a technical committee on electronics has been set up. This committee has taken over the functions of the former joint subcommittee on electronics which has been carrying on the electronics activities of the Institute for several years. The scope of the new committee is as follows:

> Treatment of all matters in which the dominant factor is electronics, particularly those dealing with the design, characteristics, or behavior of devices of an electronic nature, or of the circuits associated with them. The treatment of complete equipments or systems may be considered within the scope of this committee if they depend predominantly on electronic devices for their operation. If the field of application of such equipments or systems falls also within the scope of other technical committees, action with those committees may be joint.

In accordance with this statement of scope, the mercury-arc-rectifier subcommittee has been transferred from the committee on electrical machinery to the committee on electronics.

Much of the work being done in electronics is closely connected with military applications, so that very little material is available for publication at this time. The program activities of the committee have been principally concerned with the organization of informal technical conferences on topics of interest in connection with the war effort.

At a meeting of the committee held during the national technical meeting in January, the question of a standardization program in electronics was discussed. In co-operation with the subcommittee on electronic standards of standards co-ordinating committee 7, a joint subcommittee on standardization policy was appointed to study and recommend such a program. This committee met and made its report. It recommended the transfer of the subcommittee on electronic devices from coordinating committee 7 to the technical committee on electronics, in order to centralize all standardization activity in the technical committee. This transfer was made, after approval of the move was given by the standards committee. The committee now operates as the subcommittee on electronic standards. The policy committee further recommended that the following program of standardization be undertaken by the committee on electronics:

1. The preparation of standards of nomenclature and definitions in its field.
2. The preparation of a basic guiding standard in electronics modeled on AIEE Standard 1, "General Principles Upon Which Temperature Limits Are Based in the Rating of Electrical Machinery and Apparatus."
3. The preparation of standard methods of measurement of the characteristics and quantities associated with electron tubes.

The first of these projects is being undertaken immediately by the subcommittee on electronic standards. The latter two should be undertaken as soon as personnel can be made available, but it is doubtful if this will be possible during the war period.

COMMITTEE ON INSTRUMENTS AND MEASUREMENTS

The pressure of war work throughout the year has materially decreased the time available for either the preparation of papers or the development of standards. Nevertheless two technical sessions on instruments and measurements were held during the year. At the meeting in Chicago, three formal technical papers were presented. Following this, a conference was held on the measurement and specifications of surface finish. At the national technical meeting in New York, five formal papers were presented at the measurements session, and two other measurement papers were given at the switching equipment session. The former group were, for the most part, related to radio communication.

At the fall meeting of the committee in November 1942, the possibility of preparing a guide or interim report covering the use of instruments under emergency conditions was considered. It was felt, however, that in the nature of things there was very little to be said except for a few exceedingly obvious procedures, and hence it was decided that no guides were required.

Two new subcommittees have been formed. The first, on measurements for industrial control, has surveyed the field, developed contacts with other societies interested in this field, and is ready to join in attacking any measurement problems which may develop in it. The other, on high-frequency measurements, is planning a number of conferences or sessions at which a knowledge of the tremendous war-stimulated development of methods in this field can be disseminated to the Institute membership. Military need for secrecy will bar many items, but it is hoped that some features may be presented even before the close of hostilities.

The subcommittee on definitions has under discussion some 26 proposed revisions and additions to the definitions in the measurements section of the American Standard, "Definitions of Electrical Terms." These changes will bring about closer agreement with the wordings used in other standards.

The subcommittee on temperature measurements has made good progress in preparing a draft of a master test code. This will soon be ready for review by the main committee. When completed, it should prove of value, both by eliminating the need for repeating the details of measurement methods in the many standards for different types of apparatus, and by guiding the art toward the use of satisfactory and generally recognized methods for temperature measurement.

The subcommittee on recording instruments has begun a revision of AIEE tentative standard 40 but finds that the art is growing very rapidly at the present time. Hence, it may be desirable to postpone the revision of this standard for a while.

COMMITTEE ON LAND TRANSPORTATION

No meeting of the committee was held in 1942, but a land-transportation program for the national technical meeting in January was developed by correspondence. This included two formal papers on selection of railroad motive power and maintenance of motive power, respectively, and two conference papers on train communication, and electrification of the Sorocabana Railway in Brazil, respectively. The latter two were presented formally, the first at the Kansas City, Mo., District technical meeting, and the second at both the Pittsfield, Mass., and the Kansas City, Mo., District technical meetings. Another paper on the advantages of high-speed traction motors was developed for the Kansas City meeting.

One session of the committee was held during the January meeting. It was decided to omit a formal session at the national technical meeting in June, but subjects for future activities were developed. These included means of developing a set of standards for grounding on electric locomotives.

The paper on selection of railroad motive power developed extensive discussion, and it has been suggested that the study be continued with the co-operation of the railroad division and the gas, oil, and power division of the American Society of Mechanical Engineers.

COMMITTEE ON MARINE TRANSPORTATION

The year 1942–43 has been a war year for the committee on marine transportation, in that Standard 45 had to be reviewed carefully, and revisions had to be made in cases where essential war materials were specified. This has necessitated considerable work on the part of each member, already burdened with war work in various shipbuilding yards and allied industries.

The War Production Board has been interested in the work of this committee, and, through the president of the Institute, arranged to have a representative present at the meetings. The purpose, as stated in their letter, was to offer suggestions for the development of war emergency standards.

The work of reviewing Standard 45 to meet existing emergency conditions has been a difficult task. A large part of the work has been done by the subcommittees on wires and cables, power applications, distribution, and fittings and appliances. The work under the cognizance of the subcommittee on wires and cables was started during the previous fiscal year, but, because of the volume of work involved, it was carried over into this year.

The first meeting to hear reports from all subcommittees was held November 6, 1942. At this time, revisions as of that date were acted upon, but not all reports were complete. The subcommittees were instructed to bring their work to a conclusion at the earliest date possible, and, by correspondence and letter ballots, their work was completed in January of this year. A meeting of the main committee was held January 22, 1943, to receive final reports and approve the revisions applicable for the duration of the present emergency. In addition to this work, some few revisions and corrections have been made to keep Standard 45 up-to-date with the best current practice for electrical installations on shipboard.

The special committee appointed last year to confer with the American Bureau of Shipping completed its work and made its report at the meeting November 6, 1942. The purpose of this special committee was to reconcile existing differences between Standard 45 and the ABS rules covering the same class of electrical requirements. The results were very satisfactory, and two widely used sets of rules are now more nearly in agreement.

An extensive report on revisions of Standard 45 was compiled and submitted to the standards committee for action at the meeting held March 25, 1943. These modifications were approved, and are solely for the purpose of conserving critical materials. They are war emergency measures to be used only for the duration. The revisions are in a form which gives the modifications to the sections altered, and will have to be used in conjunction with Standard 45. In this form, it leaves Standard 45 intact and suitable for use without reprinting upon the termination of the war and emergency conditions.

A meeting of this committee will be held before the close of the administrative year to act on any pending revisions to be issued as an addenda to the basic Standard 45. If possible, unfinished work now before the committee will be given final action in order not to carry it over into the next year.

COMMITTEE ON POWER GENERATION

Two major committee projects are in progress, one of which will be completed this year.

The standards committee, at its October 9, 1942, meeting in New York, requested that each technical committee consider the preparation of wartime guides to help the electrical industry find ways and means of getting "more out of what it has." The committee on power generation considered this and decided that, because of the many variables in the construction and operation of generating stations and systems, it could best serve the desires of industry by compiling a number of "hints" on this subject. The logical approach was to make a survey of what had been done by the manufacturers and the operating companies. Two subcommittees have completed these assignments, and a complete report entitled "Emergency Measures to Increase Output of Generating Equipment and Systems" is being prepared.

One subcommittee assembled the suggestions in connection with generators. The generator was selected, as it is one of the most important links in the station, and usually other equipment is closely associated with its design.

The other subcommittee prepared a questionnaire which was quite widely distributed to the larger operating groups. This questionnaire covered such matters as loading of generators by temperature, rebuilding, and rehabilitation; interconnected operation; effect of system voltage and frequency on load; power-factor correction; maintenance schedules; and personnel problems.

Both of these reports, in preliminary form, were presented at the national technical meeting in New York and at the District technical meeting in Kansas City. They will be presented at a joint meeting of the electrical equipment committee and prime movers committee of the Edison Electric Institute to be held in Chicago in May. The comments and suggestions received from these conferences will be incorporated in a final report that will be presented at the national technical meeting in Cleveland.

The other project, which is fast nearing completion, is the preparation of specifications for prime-mover governing. This is a joint AIEE–ASME activity and is being carried on by a special committee. The specifications will be presented at the national technical meeting in Cleveland.

COMMITTEE ON POWER TRANSMISSION AND DISTRIBUTION

On account of the war, the committee activity during the current year has followed a somewhat different pattern from that of the past. Committee meetings have been limited in number, and most activities have been carried on mainly by correspondence.

The distribution subcommittee sponsored a technical session on "Distribution Systems in Wartime" at the 1942 winter convention. By this early action, less than two months after the Pearl Harbor attack, it accomplished, in its field, a rather complete discussion of the problems confronting the electric-power industry as a result of the war situation. As no further problems of pressing need have arisen in the distribution field during the year, this subcommittee's activities have been confined largely to sponsoring papers within its field.

The general systems subcommittee has been active during the past year in preparing a comprehensive report on "Shunt Capacitors," which was presented at a technical session at the national technical meeting in January 1943.

The transmission subcommittee sponsored two conferences at the January meeting, one on "Emergency Loading of Transmission Systems," at which six conference papers were presented and discussed, and the other on "Emergency Rating of Power Cables," which likewise included six conference papers. Steps are now being taken to have these last six conference papers compiled under one cover and made available in pamphlet form.

This subcommittee is also planning to

sponsor, at the national technical meeting in June, other papers or committee reports in the same field touching on emergency overloading of transmission conductors and cables.

The lightning and insulator working group of the transmission subcommittee completed the proposed revision of Standard 41, known as 41-*A* in revised form, covering the subject of "Insulator Tests." This revision was approved by the main committee and forwarded to the standards committee with recommendations for its adoption as a standard. It is understood that the standards committee will offer this standard to ASA with the recommendation that appropriate action be taken for it to appear as an American Standard.

The stations subcommittee sponsored a conference at the January meeting entitled "Substation Designs to Meet Wartime Conditions." Thirteen contributors presented material on a wide range of subjects including protective lighting, antisabotage measures, minimum use of critical materials, substitution of noncritical materials, and operating methods to increase the available electric capacity.

In addition to the conferences just referred to, which were organized and made possible by the active work of the subcommittees, the main committee sponsored three technical sessions at the January meeting: one on capacitor generators and transformers, the second on transmission lines, and the third on transmission and distribution. A large majority of the papers in these technical sessions dealt, directly or indirectly, with problems relating to aiding the war effort in the electrical industry.

The committee is planning two or more technical sessions at the meeting in June.

In these wartimes, it seems undesirable to hold frequent committee meetings except where major problems of importance arise which affect the war effort. There are several new projects which have been suggested for consideration by the committee, but it has not been considered expedient under existing conditions to initiate active work on them at this time. The starting of new projects in the main committee, its subcommittees, or working groups, unless of vital importance, is believed to be more of a detriment to the war effort than an aid, and it is planned for the future to limit committee activities, so far as feasible, to the consideration and development of projects essential to a successful prosecution of the war.

COMMITTEE ON PROTECTIVE DEVICES

The war has had marked effects upon the work of the committee. Several peacetime activities have been dropped. It has been necessary to develop as rapidly as possible two wartime guides for the conservation of materials in construction. Certain standards have been pushed to completion as rapidly as possible. Three standards have been approved by the board of directors, and another has received the approval of the standards committee.

The committee sponsored 36 technical papers presented at national meetings and two presented at the District meeting in Pittsfield. It also sponsored five conference papers.

The major divisions of the work of the committee were assigned to four subcommittees: circuit breakers, switches, and fuses; fault-current-limiting devices; lightning arresters; and relays.

Circuit Breakers, Switches, and Fuses. The subcommittee on circuit breakers, switches, and fuses prepared the "Interim Report on Application and Operation of Circuit Breakers and Switchgear," which, in preliminary draft form, was discussed at the national technical meeting in January, and in final form at the North Eastern District technical meeting in Pittsfield.

Standard 19 on oil circuit breakers has been revised, and a working group has been organized to revise Standard 20 on air circuit breakers. It is expected that the revision of proposed Standard 25 on fuses will be completed soon.

Fault-Current-Limiting Devices. A subcommittee report, "Correlation of System Overvoltages and System Grounding Impedance," was presented at the January meeting. A preliminary form of questionnaire on practices in grounding power systems has been prepared, and the final form is expected to be completed soon. Comments and criticisms on Standard 32, on neutral grounding devices, are being collected.

Lightning Arresters. An operating guide for the use of lightning arresters was presented as an individual paper at the January meeting, and is scheduled for presentation as a subcommittee report at the national technical meeting in June.

Standard 24 on protector tubes has been published in report form. Work on a report on distribution tubes is in progress, and it may be combined with the former.

Standard 28 on lightning arresters has been approved by the Institute and transmitted to ASA for consideration.

Relays. The subcommittee on relays has been actively engaged in a number of studies: pilot-wire experience, out-of-step protection, manufacturers' catalog information, relay-testing-procedure questionnaire, and a report on experience with relay systems under actual combat conditions.

Section 4 of American Standard *C*-57 on instrument transformers has been revised, and comments are being studied for final revision following a year's trial use. Standard 31 on potential devices and coupling capacitors has completed its period of trial use.

A conference on relay testing and wartime relay problems was held at the January meeting.

Awards

COMMITTEE ON AWARD OF INSTITUTE PRIZES

Four national prizes and ten District prizes were awarded for papers presented during the calendar year 1941 and for the student papers presented during the academic year ending June 30, 1942.

The papers considered were of a high order, and in determining the awards for the national best paper prizes and the initial paper prize the committee had the benefit of gradings and recommendations of the technical committees. In addition to the four papers which were awarded national prizes, honorable mention was given to others.

The changes made in the rules on award of national and District prizes, issued in the new edition of January 1941, have been in effect for more than two years and have been found to be working satisfactorily.

EDISON MEDAL

The Edison Medal, which is awarded by a committee composed of 24 members of the Institute, was awarded for 1942 to Edwin H. Armstrong, "for distinguished contributions to the art of electric communication, notably the regenerative circuit, the superheterodyne, and frequency modulation," and was presented January 27, 1943, during the national technical meeting. The medal may be awarded annually for "meritorious achievement in electrical science, electrical engineering, or the electrical arts."

LAMME MEDAL

The Lamme Medal committee awarded the medal for 1942 to Joseph Slepian, associate director of research, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., "for his contributions to the development of circuit-interrupting and current-rectifying apparatus." Arrangements are being made for the presentation of the medal at the national technical meeting in Cleveland, Ohio, June 21–25, 1943. The medal may be awarded annually to a member of the AIEE "who has shown meritorious achievement in the development of electrical apparatus or machinery."

JOHN FRITZ MEDAL

The John Fritz Medal board of award, composed of representatives of the American Society of Civil Engineers, American Institute of Mining and Metallurgical Engineers, American Society of Mechanical Engineers, and AIEE, awarded the 39th medal for 1943, to Willis R. Whitney, vice-president in general charge of research, General Electric Company, Schenectady, N. Y., "for distinguished research, both as an individual investigator and as an outstanding and inspiring administrator of pioneering enterprise, co-ordinating pure science with the service of society through industry." It was presented January 27, during the AIEE meeting.

HOOVER MEDAL

The Hoover Medal was established through a trust fund created by a gift from Conrad N. Lauer, and is to be awarded periodically "to a fellow engineer for distinguished public service" by a board representing the national societies of civil, mining and metallurgical, mechanical, and electrical engineers. The sixth medal, for 1942, was awarded to Gerard Swope, president, General Electric Company, and was presented January 27, during the AIEE meeting.

ALFRED NOBLE PRIZE

This prize, established in 1929, consists of a certificate and a cash award from the income of a fund contributed by engineers and others to perpetuate the name and achievements of Alfred Noble, past president of ASCE, and of the Western Society of Engineers. It may be made to a member of any of the co-operating societies, ASCE, AIME, ASME, AIEE, or WSE, for a technical paper of particular merit accepted by the publication committee of any of these societies, provided the author, at the time of such acceptance, is not over 30 years of age. The award for 1941–42 was presented to George Wesley Dunlap (A'35, M'42), General Electric Company, Schenectady, N. Y., for his paper "The Recovery Voltage Analyzer for Determination of Circuit Recovery Characteristics," published in AIEE *Transactions*, volume 60, 1941, November section, pages 958–62.

WASHINGTON AWARD

Audrey A. Potter, dean of engineering, Purdue University, Lafayette, Ind., and chairman of the National Advisory Committee on Engineering and War Training, was awarded the Washington Award for 1943 for "distinguished leadership in engineering education and research, and patriotic service in mobilizing technical knowledge for victory in war and peace." The presentation was made at a dinner on February 24, 1943. This award may be made annually to an engineer, by the commission of award composed of nine representatives of the Western Society of Engineers, and two each of ASCE, AIME, ASME, and AIEE.

THE MARSTON MEDAL

The Marston Medal was established in 1938 by the Iowa State College through the inspiration and generosity of Anson Marston, dean emeritus of engineering. It is awarded, not oftener than once a year, to an engineering alumnus of at least 30 years' standing for achievement in engineering in a broad field. The award is made by a board composed of four alumni, one representative each of the ASCE, ASME, AIEE, and American Institute of Chemical Engineers, and the dean of engineering who serves as chairman. The presentation is a feature of the commencement exercises.

Awards have been made as follows:

1938	E. O. Shreve
1939	Thomas H. MacDonald
1940	James W. Hook

CHARGES LE GEYT FORTESCUE FELLOWSHIP COMMITTEE

Because of conditions brought about by the war there was no meeting of the committee and no award made for the year 1943–44. This action is the same as was taken last year for similar reasons.

Joint Activities

UNITED ENGINEERING TRUSTEES, INC.

This organization manages property and funds held jointly by the four founder societies, including the Engineering Societies Building, the Engineering Societies Library, and the endowment funds of the Engineering Foundation.

During the year, a considerable program of renovation of the Engineering Societies Building was completed, despite increases in cost and difficulties in securing materials. Labor and material costs have increased, income from rental of halls and auditorium has been reduced, and it is expected that these adverse effects will become more pronounced.

The building is reported to be in excellent condition and is fully occupied by the Founder Societies and associates.

An abstract of the annual report of the corporation for the year which ended September 30, 1942, appeared in *Electrical Engineering* in January 1943, pages 43–4.

ENGINEERING FOUNDATION

The Engineering Foundation was established in 1914 as a joint organization of the four societies, ASCE, AIME, ASME, and AIEE, for "the furtherance of research in science and engineering, and the advancement in any other manner of the profession of engineering and the good of mankind." Each research project supported by it is under the sponsorship of one of the founder societies.

In recent years, the Institute has been sponsor for three research projects: stability of impregnated-paper insulation, at the Johns Hopkins University; insulating oils and cable saturants, at the Massachusetts Institute of Technology; and the comprehensive program of the Welding Research Council, with the American Welding Society as joint sponsor. The last two are still receiving Engineering Foundation support.

During its fiscal year 1942–43, the Engineering Foundation is supplying funds for 17 research projects bearing on war work and also is supporting studies in the development of the engineer through its continuing contribution to the Engineers' Council for Professional Development.

A comprehensive abstract of the annual report for the year which ended September 30, 1942, appeared in *Electrical Engineering* in January 1943, pages 44–6.

ENGINEERING SOCIETIES LIBRARY

The Engineering Societies Library, which was formed by combining the separate libraries of the four national societies of civil, mining and metallurgical, mechanical, and electrical engineers, with its composite card catalog now in preparation, probably constitutes the best collection of its type in the United States. On September 30, 1942, the library had 154,770 volumes.

Special services rendered by the library include: photoprints, searches, abstracts, translations, bibliographies, book loans by mail, and others. The library board has been considering means by which members might be informed more adequately regarding the services available.

An abstract of the annual report of the library appeared on pages 46–7 of *Electrical Engineering*, January 1943.

EMPLOYMENT SERVICE

Operating as an incorporated nonprofit-making organization, the Engineering Societies Personnel Service, Inc., assists members of the founder societies desiring to secure new positions and conversely assists employers searching for engineers. Nonmembers may receive such assistance when positions available cannot be filled by members.

Offices are operated in New York, N. Y., Boston, Mass., Detroit, Mich., Chicago, Ill., and San Francisco, Calif., with the co-operation of the Engineering Societies of New England in Boston, the Engineering Society of Detroit in that city, the Western Society of Engineers in Chicago, and the Engineers Club of San Francisco in that city.

The service is supported by the joint contributions of the societies and the individuals who secure positions. An analysis of registration and placement records as reported to the national societies is given in Table XI.

ENGINEERS' COUNCIL FOR PROFESSIONAL DEVELOPMENT

This Council, organized in 1932 to engage in activities leading toward the en-

Table XI. Analysis of Employment Service

	Men Registered						Men Placed					
Month	New York	Chicago	San Francisco	Detroit	Boston	Total	New York	Chicago	San Francisco	Detroit	Boston	Total
1942												
May	159	43	71	32		265	42	20	32	8		102
June	119	60	54	36	21	290	38	22	32	11	3	106
July	100	55	51	24	64	294	40	18	31	11	4	104
August	132	47	48	36	41	304	40	13	25	17	5	100
September	107	49	53	29	42	280	31	15	27	9	11	93
October	100	35	34	26	33	228	31	13	17	10	14	85
November	91	28	43	14	43	219	30	14	15	6	20	85
December	84	27	39	15	38	203	31	12	25	5	11	84
1943												
January	123	22	52	25	29	251	38	14	27	6	3	88
February	120	44	56	28	36	284	20	11	18	7	8	64
March	130	42	57	23	22	274	30	15	13	8	9	75
April	112	26	51	17	29	235	30	12	14	7	10	73
Total	1,377	478	609	305	398	3,127	401	179	276	105	98	1,059

hancement of the professional status of the engineer, includes three representatives of each of the eight participating organizations, which are the national societies of civil, electrical, mechanical, and mining and metallurgical engineers, the Society for the Promotion of Engineering Education, the National Council of State Boards of Engineering Examiners, and the Engineering Institute of Canada. Its principal activities have been carried on by four committees: student selection and guidance, engineering schools, professional training, and professional recognition.

The status of the accrediting program, after actions taken at the annual meeting on October 18, 1942, and not including options under accredited curricular, is given below:

Curricula	
Fully accredited	496
Provisionally accredited	75
Not accredited	101
Action pending or deferred	5
	677

War problems and their effects upon the work of ECPD were thoroughly discussed at the annual meeting October 18, plans were developed for carrying out ECPD functions locally, a statement of canons of ethics for engineers was approved in principle and submitted to the constituent societies, a new special committee on unionization of engineers was authorized, and the publication of a "Manual for Junior Engineers" was authorized.

A comprehensive report on the annual meeting, including the reports of committees was published in *Electrical Engineering* in December 1942, pages 626–30.

JOINT CONFERENCE COMMITTEE

The joint conference committee, composed of the presidents and secretaries of the founder societies and the American Institute of Chemical Engineers, met in New York April 19, 1943, and considered numerous topics of interest to the several societies.

REPRESENTATIVES

In addition to the representatives in the joint organizations mentioned above, the Institute is represented in numerous other joint activities of interest to engineers in general. The names of its representatives upon nearly 30 joint committees and national organizations were published in the September 1942 issue of *Electrical Engineering*, pages 485–6, and in the 1943 Year Book.

Appreciation

With particular pride in the accomplishments of the Institute during the past year, despite the prevailing difficulties, the board of directors thanks the national committees, and the District, Section, and Branch officers for their continuing enthusiasm and their generous contributions of time and effort; it thanks the members in general for their keen interest in and support of all Institute activities.

Respectfully submitted for the board of directors.

H. H. HENLINE
National Secretary

May 27, 1943

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Balance Sheet April 30, 1943

Exhibit A

ASSETS		
Property Fund Assets:		
One-fourth interest in physical properties of United Engineering Trustees, Inc.:		
Land, buildings, and equipment (less depreciation and renewal reserve)	$389,888.61	
Funded depreciation and renewal reserve	108,559.87	
Total	$498,448.48	
Equipment:		
Library—volumes and fixtures—estimated value	36,366.37	
Office furniture and fixtures (less reserve for depreciation, $26,287.30)	8,630.42	
Works of art, etc.	3,001.35	
Investment less reserve in full $943.73—Schedule 1		
Total property fund assets		$546,446.62
Restricted Fund Assets:		
Securities—at cost, less reserve, $20,779.92 (quoted market value, $275,195.03) Schedule 1	$278,398.33	
Cash (Exhibit C)	49,390.36	
Accrued interest receivable	462.62	
Total restricted fund assets		328,251.31
Operating Fund Assets:		
Cash (Exhibit B)	$ 54,204.19	
Accounts receivable:		
Members—for dues (less reserve, $8,000.00)	10,075.64	
Advertisers	516.70	
Miscellaneous	1,931.88	
Accrued interest receivable	3,594.63	
Inventories:		
Transactions, etc.	1,468.50	
Electrical Definitions	6,792.50	
Work in process (May issue of *Electrical Engineering*, etc.)	4,909.63	
Text and cover paper	7,106.75	
Badges	1,697.49	
Total operating fund assets		92,297.91
Total		$966,995.84

LIABILITIES		
Property Fund Reserve		$546,446.62
Restricted Fund Reserves:		
Reserve Capital fund	$381,156.04	
Pension fund	28,187.50	
Life Membership fund	7,761.92	
International Electrical Congress of St. Louis Library fund	5,488.05	
Lamme Medal fund	4,569.83	
Mailloux fund	1,087.97	
Total restricted fund reserves		328,251.31
Operating Fund Reserves, Liabilities, Etc.:		
Accounts payable	$ 13,485.63	
Deferred income:		
Dues received in advance	4,877.07	
Entrance fees and dues advanced by applicants for memberships	1,199.60	
Miscellaneous (including unallocated receipts)	1,070.51	
Subscriptions to publications received in advance	5,450.82	
Operating fund reserves	66,214.28	
Total operating fund reserves, liabilities, etc.		92,297.91
Total		$966,995.84

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Securities Owned, April 30, 1943

Schedule 1

	Principal Amount of Bonds or Number of Shares of Stock	Restricted Funds						Property Fund (Equipment Replacements)
		Reserve Capital Fund	Pension Fund	Life Membership Fund	International Electrical Congress Library Fund	Lamme Medal Fund	Total Restricted Fund	
Railroad Bonds:								
Allegheny Corporation 20 year collateral trust conv. 5%, due 1949 (stamped)	$15,000.00	$ 10,627.50					$ 10,627.50	
Baltimore & Ohio Railroad Company 6% refunding and general mortgage series C, due 1995 (stamped)	12,000.00	8,940.00				$4,330.00	13,270.00	
Chicago & Erie Railroad Company 5% first mortgage, due 1982	1,000.00	1,105.00					1,105.00	
New York Central Railroad Company 5% refunding and improvement mortgage series C, due 2013	6,000.00	5,742.50					5,742.50	
Northern Pacific Railway Company 6% refunding and improvement mortgage series B, due 2047	10,000.00	10,962.50					10,962.50	
St. Louis-San Francisco Railway Company 5% prior lien mortgage series B, due 1950 (certificates of deposit)	6,000.00*	5,497.50*					5,497.50*	
Total railroad bonds		$ 42,875.00				$4,330.00	$ 47,205.00	
Public Utility Bonds:								
New York & Queens Electric Light & Power Company $3^1/_2$% first and consolidated mortgage, due 1965	10,000.00	$ 11,000.00					$ 11,000.00	
Bond and Real Estate Mortgage:								
Fidelity Union Title and Mortgage Guaranty Company first mortgage certificates (on property 75–79 Prospect Street, East Orange, N. J.) 4%, due 1944	14,132.00*	$ 13,188.27*					$ 13,188.27*	$943.73*
United States Government Bonds:								
Treasury Savings bonds series D, due July 1, 1949	10,000.00	$ 7,644.00					$ 7,644.00	
Treasury Savings bonds series D, due January 1, 1950	10,000.00	7,500.00					7,500.00	
Treasury bonds 2%, due 1950/48	77,000.00	78,130.94					78,130.94	
Treasury bonds, $2^1/_2$%, due 1968/63	20,000.00	15,000.00		$5,000.00			20,000.00	
Treasury bonds $2^1/_2$% due 1972/67	6,300.00	1,100.00			$5,200.00		6,300.00	
Defense bonds series F, due June 1, 1953	34,000.00	25,160.00					25,160.00	
Defense bonds series G, $2^1/_2$%, due December 1, 1954	18,000.00		$18,000.00				18,000.00	
Total United States Government bonds		$134,534.94	$18,000.00	$5,000.00	$5,200.00		$162,734.94	
Capital Stocks:								
American Can Company	60 shares	$ 4,988.40					$ 4,988.40	
American Telephone & Telegraph Company	30 shares	4,897.95					4,897.95	
Boston Edison Company	200 shares	4,927.50					4,927.50	
Commonwealth Edison Company	200 shares	7,580.68					7,580.68	
Eastman Kodak Company	35 shares	4,768.23					4,768.23	
E. I. du Pont de Nemours Company	40 shares	6,398.20					6,398.20	
General Electric Company	130 shares	4,463.80					4,463.80	
General Motors Corporation	100 shares	4,235.53					4,235.53	
International Harvester Company	100 shares	5,030.50					5,030.50	
International Match Realization Co., Ltd. voting trust certificates for capital shares of International Match Corporation	6 shares*	2,094.15*					2,094.15*	
Sears Roebuck & Company	100 shares	6,014.97					6,014.97	
Standard Oil Company of New Jersey	110 shares	4,791.78					4,791.78	
Union Carbide & Carbon Corporation	70 shares	4,858.35					4,858.35	
Total capital stocks		$ 65,050.04					$ 65,050.04	
Total		$266,648.25	$18,000.00	$5,000.00	$5,200.00	$4,330.00	$299,178.25	$943.73
*Less reserve in full for the securities designated *, considered to be of doubtful value		$ 20,779.92					$ 20,779.92	$943.73
Total Securities, Less Reserve		$245,868.33	$18,000.00	$5,000.00	$5,200.00	$4,330.00	$278,398.33	

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Statement of Cash Receipts and Disbursements of Operating Fund for the Year Ended April 30, 1943

Exhibit B

Cash on Deposit With The National City Bank of New York, May 1, 1942		$ 16,633.48
Receipts:		
Dues (including $99,684.00 allocated to *Electrical Engineering* subscriptions)	$222,544.15	
Advertising	85,586.44	
Transactions subscriptions	4,802.03	
Electrical Engineering subscriptions	10,142.38	
Electrical Definitions	13,205.05	
Miscellaneous publications (preprints, Standards, *Transactions* index, etc.)	17,020.75	
Students' fees	13,337.75	
Entrance fees	11,384.51	
Membership badges	3,585.20	
Transfer fees	1,503.37	
Interest and dividends on investments of Restricted Capital fund	6,780.28	
Transfer from Life Membership fund	1,350.12	
Miscellaneous	706.38	
Total receipts		391,948.41
Total		$408,581.89
Disbursements:		
Publication expense:		
Electrical Engineering	$ 98,867.51	
Transactions	7,540.41	
Year Book	9,884.71	
Miscellaneous publications (preprints, Standards, *Transactions* supplement, etc.)	14,119.12	
Institute meetings	14,805.96	
Institute Sections	39,416.78	
Institute Branches	3,113.79	
Edison Medal committee	186.69	
Finance committee	701.10	
Headquarters committee	50.72	
Membership committee	9,307.62	
Standards committee:		
Electrical Definitions	18,055.17	
Others	8,669.83	
Committee on cooperation with war agencies	212.35	
Technical committees	464.47	
Forward	$225,396.23	$408,581.89

Total (forward)		$408,581.89
Disbursements (forward)	$225,396.23	
Traveling expenses:		
Geographical Districts:		
Executive committees	2,768.68	
Vice-presidents	482.82	
Branch counselors and chairmen	4,254.11	
President's appropriation	1,483.73	
Board of directors	6,500.91	
National nominating committee	1,226.67	
Administrative expenses	50,061.53	
Geographical Districts—Branch paper prizes	324.00	
Institute prizes, national	409.00	
American Co-ordinating Committee on Corrosion	25.00	
American Standards Association	1,500.00	
United Engineering Trustees, Inc.:		
Building assessment	10,984.81	
Library assessment	10,401.60	
Engineering Societies Personnel Service, Inc.	814.50	
Engineers' Council for Professional Development	1,700.00	
Engineers Defense Board	110.00	
Engineering Foundation Projects:		
Insulating oils and cable saturants	250.00	
Welding research	250.00	
Hoover medal	83.25	
International Committee on Illumination	200.00	
John Fritz medal	269.75	
Production clinics	80.34	
Committee on radio broadcasts	174.30	
National Fire Protection Association—Dues	60.00	
Membership badges	3,766.30	
Legal services	250.00	
Transfers:		
To Pension fund	10,000.00	
To Reserve Capital fund	20,000.00	
Office furniture and fixtures, and repairs	530.17	
Miscellaneous	20.00	
Total disbursements		354,377. 7
Cash on Deposit With The National City Bank of New York, April 30, 1943		$ 54,204.19

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Statement of Cash Receipts and Disbursements of Restricted Funds for the Year Ended April 30, 1943

Exhibit C

	Restricted Funds						
	Reserve Capital Fund	Pension Fund	Life Membership Fund	International Electrical Congress of St. Louis Library Fund	Lamme Medal Fund	Mailloux Fund	Total Restricted Funds
Cash on Deposit With The National City Bank of New York and Various Savings Banks, May 1, 1942	$35,520.87	$18,000.00	$8,805.91	$141.80	$206.83	$1,075.85	$ 63,751.26
Receipts:							
Interest on bonds				$130.00	$ 48.00		$ 178.00
Interest on bank balances			$ 31.05			$ 12.12	43.17
Proceeds from sale, etc. of securities	$ 9,944.84						9,944.84
Transfer from operating fund	20,000.00	$10,000.00					30,000.00
Life membership fee			228.21				228.21
Total receipts	$29,944.84	$10,000.00	$ 259.26	$130.00	$ 48.00	$ 12.12	$ 40,394.22
Total	$65,465.71	$28,000.00	$9,065.17	$271.80	$254.83	$1,087.97	$104,145.48
Disbursements:							
Purchase of securities	$30,178.00	$18,000.00	$5,000.00				$ 53,178.00
Lamme gold medal, bronze replica, and certificate					$227.00		227.00
Transfer to operating fund			1,350.12				1,350.12
Total disbursements	$30,178.00	$18,000.00	$6,350.12		$227.00		$ 54,755.12
Balance on Deposit With The National City Bank of New York and Various Savings Banks, April 30, 1943	$35,287.71	$10,000.00	$2,715.05	$271.80	$ 27.83	$1,087.97	$ 49,390.36

HASKINS & SELLS
CERTIFIED PUBLIC ACCOUNTANTS

1 EAST 44TH STREET
NEW YORK

May 14, 1943

American Institute of Electrical Engineers,
33 West 39th Street, New York.

Dear Sirs:

We have examined the balance sheet of the American Institute of Electrical Engineers as of April 30, 1943, the related statements of cash receipts and disbursements of operating and restricted funds for the year ended that date and schedule of securities owned as of April 30, 1943, have reviewed the accounting procedures of the Institute, and have examined its accounting records and other evidence in support of such financial statements. Our examination was made in accordance with generally accepted auditing standards applicable in the circumstances and included all auditing procedures we considered necessary, which procedures were applied by tests to the extent we deemed appropriate in view of the system of internal control.

In our opinion, the accompanying balance sheet, schedule of securities owned, and statements of cash receipts and disbursements fairly present, respectively, the financial condition of, and securities owned by, the Institute as of April 30, 1943 and its recorded cash receipts and cash disbursements for the year ended that date, in conformity with generally accepted accounting principles and practices applied on a basis consistent with that of the preceding year.

Yours truly,

(Signed) HASKINS & SELLS

Officers and Committees for 1943–44

Officers

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(*Term expires July 31, 1944*)

Junior Past Presidents

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(*Term expires July 31, 1944*)

HAROLD S. OSBORNE	New York, N. Y.

(*Term expires July 31, 1945*)

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3	CHARLES R. JONES	New York, N. Y.
5	A. G. DEWARS	Minneapolis, Minn.
7	E. T. MAHOOD	Kansas City, Mo.
9	E. W. SCHILLING	Bozeman, Mont.

(*Terms expire July 31, 1944*)

District		
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4	C. W. RICKER	New Orleans, La.
6	L. A. BINGHAM	Lincoln, Nebr.
8	J. M. GAYLORD	Los Angeles, Calif.
10	W. J. GILSON	Toronto, Ont.

(*Terms expire July 31, 1945*)

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R. G. WARNER	New Haven, Conn.

(*Terms expire July 31, 1944*)

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F. R. MAXWELL, JR.	Pensacola, Fla.

(*Terms expire July 31, 1945*)

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W. B. MORTON	Philadelphia, Pa.
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(*Terms expire July 31, 1946*)

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C. W. MIER	Dallas, Tex.
S. H. MORTENSEN	Milwaukee, Wis.

(*Terms expire July 31, 1947*)

National Treasurer

W. I. SLICHTER	New York, N. Y.

(*Term expires July 31, 1944*)

National Secretary

H. H. HENLINE	New York, N. Y.

(*Term expires July 31, 1944*)

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BRAZIL—Richard H. Bowles, Sao Paulo Tramway Light and Power Company, Sao Paulo

ENGLAND—A. P. M. Fleming, Metropolitan-Vickers Electric Company, Trafford Park, Manchester

FRANCE—A. S. Garfield, 173 Boulevard Haussmann, Paris, 8E

INDIA, NORTHERN—V. F. Critchley, 17 Bahawalpur Road, Lahore, Punjab

INDIA, SOUTHERN—N. N. Iengar, The Tata Power Co. Ltd., Bombay House, Bruce Street, Fort Bombay

NEW ZEALAND—P. H. Powell, Canterbury College, Christchurch

SWEDEN—A. F. Enstrom, Ingeniorsvetenskrapsakademien, Stockholm 5

TRANSVAAL, SOUTH AFRICA—W. Elsdon-Dew, P.O. Box 4563, Johannesburg

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C. R. Jones
W. I. Slichter
H. S. Osborne
R. G. Warner

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D. D. Ewing
Walter C. Smith
Dugald C. Jackson
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(*Terms expire July 31, 1944*)

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W. D. Coolidge
J. M. Thomson
(*Terms expire July 31, 1945*)

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R. E. Doherty
J. V. B. Duer
(*Terms expire July 31, 1946*)

F. D. Newbury
D. C. Prince
W. E. Wickenden
(*Terms expire July 31, 1947*)

C. R. Freehafer
C. A. Powel
David Sarnoff
(*Term expires July 31, 1948*)

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T. G. LeClair
K. B. McEachron
(*Terms expire July 31, 1944*)

W. J. Gilson
C. M. Laffoon
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(*Terms expire July 31, 1945*)

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W. I. Slichter, national treasurer
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(*Terms expire July 31, 1944*)

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(*Terms expire July 31, 1944*)

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J. J. Orr
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F. O. Schnure
Hugh L. Smith
Herbert Speight
Jerome J. Taylor
E. E. Turkington
L. A. Umansky
R. L. Walsh
W. E. Wickenden
R. H. Wright

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P. H. Hatch, *secretary*, 514 Siwanoy Place, Pelham Manor, N. Y.

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Leland W. Birch
W. A. Brecht
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E. B. Fitzgerald
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J. G. Inglis
Fraser Jeffrey
C. R. Jones
L. C. Josephs
Paul Lebenbaum
P. A. McGee
T. H. Murphy
J. A. Noertker
F. W. Willcut

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A. G. Dewars
C. L. Dows
S. G. Hibben
O. W. Holden
H. E. Mahan
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E. M. Strong
C. C. Whipple
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Marine Transportation

W. N. Zippler, *chairman*, Gibbs & Cox, Inc., 1 Broadway, New York 4, N. Y.

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J. B. Feder
A. R. Gatewood
L. M. Goldsmith
H. F. Harvey, Jr.
P. B. Harwood
C. J. Henschel
Alex Kennedy, Jr.
Clarence Lynn
F. R. Maxwell, Jr.
I. H. Osborne
Wm. H. Reed
E. M. Rothen
P. E. Vance
O. A. Wilde

Power Generation

A. C. Monteith, *chairman*, Westinghouse Electric & Mfg. Co., East Pittsburgh, Pa.

F. A. Annett
C. A. Corney
R. P. Crippen
John Joseph Dougherty
H. A. Dryar
J. H. Foote
A. H. Frampton
R. L. Frisby
Fraser W. Gay
W. D. Hardaway
Fraser Jeffrey
J. A. Koontz
A. J. Krupy
C. M. Laffoon
V. M. Marquis
C. W. Mayott
G. M. Pollard
Charles R. Reid
Herbert B. Reynolds
M. J. Steinberg
H. D. Taylor
Robert Treat
F. D. Troxel

Technical Committees (continued)

Instruments and Measurements

Chester L. Dawes, *chairman*, Harvard University, Pierce Hall, Cambridge, Mass.
T. S. Gray, *vice-chairman*, Massachusetts Institute of Technology, Cambridge, Mass.
E. I. Green, *secretary*, Bell Telephone Laboratories, 180 Varick St., New York 14, N. Y.

Perry A. Borden
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L. A. Burckmyer, Jr.
C. T. Burke
D. T. Canfield
A. G. Dewars
E. D. Doyle
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W. N. Goodwin, Jr.
Stanley Green
C. M. Hathaway
N. S. Hibshman
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I. F. Kinnard
W. G. Knickerbocker
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H. C. Koenig
Everett S. Lee
J. T. Lusignan
Paul MacGahan
H. C. Otten
W. E. Pakala
G. R. Patterson
E. G. Ratz
A. R. Rutter
F. B. Silsbee
Roy M. Smith
H. M. Turner

Power Transmission and Distribution

H. E. Wulfing, *chairman*, Commonwealth Edison Co., 72 W. Adams St., Chicago, Ill.

D. L. Beeman
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H. M. Trueblood
C. F. Wagner
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Protective Devices

J. B. MacNeill, *chairman*, Westinghouse Electric & Mfg. Company, East Pittsburgh, Pa.

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H. D. Braley
O. E. Charlton
J. E. Clem
H. W. Collins
F. W. Cramer
William Deans
F. M. Defandorf
G. B. Dodds
W. S. Edsall
F. R. Ford
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F. C. Hanker
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P. A. Jeanne
L. F. Kennedy
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Roy M. Smith
H. R. Stewart
H. E. Strang
H. R. Summerhayes
P. L. Taylor
J. J. Tesar
J. M. Towner
J. D. Wood

Therapeutics, Applications of Electricity to

C. V. Aggers, *chairman*, Westinghouse Elec. & Mfg. Co., X-Ray Div., 2519 Wilkins Ave., Baltimore 3, Md.

Lloyd L. Call
W. D. Coolidge
Roy Kegerreis
W. B. Kouwenhoven
H. C. Rentschler
C. W. Ricker

Special Committee

Registration of Engineers

C. R. Beardsley, *chairman*, New York City Department of Commerce, 60 Broadway, Room 1404, New York 4, N. Y.

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Tomlinson Fort
N. B. Hinson
T. G. LeClair
K. B. McEachron
W. H. Stueve
J. G. Wray

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Robin Beach

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American Coordinating Committee on Corrosion
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American Research Committee on Grounding
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Quarterly of Applied Mathematics
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U. S. National Committee, International Electrotechnical Commission
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V. M. Montsinger
J. R. North
Alternates
H. E. Farrer
E. L. Moreland
E. B. Paxton

Washington Award Commission
H. B. Gear
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World Power Conference, Executive Committee of U. S. National Committee
Nevin E. Funk

General Counsel

Parker & Aaron
20 Exchange Place, New York 5, N. Y.

Student Branches of the Institute

Name and Location	District	Counseler (Member of Faculty)	Chairman	Secretary
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Alabama Polytechnic Inst., Auburn, Ala.	4	W. W. Hill	J. T. McRae	Warner Sinback
Alabama, Univ. of, University, Ala.	4	W. J. Miller	Frank Tompkins	Louise Inge
Alberta, Univ. of, Edmonton, Alberta, Can.	10	R. E. Phillips	B. McDiarmaid	D. MacDonald
Arizona, Univ. of, Tucson, Ariz.	8	J. C. Clark	S. H. Foote	Herbert Jacobs
Arkansas, Univ. of, Fayetteville, Ark.	7	W. B. Stelzner	W. R. Thomas	D. P. Patterson
British Columbia, Univ. of, Vancouver, Can.	10	W. B. Coulthard	S. J. Beaton	O. M. Julson
Brooklyn, Poly. Inst. of, Brooklyn, N. Y.	3	A. B. Giordano		
(Day Division)			Eugene Lovette	Wm. Stolze
(Evening Division)			A. E. Ruppel	
Brown Univ., Providence, R. I.	1	F. N. Tompkins	W. H. Kimball	D. C. Taylor
Bucknell Univ., Lewisburg, Pa.	2	K. B. MacKichan	Henry Parkin	John Raudenbush
Calif. Inst. of Tech., Pasadena, Calif.	8	F. W. Maxstadt	R. Lauterbach	Alfred Robinson
Calif., Univ. of, Berkeley, Calif.	8	P. L. Morton	Henry Doeleman	J. B. Duryea
Carnegie Inst. of Tech., Pittsburgh, Pa.	2	G. R. Patterson	W. W. Carpenter	E. A. Sagan
Case School of Applied Science, Cleveland, Ohio	2	P. L. Hoover	R. E. Rohrer	G. B. Ober
Catholic Univ. of America, Washington, D. C.	2	T. J. MacKavanagh	Wm. Whelan	P. J. Bayer
Cincinnati, Univ. of, Cincinnati, Ohio	2	L. R. Culver	W. J. Gruber	C. G. Haehnle
Clarkson College of Tech., Potsdam, N. Y.	1	A. R. Powers	R. J. Whalen	G. E. McElroy
Clemson Agri. College, Clemson, S. C.	4	F. T. Tingley	E. Hutto, Jr.	J. E. Webb
Colorado State Col. of A. & M. Arts, Fort Collins, Colo.	6	F. B. Beatty	Glen Powers	Ralph Green
Colorado, Univ. of, Boulder, Colo.	6	H. B. Palmer	Douglas Trego	R. E. McCosh
Columbia Univ., New York, N. Y.	3	J. R. Ragazzini	Edward Buyer	C. P. Thomas
Connecticut, Univ. of, Storrs, Conn.	1	G. S. Timoshenko	J. L. Stern	R. J. Jeffries
Cooper Union, New York, N. Y.	3	J. W. Hostetter		
(Day Division)			Seymour Levander	Thomas Schwartz
(Evening Division)			J. J. Coughlin	S. Meyers
Cornell Univ., Ithaca, N. Y.	1	E. T. B. Gross	Robert Garmezy	Richard Koch
Delaware, Univ. of, Newark, Del.	2	M. G. Young	Wm. Schuster	Clarence Perry
Denver, Univ. of, Denver, Colo.	6	F. H. McClain	Maynard Thompson	Walter Magnuson
Detroit, Univ. of, Detroit, Mich.	5	H. O. Warner	J. P. Bellafaire	F. W. Rowe
Drexel Inst. of Tech., Philadelphia, Pa.	2	E. O. Lange	Paul Beroza	D. E. Thomas
Duke Univ., Durham, N. C.	4	O. J. Meier, Jr.	K. E. Sanger	Charles Myers
Florida, Univ. of, Gainesville, Fla.	4	E. F. Smith	Leslie Stroup	O. R. Gano
George Washington Univ., Washington, D. C.	2		Ned Schreiner	George Conrad
Georgia School of Tech., Atlanta, Ga.	4	H. B. Duling	R. E. Morris	L. S. Apple
Harvard Univ., Cambridge, Mass.	1	J. D. Cobine	J. C. Fisher	
Idaho, Univ. of, Moscow, Idaho	9	J. Hugo Johnson	R. F. Conrad	Howard Kambitsch
Illinois Inst. of Tech., Chicago, Ill.	5	E. H. Freeman	James McDonald	Irving Kalusna
Illinois, Univ. of, Urbana, Ill.	5	E. A. Reid	V. A. Rydbeck	J. E. Hinchcliffe
Iowa State College, Ames, Iowa	5	B. S. Willis	John Lagerstrom	
Iowa, Univ. of, Iowa City, Iowa	5	H. R. Reed	E. E. Carlson	V. W. Chabal
Johns Hopkins Univ., Baltimore, Md.	2	F. Hamburger, Jr.	Vernon Evans	Earl Keller
Kansas State College, Manhattan, Kan.	7	J. E. Ward, Jr.	R. J. Cossaart	A. K. Kingsley
Kansas, Univ. of, Lawrence, Kan.	7	G. A. Richardson	Frank Blue	Glen Sankey
Kentucky, Univ. of, Lexington, Ky.	4	Brinkley Barnett	R. L. Hucaby	
Lafayette College, Easton, Pa.	2	F. W. Smith	Paul Ackroyd	G. P. Clute, Jr.
Lehigh Univ., Bethlehem, Pa.	2	C. G. Brennecke	Gordon Roberts	Ralph Lau
Louisiana State Univ., Baton Rouge, La.	4	A. K. Ramsey	A. B. Daniels	L. E. Gulledge
Louisville, Univ. of, Louisville, Ky.	4	M. G. Northrop	L. E. Vordenbrueggen	J. L. Wedekind
Maine, Univ. of, Orono, Me.	1	N. E. Wilson	D. E. Davis	
Manhattan College, New York, N. Y.	3	R. T. Weil	R. E. Blewitt	D. G. O'Connor
Marquette Univ., Milwaukee, Wis.	5	J. F. H. Douglas	Arthur Zimmer	D. Bernhagen
Maryland, Univ. of, College Park, Md.	2	L. J. Hodgins	Lyal Merriken	Wade Dorsett
Mass. Inst. of Tech., Cambridge, Mass.	1	K. L. Wildes	J. N. Childs	J. B. Angell
Michigan Col. of Min. & Tech., Houghton, Mich.	5	C. Russell	R.E. Sherman	W. T. Ames
Michigan State Col., E. Lansing, Mich.	5	M. M. Cory	W. P. Bennett	J. E. Allen
Michigan, Univ. of, Ann Arbor, Mich.	5	J. S. Gault	M. A. Gilleo	R. E. Miller
Milwaukee Sch. of Engg., Milwaukee, Wis.	5	E. L. Wiedner	Gordon Stanhope	D. C. Schmidt
Minnesota, Univ. of, Minneapolis, Minn.	5	J. H. Kuhlmann	W. F. Carter	E. J. Proszek
Mississippi State Col., State College, Miss.	4	N. M. McCorkle	F. E. Uithoven	J. E. King, Jr.
Missouri Sch. of Mines & Met., Rolla, Mo.	7	J. S. Johnson	G. E. Barber	R. E. Gogan
Missouri, Univ. of, Columbia, Mo.	7	D. Waidelich	B. J. Gastineau	Robert Roney
Montana State College, Bozeman, Mont.	9	E. W. Schilling	Howard Ellsworth	Rod Auclair
Nebraska, Univ. of, Lincoln, Neb.	6	O. E. Edison	C. A. Stutt	J. V. Jensen
Nevada, Univ. of, Reno, Nevada	8	S. G. Palmer	Wm. Richter	Robert Howard
Newark Col. of Engg., Newark, N. J.	3	F. A. Russell	R. H. Rose	J. G. Friedman
New Hampshire, Univ. of, Durham, N. H.	1	W. B. Nulsen	Richard Foley	A. G. Yeaton
New Mexico State Col., State College, N. M.	7	M. A. Thomas	Jack Burleson	Paul Russell
New Mexico, Univ. of, Albuquerque, N. M.	7	R. W. Tapy	Edward Ancona	N. Maguire
New York, Col. of the City of, New York	3	Harry Baum		
(Day Division)			N. Rynkowsky	Irving Flyer
(Evening Division)				
New York Univ., New York	3	P. C. Cromwell		
(Day Division)			Thomas Fentress	Sidney Hasin
(Evening Division)			N. M. Vrana	C R. Morrell
North Carolina State Col., Raleigh, N. C.	4	L. M. Keever	G. B. Stevens	P. D. Strum
North Dakota State Col., Fargo, N. D.	5	H. S. Rush	Waldron Wigtil	Marvin Gebhardt
North Dakota, Univ. of, Grand Forks, N. D.	5	C. W. Rook	Ernest Lees	Herbert Frey
Northeastern Univ., Boston, Mass.	1	R. G. Porter		
(Division A)			T. F. Mahoney	J. O'Leary
(Division B)			G. P. Minalga	J. E. Mrose
Northwestern Univ., Evanston, Ill.	5	R. W. Jones	Chad Pierce	G. Gauer
Norwich Univ., Northfield, Vt.	1	D. E. Howes	J. S. Allen	W. Flint
Notre Dame, Univ. of, Notre Dame, Ind.	5	J. A. Northcott	G. A. Charters	R. J. Martina
Ohio Northern Univ., Ada, Ohio	2	G. W. Klein	Ralph Walters	H. A. Ault
Ohio State Univ., Columbis, Ohio	2	S. O. Evans	Edward Hayden	John Birch
Ohio Univ., Athens, Ohio	2		Thomas Raymond	
Oklahoma A. & M. Col., Stillwater, Okla.	7	A. Naeter	G. L. Lucky	Stephen Nelson
Oklahoma, Univ. of, Norman, Okla.	7	R. A. Church	F. S. Chaffin	J. C. Curry
Oregon State Col., Corvallis, Ore.	9	W. H. Huggins	C. W. Eastman	C. E. Allen
Pennsylvania State Col., State College, Pa.	2	P. X. Rice	E. F. Von Arx	E. P. Diehl

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Pittsburgh, Univ. of, Pittsburgh, Pa.	2	R. C. Gorham	K. E. Doriot	A. J. Baeslack
Porto Rico, Univ. of, Mayaguez, P. R.	3	M. Wiewall, Jr.	R. A. Preston	J. E. Gomez
Pratt Institute, Brooklyn, N. Y.	3	D. H. Wright	Martin Lewis	M. H. Kitchell
Princeton Univ., Princeton, N. J.	2	W. C. Johnson	R. G. Mills	G. R. Compton III
Purdue Univ., Lafayette, Ind.	5	J. H. Karr	R. E. Williams	W. M. Robinson
Rensselaer Poly. Inst., Troy, N. Y.	1	E. D. Broadwell	P. E. Smith	John Litsios
Rhode Island State Col., Kingston, R. I.	1	W. J. Mowbray	Donald Campbell	Paul Ross
Rice Institute, Houston, Texas	7	C. R. Wischmeyer	Hugh Saye	J. H. Valcik
Rose Poly. Inst., Terre Haute, Ind.	5	C. C. Knipmeyer	R. J. Howell	R. D. Calvert
Rutgers Univ., New Brunswick, N. J.	3	J. L. Potter	M. Taubenslag	Wm. Blauvelt
Santa Clara, Univ. of, Santa Clara, Calif.	8	W. J. Warren	R. P. Rossi	E. M. Selle
South Carolina, Univ. of, Columbia, S. C.	4	W. M. Bauer	J. D. Carson	Enoch Smith
South Dakota State Col., Brookings, S. D.	5	Wm. H. Gamble	B. O. Randall	V. V. Nielson
So. Dakota State School of Mines & Tech., Rapid City, S. D.	6	E. E. Clark	Donald Sullivan	W. H. Gardner
Southern California, Univ. of, Los Angeles, Calif.	8	P. S. Biegler	Howard Helfman	Alois Jokl
Southern Methodist Univ., Dallas, Texas	7	E. H. Flath	R. R. Hair	John Savage
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Tennessee, Univ. of, Knoxville, Tenn.	4	W. O. Leffell	Robert Powell	T. A. Pomeroy
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Texas Technological Col., Lubbock, Texas	7	C. V. Bullen	O. L. Cheaney	Hal Pender
Texas, Univ. of, Austin, Texas	7	R. A. Galbraith	Harold Smith	Jay Seefeld
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Virginia Poly. Inst., Blacksburg, Va.	4	Claudius Lee	C. E. Carr	R. B. Fetter
Virginia, Univ. of, University, Va.	4	J. S. Miller, Jr.	R. A. Dudley	P. B. Peyton, Jr.
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Washington, Univ. of, Seattle, Wash.	9	R. E. Lindblom	A. R. True	Lloyd Elliott
Washington Univ., St. Louis, Mo.	7	D. A. Fischer	E. W. Wechsler	C. W. Jeep, Jr.
West Virginia Univ., Morgantown, W. Va.	2	A. H. Forman	G. M. Powers	D. R. Steele
Wisconsin, Univ. of, Madison, Wis.	5	J. R. Price	J. J. Lyons	M. W. Oleson
Worcester Poly. Inst., Worcester, Mass.	1	V. Siegfried	F. J. Mickiewicz	E. G. Baldwin
Wyoming, Univ. of, Laramie, Wyo.	6	G. H. Sechrist	E. R. Allen	Y. Hattori
Yale Univ., New Haven, Conn.	1	A. G. Conrad	A. B. Williams	T. H. Hedene
Total Branches 125				

Number of Enrolled Students November 1, 1943—3,573

Geographical District Executive Committees

District	Chairman (Vice-President, AIEE)	Secretary (District Secretary)	Chairman, District Committee on Student Activities
1 North Eastern	K. B. McEachron, General Electric Company, 100 Woodlawn Ave., Pittsfield, Mass.	Victor Siegfried, Worcester Polytechnic Institute, Worcester, Mass.	R. G. Porter, Northeastern University, Boston, Mass.
2 Middle Eastern	W. E. Wickenden, Case School of Applied Science, Cleveland 6, Ohio	H. R. Vaughan, Westinghouse Elec. & Mfg. Co., East Pittsburgh, Pa.	P. X. Rice, Pennsylvania State College, State College, Pa.
*3 New York City	C. R. Jones, Westinghouse Elec. & Mfg. Co., 40 Wall St., New York 5, N. Y.	R. L. Webb, Consolidated Edison Co. of New York, Inc., 4 Irving Place, New York 3, N. Y.	D. H. Wright, Pratt Institute, Brooklyn, N. Y.
4 Southern	C. W. Ricker, Tulane University, New Orleans 15, La.	F. E. Johnson, Jr., 317 Baronne St., New Orleans 9, La.	Claudius Lee, Virginia Polytechnic Institute, Blacksburg, Va.
5 Great Lakes	A. G. Dewars, Northern States Power Co., 15 S. 5th St., Minneapolis, Minn.	N. C. Pearcy, Public Utility Engg. & Service Corp., 231 S. La Salle St., Chicago 4, Ill.	M. M. Cory, Michigan State College, East Lansing, Mich.
6 North Central	L. A. Bingham, University of Nebraska, Lincoln, Nebr.	Frank C. Howard, Iowa-Nebraska Light & Power Co., Lincoln, Nebr.	H. B. Palmer, University of Colorado, Boulder, Colo.
7 South West	E. T. Mahood, Southwestern Bell Telephone Co., 11th & Oak Sts., Kansas City, Mo.	R. G. Kloeffler, Kansas State College, Manhattan, Kans.	M. A. Thomas, New Mexico State College, State College, N. M.
8 Pacific	J. M. Gaylord, Metropolitan Water District of Southern California, 306 W. 3rd St., Los Angeles 13, Calif.	Mark A. Sawyer, Box 5300, Metropolitan Station, Los Angeles, Calif.	F. W. Maxstadt, California Institute of Technology, Pasadena 4, Calif.
9 North West	E. W. Schilling, Montana State College, Bozeman, Mont.	W. A. Boyer, Anaconda Copper Mining Co., Butte, Mont.	L. D. Harris, University of Utah, Salt Lake City, Utah
10 Canada	W. J. Gilson, 1244 Dufferin St., Toronto, Ont.	W. Roy Harmer, H.E.P.C. of Ontario, 620 University Ave., Toronto, Ont.	

* District 3 includes all foreign countries except Canada.

Note: Each District executive committee includes also the chairmen and secretaries of all Sections within the District, and the District vice-chairman of the national membership committee.

Sections of the Institute

Name	District	When Organized	Membership Aug. 1, 1943	Chairman	Secretary	Secretary's Address
Akron	2	Aug. 12, '20	87	R. F. Snyder	H. L. Smith	Goodyear Tire & Rubber Co., Akron, Ohio
Alabama	4	May 22, '29	42	E. R. Coulbourn	C. F. Sittloh	Alabama Power Co., Birmingham, Ala.
Arizona	8	Mar. 22, '41	37	Malcolm Bridgwater	E. A. Gisel	Central Arizona Light & Power Co., Phoenix, Ariz.
Boston	1	Feb. 13, '03	574	T. Cooper, Jr.	W. I. Middleton	Simplex Wire & Cable Co., Cambridge, Mass.
Central Indiana	5	Jan. 12, '12	133	C. R. Swenson	C. E. Parks	Public Service Co. of Indiana, Inc., Indianapolis, Ind.
Chicago	5	1893	849	F. E. Keith	R. C. Ericson	Northern Indiana Public Service Co., Hammond, Ind.
Cincinnati	2	June 30, '20	151	V. G. Rettig	W. H. Breunig	303 West Third St., Cincinnati 2, Ohio
Cleveland	2	Sept. 27, '07	368	P. L. Hoover	V. A. Diggs	Ohio Bell Telephone Co., 750 Huron Rd., Cleveland, Ohio
Columbus	2	Mar. 17, '22	82	S. O. Evans	R. W. Miner	323 No. Ardmore Road,, Columbus 9, Ohio
Connecticut	1	Apr. 16, '21	365	E. C. Brown	C. D. Hewitt	P.O. Box 1562, New Haven 6, Conn.
Dayton	2	June 9, '43	122	L. J. Fritz	J. W. Gehrke	Dayton Power & Light Co., 25 No. Main St., Dayton 2, Ohio
Denver	6	May 18, '15	186	W. H. Taylor	L. R. Patterson	Public Service Co. of Colorado, Denver, Colo.
East Tennessee	4	Sept. 2, '36	134	R. M. Ferrill	W. R. Gilkeson	Tennessee Valley Authority, Chattanooga, Tenn.
Erie	2	Jan. 11, '18	71	H. N. Shaw	W. D. Bearce	4121 Sassafras St., Erie, Pa.
Florida	4	Jan. 28, '31	136	C. F. Titus	P. J. Carlin	Florida Power & Light Co., Miami, Fla.
Fort Wayne	5	Aug. 14, '08	100	C. W. Kronmiller	E. G. Downie	General Electric Co., Fort Wayne, Ind.
Georgia	4	Jan. 14, '04	101	J. E. Mellett	Carl Evans	Electrical South, 1020 Grant Bldg., Atlanta 3, Ga.
Houston	7	Aug. 7, '28	153	H. P. Heafer	Hezzie Clark	P.O. Drawer 2220, Houston 1, Tex.
Iowa	5	June 25, '29	80	W. B. Boast	E. B. Fowler	Northwestern Bell Telephone Co., Des Moines 9, Iowa
Ithaca	1	Oct. 15, '02	60	A. B. Credle	W. E. Meserve	Cornell University, Ithaca, N. Y.
Kansas City	7	Apr. 14, '16	143	V. P. Hessler	S. H. Pollock	Box 679, Kansas City 10, Mo.
Lehigh Valley	2	Apr. 16, '21	189	R. M. Wyatt	L. Z. Ludorf	Penn. Pwr. & Lt. Co., 135 N. Washington St., Wilkes Barre, Pa.
Los Angeles	8	May 19, '08	548	L. F. Hunt	E. S. Condon	357 South Hill St., Los Angeles 13, Calif.
Louisville	4	Oct. 15, '26	75	L. G. Weiser	M. S. Winstandley	Louisville Gas & Elec. Co., Louisville, Ky.
Lynn	1	Aug. 22, '11	202	W. R. Cox	C. A. Atherton	Champion Lamp Works, 600 Broad St., Lynn, Mass.
Madison	5	Jan. 8, '09	75	F. D. Mackie	E. H. Scheibe	144 W. Gilman St., Madison 3, Wis.
Mansfield	2	Mar. 6, '39	68	R. Felver	M. A. Giles	589 Crescent Road, Mansfield, Ohio
Maryland	2	Dec. 16, '04	349	M. W. Pullen	J. L. Hildebrandt	Consol. Gas Elec. Lt. & Pwr. Co., Monument St. Bldg., Baltimore 3, Md.
Memphis	4	May 22, '30	57	J. F. Fossick	M. G. Siflord	Westinghouse Electric Supply Co., Memphis 1, Tenn.
Mexico	3	June 29, '22	57	F. A. Nava	F. Aubert	Apartado Postal 1399, Mexico, D. F. Mexico
Michigan	5	Jan. 13, '11	393	J. W. Bishop	M. M. Cory	Michigan State College, East Lansing, Mich.
Midwestern Canada	10	Oct. 14, '25	26	M. L. Haynes	J. R. Young	1945 Scarth St., Regina, Sask., Can.
Milwaukee	5	Feb. 11, '10	318	R. H. Earle	E. L. McClure	Wisconsin Elec. Pwr. Co., 231 W. Michigan St., Milwaukee 3, Wis.
Minnesota	5	Apr. 7, '02	118	W. H. Gille	L. A. Griffith	Minneapolis-Honeywell Regulator Co., Minneapolis, Minn.
Montana	9	June 24, '31	40	C. R. Davis	W. H. Blankmeyer	Montana Power Co., Butte, Mont.
Montreal	10	Apr. 16, '43	149	Frederick Krug	D. M. Farnham	107 Craig St. West, Room 307, Montreal, Que., Can.
Muscle Shoals	4	Feb. 18, '38	23	Leo King	Arthur Vaughn	702 Commons, Tuscumbia, Ala.
Nebraska	6	Jan. 21, '25	50	C. P. Kahler	I. M. Ellestad	Northwestern Bell Telephone Co., Omaha, Nebr.
New Mexico–West Texas	7	Mar. 7, '40	48	E. C. Wise	G. V. Cooper	El Paso Electric Co., El Paso, Texas
New Orleans	4	Dec. 8, '33	141	C. P. Knost	F. E. Johnson	317 Baronne St., New Orleans 9, La.
New York	3	Dec. 10, '19	3,586	C. S. Purnell	J. L. Callahan	R.C.A. Communications, Inc., 66 Broad St., New York 4, N. Y.
Niagara Frontier	1	Feb. 10, '25	186	C. E. Gaylord	W. J. Freudenberger	Buffalo Niagara Electric Corp., Buffalo 3, N. Y.
North Carolina	4	Mar. 21, '29	99	H. B. Robinson	L. M. Keever	North Carolina State College, Raleigh, N. C.
North Texas	7	May 18, '28	164	H. G. Mathewson	E. H. Flath	Southern Methodist University, Dallas, Tex.
Oklahoma City	7	Feb. 16, '22	90	R. W. Linney	C. L. Jobe	Oklahoma Gas & Electric Co., Oklahoma City 2, Okla.
Philadelphia	2	Feb. 18, '03	863	H. E. Strang	M. L. Lehman	American Tel. & Tel. Co., 530 Bourse Bldg., Philadelphia 6, Pa.
Pittsburgh	2	Oct. 13, '02	657	G. W. Penney	R. C. Gorham	University of Pittsburgh, Pittsburgh, Pa.
Pittsfield	1	Mar. 25, '04	195	J. R. Meador	D. D. MacCarthy	General Electric Co., Pittsfield, Mass.
Portland	9	May 18, '09	177	A. O. Mangold	W. E. Enns	Portland General Elec. Co., Portland, Oreg.
Providence	1	Mar. 12, '20	93	H. P. Turner	G. E. Andrews	40 Bridgham St., Providence, R. I.
Rochester	1	Oct. 9, '14	121	Walter Criley	O. L. Angevine, Jr.	Stromberg-Carlson Co., 100 Carlson Rd., Rochester, 3, N. Y.
St. Louis	7	Jan. 14, '03	295	F. A. Cooper	F. J. McCluskey	James R. Kearney Corp., 4236 Clayton Ave., St. Louis, Mo.
San Diego	8	Jan. 18, '39	56	W. T. Johnson	J. W. Doolittle	Southern California Tel. Co., 914 C St., San Diego 1, Calif.
San Francisco	8	Dec. 23, '04	601	C. E. Baugh	D. I. Anzini	General Electric Co., 804 Russ Bldg., San Francisco 4, Calif.
Schenectady	1	Jan. 26, '03	630	O. C. Rutledge	P. H. Light	General Electric Co., Schenectady, N. Y.
Seattle	9	Jan. 19, '04	249	H. V. Strandberg	C. D. Howe	Pacific Tel. & Tel. Co., Seattle, Wash.
Sharon	2	Dec. 11, '25	162	H. S. Gates	F. D. Fielder	1324 Yahres Road, Sharon, Pa.
South Bend	5	Feb. 26, '41	48	L. F. Stauder	C. M. Dunn	General Electric Co., 112 W. Jefferson, South Bend, Ind.
South Carolina	4	Mar. 2, '40	55	W. H. Kendrick	F. T. Tingley	Clemson College, Clemson, S. C.
South Texas	7	May 23, '30	50	G. E. Schmitt	S. R. Friedsam	Lower Colorado River Authority, Austin, Tex.
Spokane	9	Feb. 14, '13	85	H. E. Mellrud	J. F. Gogins	General Electric Co., 421 W. Riverside Ave., Spokane 8, Wash.
Springfield	1	June 29, '22	51	A. L. Davis	B. N. Durfee	28 Talcott Ave., West Springfield, Mass.
Syracuse	1	Aug. 12, '20	129	M. H. Pratt	J. W. Dice	Westinghouse Elec. & Mfg. Co., 420 N. Geddes St., Syracuse 4, N. Y.
Toledo	2	June 3, '07	88	E. B. Thurston	W. M. Campbell	2145 Central Grove Ave., Toledo, Ohio
Toronto	10	Sept. 30, '03	381	F. C. Barnes	T. C. D. Churchill	76 Adelaide St., West, Toronto, Ont., Can.
Tulsa	7	Oct. 1, '37	79	W. E. Slemmer	P. E. Gentry	Southwestern Bell Tel. Co., 424 So. Detroit Ave., Tulsa 3, Okla.
Urbana	5	Nov. 25, '02	77	G. R. Peirce	A. D. Bailey	University of Illinois, Urbana, Ill.
Utah	9	Mar. 9, '17	85	T. A. Robinson	J. A. McDonald	General Electric Co., P.O. Box 779, Salt Lake City 9, Utah
Vancouver	10	Aug. 22, '11	98	T. Ingledow	L. B. Stacey	570 Dunsmuir St., Vancouver, B. C., Can.
Virginia	4	May 19, '22	147	C. E. Creecy	A. F. Forbes	Newport News Shipbuilding & Drydock Co., Newport News, Va.
Washington	2	Apr. 9, '03	675	F. B. Silsbee	W. F. Dietz	Westinghouse Elec. & Mfg. Co., 1625 K St., N. W., Washington, D. C.
West Virginia	2	Apr. 9, '40	38	A. M. Rosenblatt	P. M. Barlow	Ravens Park, St. Albans, W. Va.
Wichita	7	Sept. 16, '37	62	G. W. Fisher	R. F. Dice	Kansas Gas & Elec. Co., 201 N. Market St., Wichita 1, Kans.
Worcester	1	Feb. 18, '20	57	J. H. Jewell	R. M. Peirce	American Steel & Wire Co., Electrical Cable Works, Worcester, Mass.

Total Sections.............74

SUBSECTIONS

Illinois Valley (Urbana Section)	E. W. Stone	F. A. Ericzon	Central Illinois Light Co., Peoria, Ill.
Niagara Falls (Niagara Frontier Section)	C. G. Moore	A. W. Walmsley	Electro Metallurgical Co., Niagara Falls, N. Y.
Rock River Valley (Madison Section)	C. V. Bullen	B. T. Anderson	625 Rome Ave., Rockford, Ill.
Zanesville (Columbus Section)	J. O. Fenwick	J. G. Everhart	Line Material Co., Zanesville, Ohio